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Proceedings of the Seminar On

LEAK-BEFORE-BREAK: Progress in Regulatory Policies and Supporting Research

Held at
Tokyo, Japan
May 14-15, 1987

Edited by
K. Kashima/CRIEPI
G. M. Wilkowski/BCD

Sponsored by
Central Research Institute of Electric Power Industry
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Battelle Columbus Division

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PREVIOUS DOCUMENTS IN THE SERIES

CSNI Specialist Meeting on Leak-Before-Break in Nuclear Reactor Piping.
Proceedings of a CSNI seminar held at Monterey, California on September 1-2, 1983, NUREG/CP-0051 published August 1984.

Leak-Before-Break: International Policies and Supporting Research.
Proceedings of a seminar held at Columbus, Ohio on October 28-30, 1985, NUREG/CP-0077 published June 1986.

ABSTRACT

The third in a series of international Leak-Before-Break (LBB) Seminars supported in part by the U.S. Nuclear Regulatory Commission was held at TEPCO Hall in the Tokyo Electric Power Company's (TEPCO) Electric Power Museum on May 14 and 15, 1987. The seminar updated the international policies and supporting research on LBB. Attendees included representatives from regulatory agencies, electric utility representatives, fabricators of nuclear power plants, research organizations, and university professors.

Regulatory policy was the subject of presentations by Mr. G. Arlotto (U.S. NRC, U.S.A.), Dr. H. Schultz (GRS, W. Germany), Dr. P. Milella (ENEA-DISP, Italy), Dr. C. Faidy, P. Jamet, and S. Bhandari (EDF/Septen, CEA/CEN, and Framatome, France), and Mr. T. Fukuzawa (MITI, Japan). Dr. F. Nilsson presented revised nondestructive inspection requirements relative to LBB in Sweden. In addition, several papers on the supporting research programs discussed regulatory policy. Questions following the presentations of the papers focused on the impact of various LBB policies or the impact of research findings. Supporting research programs were reviewed on the first and second day by several participants from the U.S., Japan, Germany, Canada, Italy, Sweden, England, and France.

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ACKNOWLEDGMENTS

This was the third international seminar held on the subject of Leak-Before-Break (LBB) which was supported in part by the U.S. Nuclear Regulatory Commission (NRC). This seminar was held at TEPCO Hall in the Tokyo Electric Power Company (TEPCO) Energy Museum on May 14 and 15, 1987. During the conference, simultaneous translation was supplied in English and Japanese. We would like to thank TEPCO for the use of their facilities.

The major efforts in organizing and supporting this conference were made by CRIEPI. Drs. K. Kuwabara and K. Kashima were primarily responsible for this seminar. We owe a great deal of thanks to the many staff members of CRIEPI for their organizational efforts.

These conference proceedings were organized by Dr. K. Kashima and Mr. G. Wilkowski with the insistence that the participants submit their manuscripts quickly. We thank those who have contributed to these proceedings. Finally, thanks also go to Mr. M. Steve of Battelle for his efforts in compiling this document, as well as to the NRC for publishing it.

Gery Wilkowski
Battelle Columbus Division

K. Kuwabara and K. Kashima
CRIEPI

SUMMARY

The third in a series of international Leak-Before-Break (LBB) Seminars supported in part by the U.S. Nuclear Regulatory Commission was held at TEPCO Hall in the Tokyo Electric Power Company's (TEPCO) Electric Power Museum on May 14 and 15, 1987. The Central Research Institute of Electric Power Industry (CRIEPI) of Japan sponsored the seminar with NRC's encouragement. The seminar updated the international policies and supporting research on LBB. The prior meetings were held at Monterey, California, in September 1983, and Columbus, Ohio, in October 1985. The proceedings of these past seminars have been published in NUREG/CP-0051 and NUREG/CP-0077 respectively.

At this seminar over 100 people were in attendance, including representatives of 12 countries from 49 different organizations. Attendees included representatives from regulatory agencies, electric utility representatives, fabricators of nuclear power plants, research organizations, and university professors. The attendance list is in Appendix A.

Regulatory policy was the subject of presentations by Mr. G. Arlotto (U.S. NRC, U.S.A.), Dr. H. Schultz (GRS, W. Germany), Dr. P. Milella (ENEA-DISP, Italy), Dr. C. Faidy, P. Jamet, and S. Bhandari (EDF/Septen, CEA/CEN, and Framatome, France), and Mr. T. Fukuzawa (MITI, Japan). Dr. F. Nilsson presented revised nondestructive inspection requirements relative to LBB in Sweden. In addition, several papers on the supporting research programs discussed regulatory policy. Questions following the presentations of the papers focused on the impact of various LBB policies or the impact of research findings.

Supporting research programs were reviewed on the first and second day by several participants from the U.S., Japan, Germany, Canada, Italy, Sweden, England, and France. Highlights of regulatory presentations are summarized below.

Summary of Presentations on Regulatory LBB Policies or Procedures

In the U.S., LBB has been accepted for primary PWR piping. This is referred to as a limited scope change to NRC's General Design Criteria 4 (GDC-4). As noted by Mr. Arlotto, currently a broad scope change to GDC-4 is being implemented so that it extends LBB to all high-energy piping that meets rigorous criteria. In these design rule changes, certain limitations exist. First, the piping system must not be susceptible to either fatigue (mechanical, thermal, or corrosion-assisted), corrosion (stress corrosion cracking or general corrosion), erosion (or erosion-corrosion), creep, or water hammer. This restriction generally gives a small probability of long cracks occurring, and the stress levels should be low or known. For example, loads from a water hammer event are unknown. If these restrictions are satisfied, then it must be shown that for a postulated through-wall crack, the leakage can be detected at normal operating stresses for a crack length that will be stable at faulted loads (normal plus safe shutdown earthquake stresses). A safety factor of ten on the leakage detection capability has been used. There are also safety factors on the critical crack size at the faulted loads. Elastic-plastic fracture mechanics analysis is used with the worst case material properties in the crack stability analysis. The GDC-4 changes permit elimination of pipe whip restraints and jet impingement shields for qualifying piping systems. LBB is not used for sizing of containments, emergency core cooling systems design changes, or environmental qualification of electrical equipment. One issue to be addressed in the future is an appropriate replacement for the double-end guillotine break (DEGB) design criterion.

In Japan, a joint program has been conducted between MITI, electric utilities, and reactor vendors to improve and standardize LWR component design. This has involved research programs on stainless steel piping, which have been completed, and a current carbon steel piping program. Mr. Fukuzawa of MITI stated that a task group and technical advisors have been reviewing the applicability of the LBB concept and the impact on safety. Concerning applicability of the LBB concept, recent studies on subcritical crack propagation showed that the LBB concept is applicable to BWR and PWR piping. This assumes that (1) selection of piping material, design, fabrication, and inspection is done in accordance with technical standards and codes approved by MITI, (2) proper measures are undertaken to prevent stress corrosion cracking, (3) leak detection is possible with existing equipment, and (4) in-service inspection is carried out in accordance with the existing standards. Concerning the impact on safety, the review group determined LBB has no impact on (1) the engineering safety features, (2) the emergency shutdown systems, and (3) the containment. The applications of LBB should substantially reduce dynamic loads due to pipe whipping, jet impingement, and pressure imbalance in the vessel cavity.

In West Germany, LBB has been accepted in the guidelines of the Reactor Safety Commission (RSK) since 1981. Dr. Schultz stated that this has been applied to PWR's for the abandonment of pipe whip restraints on main primary coolant piping. The RSK guidelines require that the basic safety concept is followed to guarantee high-quality piping systems. LBB, for the purpose of elimination of pipe whip restraints, has also been accepted for the main steamlines and the feedwater piping inside the containment and up to the first closure valve outside the containment. This was included in the 1983 RSK guidelines. In March 1984, the exclusion of the DEGB was extended to austenitic steel piping in PWRs such as in the surge line and the branch connections of the emergency core cooling pipe system. For BWR piping, similar decisions are made for piping replaced by ferritic materials. For the high-temperature gas-cooled reactor (THTR 300), as well as for the sodium-cooled reactor (SNR 300), the LBB concept has been accepted in certain systems. In general the LBB concept has received wide acceptance to large diameter piping, but difficulties arise for small and medium sized pipe. A point noted in the question and answer period was that, in West Germany, if a crack has been detected, frequently the flaw assessment criteria do not necessarily assume that the stresses in the design report are correct. The service stresses will be reviewed and may be documented with in-plant instrumentation. In this way the remaining life extension can be properly evaluated. A second point of discussion was that West German policy allows LBB justification to reduce design requirements for heavy components.

In Canada, LBB is currently being evaluated for application to the large diameter heat transport piping system in Candu reactors. Dr. J. S. Nathwani described an ongoing program. The current study is limited to carbon steel piping larger than 21 inches (533 mm) in diameter, and is divided into two elements: those that demonstrate crack stability and those related to leakage.

The crack stability evaluations involved a material test program and elastic-plastic fracture mechanics. The leakrate program involves development of a leakrate estimation computer code and leakrate testing. The testing facility is in the process of being built.

In Italy, LBB policy is currently being developed by ENEA. A basic approach was to assume that a postulated through-wall crack may exist. This eliminates uncertainties in fatigue analysis and in nondestructive-testing crack-depth accuracies. Since new plants require seamless straight pipe and elbows without longitudinal seam welds, the circumferential crack is the most likely crack orientation. Current research results show that the crack length should be limited to less than 140 degrees around the pipe circumference for the maximum allowable loads from the ASME Section III piping stress code. A safety factor of four on the crack length is used, so that the maximum length is 35 degrees for in-service inspection. No credit is given for the depth of the crack. For reaction forces, the leakage area for the 140-degree crack is used. Experiments in Italy showed that at the start of ductile tearing, the leakage area for such a crack is less than 5 percent of the pipe's cross-sectional area. At the maximum load after the start of ductile tearing, the leakage area has been found to be less than 10 percent of the pipe's cross-section. With the 10 percent cross sectional leakage, the thrust loads are sufficiently small enough so that practically no restraint is needed. Pipe whip restraints are then not needed. This improves inspectability. For in-service inspection, the same requirements are now imposed on secondary and primary systems. Small diameter pipe, and pipes with radius-to-thickness ratios greater than 10 will not be eligible for LBB justification of pipe whip restraint removal. Containment and ECCS designs will continue to be based on the assumption of the DEGB of a primary pipe. DEGB will also continue to be used for supports of large components, such as steam generators, pressurizers, and pumps.

In Sweden, new regulations have been developed for the safety of pressurized components in nuclear power plants. Two aspects of these regulatory changes were discussed. One aspect was a new classification system to determine the inspection frequency of components. Components can be classified into three control groups. For Control Group A, 75 percent of the objects should be inspected once every six years. For Control Group B, 10 percent of the objects are inspected. For Control Group C, rules are prescribed for non-nuclear equipment. The determination of a component's control group classification depends on two indices. The first is the Fracture Index which has high, medium, and low categories. The second index is the Consequence Index which has four categories. A matrix of these two indices has been created so that components with high Fracture and Consequence Indices will result in a component being in Control Group A for inspection frequency requirements. At the other extreme, any component with the lowest Consequence Index will fall into Control Group C for inspection frequency. A second aspect discussed involved the continued operation of degraded equipment, that is components that might have cracks. Such components may continue to be used in service if (i) the component satisfies the requirements of ASME Section XI; (ii) the R-6 method could be used if not covered by ASME; and (iii) for equipment in Control Groups A and B LBB is highly probable.

In England, LBB has been applied in certain specific instances. More recently, the work has focussed on the formulation of the LBB procedure using the R6-CEGB procedure.

In France, LBB is currently being considered for potential application to their nuclear power plants. The main objective is to eliminate the consequences of longitudinal and circumferential pipe breaks, so that pipe whip restraints and jet impingement shields can be eliminated. The design loads on civil structures for pipe supports can also be simplified. Research programs are under way to analytically and experimentally validate elastic-plastic fracture mechanics analyses for ferritic and austenitic steel pipes under quasi-static and dynamic (seismic) loading. In the steps to demonstrate LBB, the stability of both through-wall and surface-cracked pipe at level D (seismic) loads is evaluated. Inclusion of a surface crack stability analysis is an additional feature that is not considered in LBB analyses in many countries. Currently in France, LBB is not applied to centrifugally cast stainless steel due to concerns about thermal-aging degradation of the material's toughness and the fact that the ability to inspect for cracks by ultrasonics is difficult.

Summary of Presentations on Research Activities

In the U.S., piping integrity research activities are sponsored by the U.S. NRC and EPRI. The U.S. NRC is currently sponsoring programs in elastic-plastic fracture of piping at Battelle (Degraded Piping Program and International Pipe Integrity Research Group); cracked pipe integrity and fracture toughness studies at David Taylor Research Center and the U.S. Naval Academy; pipe material property data base and ferritic steel corrosion fatigue at Materials Engineering Associates; aging of cast stainless steels, stress corrosion cracking, and acoustic emission evaluation of leakage detection at Argonne National Laboratories; and NDT at Battelle Pacific Northwest Division. Of these, a summary of the significance of the Degraded Piping Program results on LBB and flaw evaluation procedures was made (see paper by Wilkowski).

EPRI has been very active in the formulation of the ASME Section XI pipe flaw evaluation code procedures. A stainless steel pipe flaw evaluation procedure has been developed and is in Article IWB-3640. A ferritic steel pipe flaw evaluation criteria is currently under development (see paper by Kishida and Norris). EPRI has also been responsible for many other developments, such as the EPRI/GE J-estimation scheme analyses, the PICEP leakrate estimation computer code (see paper by Norris, Kishida, and Chexal), stress-corrosion cracking studies, NDT improvements, and is active in promoting LBB for the industry (see paper by Norris, Kishida and Chexal).

In Japan, research efforts have been undertaken by MITI at NUPEC (see paper by Asada), by STA at JAERI (see paper by Shibata, Yasuda, Onizawa, and Miyazono), as well as some efforts at various nuclear system vendors. Experimental efforts have been completed on fracture of stainless steel piping, while a carbon steel pipe fracture program is currently ongoing. Various round robin analyses have been undertaken to gain confidence in methodologies to be used (see papers by Takahashi and Hasegawa et al.). Analysis methodologies involve comparisons of large-scale finite element analyses to estimation scheme analyses for elastic-plastic fracture (see paper by Yagawa and Ueda).

In West Germany, further research topics include component testing for crack growth under environmental conditions, crack opening behavior in elbow and branch connections, leakrates at transient loading and operating conditions, continuous evaluation of operating experience, and generic evaluation for load following operation with respect to loads, water chemistry, and operator errors (see paper by Schultz). Significant efforts have also been undertaken at MPA-Stuttgart. Past efforts in the Phenomenological Burst Behaviour-Programme have had a significant impact on the development of the Basis Safety Approach employed in the RSK guidelines for LBB justification of the elimination of pipe whip restraints and jet impingement shields. Most of these efforts have concentrated on ferritic pipes with axial cracks. Currently efforts are being made to evaluate circumferentially cracked pipe under quasi-static loading and impact loading (see paper by Sturm). Probabilistic fracture mechanics analysis for LBB are also being undertaken (see paper by Bruckner-Foit and Munz). In this analysis, the effect of multiple cracks initiating, rather than just a single crack, was considered.

In Canada, several research programs are under way at Ontario Hydro (see paper by Nathwani). These programs are in the areas of material testing, fracture mechanics analyses, and leakrate evaluations. The material testing program is described in the paper by Mukherjee. The program is aimed at determining the J-integral crack growth resistance curves for various material to be used in the large diameter heat transport pipe for the Darlington Nuclear Generating Station A. The elastic-plastic fracture mechanics analyses involved using the ABAQUS general purpose finite element computer code. Analyses were performed for circumferential cracked straight pipe, as well as axial cracks in elbows, tees, and branch connections. The leakage studies were on leakage detection capability, leakrate models, and leakrate tests.

In Italy, research efforts have been continuing since 1981. These efforts involve axial and circumferentially cracked pipe. Both stainless steel and carbon steel piping have been evaluated by quasi-static fracture tests. Future programs will involve evaluation of cracked pipe under dynamic loads, material property data bases, fracture of elbows and flanges, jet forces and leak detection systems, and development of UT inspection on stainless steel pipes.

In England, various aspects of LBB are being evaluated and applied. The paper by Darlston discusses three applications of LBB: evaluation of pressure vessel tests, application to bellows in a gas-cooled reactor, and an assessment for fast reactor primary vessels. More recent work is focusing on the formulation of a LBB procedure to be included in the R6-CEGB method.

In France, research in the past has involved corrosion fatigue studies, small diameter pipe burst tests, effects of thermal aging on cast stainless steels (see paper by Faidy, Jamet, and Bhandari), and leak flow rates through cracks in pipes (see paper by Chouard and Richard). Current programs involve prototypical tests on circumferentially cracked pipes under quasi-static loading, validation of a one-dimensional cracked element in finite element analysis, dynamic tests on cracked pipe under inertial stresses, development of a one dimensional beam element with three-dimensional elements to conduct dynamic analyses, and development of a hinge element for simpler finite element analysis, and assessment of different engineering methods to evaluate elastic-plastic fracture of cracked pipe.

Summary of Questionnaire Responses

A questionnaire on LBB concerns was distributed to all the seminar attendees. Twenty-six questionnaires were returned. The questionnaire asked about concerns that people had about LBB. The questionnaire is given in Appendix B. The responses are given in Appendix C and are ordered by the respondent's country. A similar questionnaire was issued to the attendees of the Columbus LBB Seminar. For reference the Columbus questionnaire and responses are given in Appendix D. A summary of the questionnaire responses from this meeting is given below.

The first question was related to limiting conditions when the LBB concept is applied. Many conditions were considered, but most of them were classified into the following three catagories: (1) material (stress corrosion cracking, erosion, quality assurance, toughness, fatigue, crack geometry, etc.), (2) design and manufacturing (load, pipe size, welding, etc.), (3) operating and monitoring (leak detection, operating control, water chemistry, etc.). Of the three, the significance of material behavior, stress corrosion cracking and erosion, in particular, was pointed out by many respondents.

The second question was related to the applicability of the LBB concept to the loss of coolant accident (LOCA) definition and the design basis of engineered safety features. There were many views for and against this type of application. However, the significance of an international consensus was commonly recognized by most of the respondents.

The third question was related to the R&D activities to be conducted for the improvement of LBB studies. The six items considered are as follows: (1) full-scale LBB tests (pipe fracture tests, leak tests, component tests), (2) material property tests (toughness, data bank, quality assurance), (3) fracture mechanics approach (crack growth analysis, surface crack analysis, fracture mechanics parameters), (4) monitoring system (crack detection, leak detection), (5) loading conditions, and (6) LBB requirements. Of these six items, many respondents stressed the significance of full-scale tests and material properties studies.

The final question was related to management of the seminar. In general, most attendees felt the seminar was helpful in updating the information on regulations and research activities for LBB. However, different people requested more discussion on either public acceptance, leak detection, or experimental data in future conferences.

Similarities With Past Questionnaires

The similarities and differences with the 1985 Columbus LBB questionnaire responses show the following.

Similarities: Responses for the second question at the 1985 Columbus seminar ("What are your critical concerns in regard to LBB?") are compared with responses for the third question at the 1987 Tokyo seminar ("What kind of research work should have priority for the progress of LBB technology?").

Fundamentally, no remarkable differences were obtained at the seminars. Answers from both seminars can be classified into six common catagories: full-scale tests, material property tests, fracture mechanics approaches, monitoring systems, loading conditions, and LBB requirements.

Differences: At the 1985 Columbus seminar, the significance of leak detection was stressed by many respondents. While at the 1987 Tokyo seminar, R&D priorities are also placed on full-scale tests and material property tests. It should be noted that, at the 1987 Tokyo seminar, the significance of the effects of cyclic load and dynamic strain-aging on material toughness was discussed as one of the new research topics to be conducted.

This seminar was very helpful in clarifying regulatory policies and making others aware of the research results and plans in different countries. Several different areas exist where there are differences of policies in the application of LBB. Future seminars of this type will be beneficial to eliminate any differences that appear to exist in international LBB policies. The development of an international technical consensus is the goal of these seminars. Another seminar may be held in England in May of 1988.

SESSION 1: THE LATEST LBB POLICY

Chairman: Y. Asada, University of Tokyo, Japan

LEAK BEFORE BREAK

SAFETY INCREASED TODAY - WHAT NEXT

by

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INTRODUCTION

I have entitled my presentation "Leak Before Break" - Safety Increased Today - What Next." I want to be sure all understand that the proper application of "Leak Before Break" as I will discuss will increase the overall safety of nuclear power plants. This is what we are all trying to achieve, and we should reflect on our accomplishments. But not for too long a time. There is more to be done, particularly by the international community if we are to achieve the full safety benefit of this technology. Saying it differently, we have increased safety, but there are potentially additional increases that we must pursue. That is the "what next."

The USNRC has been funding, and continues to fund, research programs addressing a broad spectrum of piping research topics. Key research topics include piping design criteria, environmentally assisted cracking, piping fracture criteria, leak detection systems and requirements, and in-service inspection. There have been recent changes in USNRC regulations that stem from research in these areas; it is anticipated that there will be further changes. Today, I will focus on current and future regulatory changes resulting from acceptance of the conclusions of one of these research efforts; namely, "leak-before-break."

Since 1978, the USNRC has been funding research examining the leak-before-break concept because concerns were raised regarding whether overall plant safety was increased by postulating a double-ended pipe break. The leak-before-break topic has been the focus of considerable interest in the international community as well. The USNRC sponsored a CSNI Specialists Meeting in the fall of 1983 addressing leak-before-break, and another international seminar on the topic was held in the fall of 1985. In addition, there have been several technical meetings on the subject held in conjunction with the ASME Pressure Vessel and Piping conferences. All of these meetings have been well attended by the international technical community, suggesting a high level of interest.

Now that a clear picture is beginning to emerge that provides technical justification for applying leak-before-break, it is imperative that the international dialogue continue to assure mutual understanding that will lead to a broader acceptance. Seminars such as this one and the workshop scheduled for next week help disseminate current research results and encourage completion of technology development, thus increasing the probability of international consensus.

Before discussing the regulatory changes in the United States, I would like to give you some history on how the regulations were introduced and what has led us to change them.

BACKGROUND

Almost 20 years ago, the USNRC's predecessor, the Atomic Energy Commission, considered the problem of pipe breaks and conservatively concluded, or so it seemed at the time, that double-ended pipe breaks should be postulated. Initially, the pipe rupture event was postulated only for containment design and the sizing of emergency core cooling systems. Subsequently, the dynamic effects associated with pipe rupture were also assumed to be credible. In implementing this decision, a "break everywhere" approach was taken, including a postulated double-ended break in the large reactor coolant piping. A consequence of protecting against the potential dynamic effects of pipe breaks is the installation of massive structures to restrain "whipping" pipes and the installation of jet impingement barriers to protect important equipment from the effects of escaping fluid. Further, there were very large loads associated with these postulated breaks, and those loads became part of the design basis; in many cases the controlling part, particularly for heavy components such as steam generators, reactor vessels, and coolant pumps.

Results from subsequent piping materials research and insights from probabilistic risk analyses have shown that, in some cases, postulating a double-ended pipe break may not be contributing to overall plant safety. Consequently, the USNRC has implemented modifications to its regulations to permit designs that do not require protection against dynamic effects associated with postulated double-ended pipe ruptures for piping that meets rigorous acceptance criteria. Satisfying the acceptance criteria is deemed an adequate demonstration that the line under consideration will leak before it breaks. Eliminating the need to protect against these dynamic effects leads to removal (or noninstallation for new designs) of certain pipe whip restraints and jet impingement barriers, and permits redesign of heavy component supports. Properly implemented, this approach will enhance safety, reduce occupational radiation exposures, and reduce costs.

MODIFICATIONS TO GDC-4

Turning then to the changes to our regulations, the requirement to postulate the double-ended pipe break for protection against dynamic effects appears most notably in General Design Criterion 4 (GDC-4) of Appendix A to 10 CFR 50. A two-step procedure has been undertaken to modify GDC-4. The first modification, finalized in April of 1986, was limited in scope to the primary coolant loop of Pressurized Water Reactors (PWRs). The second modification covers all high energy piping in all U.S. nuclear power plants. Despite the difference in scope of the two modifications, the acceptance criteria basically were the same.

A fundamental premise of the GDC-4 modifications is that an acceptably low probability of failure is assured if deterministic acceptance and fracture analysis criteria are satisfied. The acceptance criteria preclude applying leak-before-break to any line that is susceptible to significant damage mechanisms such as corrosion (IGSCC for example), erosion, water hammer, fatigue, creep, or indirect failure mechanisms. Satisfying the acceptance criteria, while significant, simply allows one to proceed with the crack stability analyses. Said another way, satisfying the acceptance criteria is a necessary condition but is not a sufficient condition.

If it is demonstrated that a line is not susceptible to these damage mechanisms, then the analysis procedure requires a demonstration that even if a through wall crack were to develop by some unspecified mechanism, it would remain stable with margin under the design basis loadings, i.e., normal plus SSE loads. The size of the through wall crack used in this analysis is derived from plant leak detection capabilities and a validated leak rate model. If the staff determines that the acceptance criteria have been satisfied and that the results of the fracture analyses indicate sufficient margin against failure, then the plant would be permitted to be designed and operated without the protective hardware (pipe whip restraints and jet impingement barriers) on the specific line analyzed. Other related changes can take place as well.

At the NRC, the document that governs the licensing review of an application to design or operate a plant is called the Standard Review Plan. To implement the regulation change just discussed, a new section to the Standard Review Plan, SRP 3.6.3, "Leak-Before-Break Evaluation Procedures," has been drafted that includes the acceptance criteria and fracture analyses. I will summarize the more significant points in the evaluation procedure.

The significant aspects of the review procedures require that:

1. LBB is applied only to ASME Code Class 1 and 2 piping or the equivalent. However, applications to other high energy piping will be considered based on an evaluation of the proposed design and in-service inspection requirements.

2. LBB is applicable only to an entire piping system or a specified portion that can be analyzed as an entity such as piping segments located between anchor points. It cannot be applied to individual welded joints or other discrete locations.

3. The LBB evaluation uses design basis loads and is based on the as-built configuration as opposed to the design configuration. Particular attention is given to snubbers whose failure may invalidate the stresses used in the crack stability evaluation.

4. Evaluations of degradation by erosion, erosion/corrosion, and erosion/cavitation due to unfavorable flow conditions and water chemistry must demonstrate that these mechanisms are not potentially significant sources of pipe rupture. Data based on extended plant operating experience are most useful in these evaluations.

5. The potential for water hammer is evaluated for the system under consideration to assure that pipe rupture due to this mechanism is unlikely. Frequency of water hammer events in specific piping systems over extended periods of operation coupled with a review of operating procedures and conditions would be most useful to demonstrate that water hammer is not a significant contributor to pipe rupture.

6. It must be demonstrated that the line is not susceptible to creep or creep-fatigue. Operating below 700°F in ferritic steels and 800°F in austenitic steels can satisfy concerns of creep for materials commonly used in piping systems. Light water reactors normally operate below these temperatures and, thus, are expected to meet this requirement.

7. It must be demonstrated that the line is resistant to corrosion damage. This demonstration must be based on favorable data from investigations of the frequency and degree of corrosion in the specific piping systems over extended periods of operation. Modification to operating conditions (as for example, careful control of water chemistry) or design changes (as for example, replacing piping material) are measures that can be taken to improve corrosion resistance in piping. Stress corrosion cracking is important but not the only corrosion mechanism to be addressed.

8. It must be demonstrated that the systems under evaluation do not have a history of fatigue cracking or failure. An evaluation must be performed to assure that the potential for pipe rupture due to thermal and mechanical fatigue is unlikely. In addition, it must be demonstrated that there is no significant potential for vibration induced fatigue cracking or failure.

For piping systems that satisfy the acceptance criteria, the next step in the leak-before-break evaluation is to demonstrate that a through wall crack, whose length is based on leak detection considerations, would remain stable with margin for the design basis loading; i.e., normal plus the safe shutdown earthquake. If the acceptance criteria are satisfied and crack stability is demonstrated, it is judged that the line will leak before it breaks.

The details of the crack stability analysis are too involved to elaborate here. However, I would like to point out that the crack stability analysis is conducted using the location in the system with the most unfavorable combination of stress and material properties for base metal, weldments and safe ends. The results of the analysis, which may be based on either a fracture mechanics approach or a limit load approach, as appropriate, are evaluated against two criteria. First, the critical size crack is determined for normal plus SSE for the particular location. This critical size crack must be larger than the leakage related size crack by at least a factor of two. Second, the leakage size crack must be shown to be stable if 1.4 times the normal plus SSE loads are applied. If loads are combined by absolute summation, then the factor of 1.4 may be reduced to 1.0.

We feel that this combination of rigorous acceptance criteria, intended to exclude lines that are susceptible to known damage mechanisms, combined with a crack stability analysis used to account for those unforeseen damage mechanisms, provides an acceptable demonstration that the line will leak before it breaks.

As I noted earlier, for PWR primary coolant loops, redesign of heavy component supports is permitted for those plants where leak-before-break has been demonstrated. The scope is being expanded to other piping in PWRs and to BWRs.

The criteria for redesign have just been finalized. For existing plants, changes in the supporting steel and concrete structures will not be permitted. Redesign will be limited to reducing the capacity and number of snubbers that provide lateral support to the component, and to replacing high strength bolt material. Eliminating the LOCA loads from the design basis leads to a significant reduction in the needed lateral support capability. This, in turn, should reduce the need for very large hydraulic snubbers that experience has shown to be less reliable. Smaller, more reliable, snubbers, which have an in-place test capability, will replace the larger hydraulic snubbers. The NRC's present and on-going modifications to GDC-4 reflect the realization that the interpretation of the original regulations, requiring the installation of pipe whip restraints, jet impingement barriers, and snubbers may not have been in the best interest of safety for all cases because of the reduced effectiveness of inservice inspection and potential inadvertent restraint of thermal growth. We believe that the changes to the regulations, if properly implemented, could result in greater safety for those lines that meet the stringent criteria.

DIRECTIONS FOR THE FUTURE

Turning now to the question of where do we go from here. The changes to GDC-4 are restricted to design for dynamic effects associated with postulated double-ended pipe ruptures. However, the double-ended break of the largest primary coolant pipe is retained as the design basis for other considerations, such as containment design, ECCS sizing requirements, and the environmental qualification of electrical and mechanical equipment. Many of the public comments we received on the changes to GDC-4 suggested extending leak-before-break to these other areas. At this point in time, there is not sufficient technological evidence to support a different design basis for containment design, ECCS requirements, or equipment qualification.

In closing, let me note that we have come a long way since the AEC imposed the break everywhere concept. We now have better knowledge of how pipes may break. This knowledge, properly applied, leads to greater overall safety. However, there is a need for validation of certain criteria discussed earlier.

Further, as we consider the extension of leak-before-break to other aspects of plant design, we must be certain that plant safety is not compromised. At this time, principal reliance will be placed on industry efforts for justification of this extension.

I will close on a personal technological note. We have faced up to a difficult problem, developed data, done analyses, made decisions, and reflected this work in our regulatory decisionmaking process regarding the need to protect against dynamic effects of double-ended pipe breaks. I believe the key technological question that remains is: What do we substitute for the double-ended pipe break? It is only in confronting this question and answering it based on a strong technological base that we can have a consistent design basis and assure ourselves that optimum safety is close at hand. In my judgment, the only hope for success in answering this question rests with the international technological experts - you. Without international agreement on such a dramatic and far reaching change, there is little chance of acceptance. This is the challenge of the future - it is yours.

Latest Development of LBB Policy

and

Future Subjects in West Germany

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Leak-Before-Break Seminar
May 14-15, 1987
Tokyo

ABSTRACT

Since the revision of the Guidelines of the "Reactor Safety Commission" (RSK) in 1981 all license applications for construction of PWR's which abandoned the pipe whip restraints on the main primary coolant piping have been accepted by the licensing authorities.

For PWRs already under construction where the license part concerning the primary circuit was still under consideration the abandonment of the pipe whip restraints was accepted if the applicant could demonstrate that the achieved quality of the main primary coolant piping complied with the requirement of the basic safety concept.

The exclusion of pipe break for pipes made of austenitic steel, like the surge line and the branch connections of the ECC-systems have been decided in March 1984. It has been demonstrated that the reliability of special ultrasonic testing methods are sufficient to ensure the detection of any relevant defects developing inservice from the inside of the pipes.

For PWRs licence applications which abandoned the pipe whip restraints on the main steam and feedwater line inside the containment, up to the first closure valve in the outside containment compartment have also been accepted if the applicant had demonstrated that the principles of basic safety were met equally to the primary piping. A corresponding amendment to the RSK guidelines has been published in 1983.

For BWR systems equivalent decisions are taken in some license applications concerning the replacement of ferritic piping in the main coolant system.

For the high-temperature gas-cooled reactor (THTR 300) as well as the sodium cooled reactor (SNR 300) the leak-before-break concept has been accepted in certain systems.

In general the application of the LBB concept to large diameter piping has gained wide acceptance in the last years. Difficulties arise in the application of the LBB concept to small and medium sized diameter piping, although small leaks contribute significantly to the melt-down frequency in severe accident analysis.

Probabilities of leakages in piping systems as used in risk studies up to now do not represent the present state of the art. The goal of our present investigation is to formulate a new set of probabilities of leakages in piping systems of German pressurized water reactors for the whole range of pipes which are of interest using the operating experience, the principles of the basis safety approach and fracture mechanics studies.

1. Introduction

Since the revision of the postulates of pipe breaks in the guidelines of the "Reaktorsicherheitskommission" (RSK) /10/ in the Federal Republic of Germany (FRG) a number of nuclear power plants have been erected. The change in regulation could be implemented without any great difficulties. The new requirements resulted at the end into a

- uniformly high quality level of the components and piping
- simpler lay-out of the systems

which is expected to give an increase in reliability.

2. LBB Philosophy And Application

The change in the regulatory requirements in the FRG with respect to the design against postulated pipe breaks was presented and discussed in the past LBB seminars /9, 16/. For the sake of clarification the approach development in the RSK-Guidelines is summerized in table A and B. The related requirements for the component and system design are shown in table C.

The term 'break exclusion' as compared to the term 'leak-before-break' does express that a pipe break due to internal as well as external loads within the frame of the design loading and analyzed accident conditions can be excluded. The term leak-before-break is sometimes narrowed down to a certain behavior of crack growth under design loading conditions including only seismic exitation as external loads. Especially for medium size piping impact loads from plant malfunctions may introduce large damage and must be included in the evaluation.

Break postulates according to RSK-Guidelines

• main primary coolant piping • branch connections – surge line – RHR and ECCS-lines up to the first check-valve	break exclusions maximum opening area of 0.1 cross section (A)
• main steam line inside cont. • main feedwater line " "	
• main steam line outside cont. • main feedwater line " "	break due to external events (2A)
• safety and auxiliary systems O.D. ≥ 50 mm	break postulates selected according to criteria
• instrumentation, safety and auxiliary systems O.D. < 50 mm	break (2A)

TABLE A

BNNFS FIG. 1 06/03/87 17:20:15

Approach to determine break postulates according to RSK GL 4.2

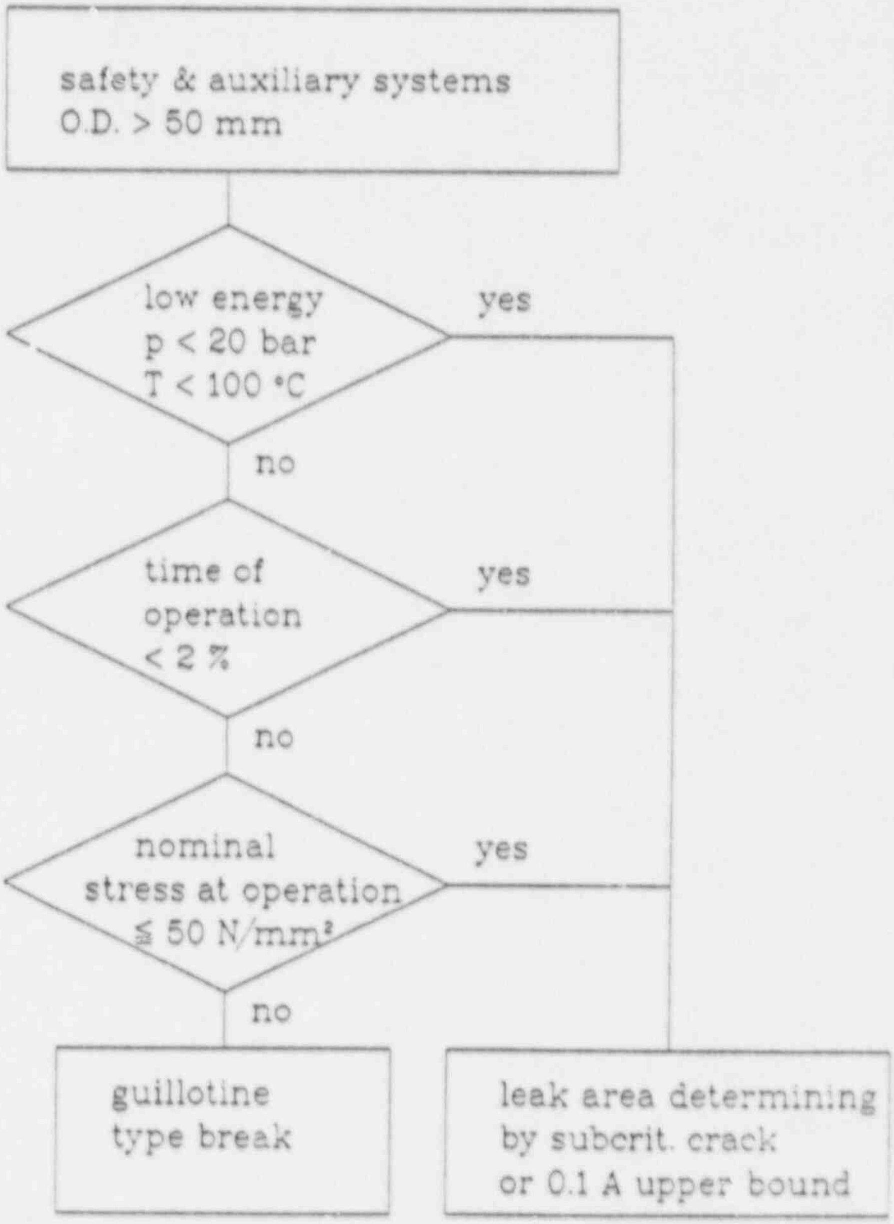


TABLE B

Requirements related to break postulates

	leak size
containment design	2A, c
compartment design	2A, c
system reactivity, shutdown margin	2A, c
flooding	2A, c
instrumentation qualification	for containment atmosphere
component support	F = 2 x pressure x A
design of internal structures	0.1 A (15 ms)
pipe whip	0.1 A
jet impingement	0.1 A

A = cross section of the pipe
c = circumferential

TABLE C

BNNE6 FIG. 1 06/08/87 15:33:12

3. Implications To Risk Studies

3.1 Introduction

The thermohydraulic analysis of incident sequences includes a number of various leakage sizes for the occurrence of which certain specific measures are laid down in order to cope with the incident. Table 1 is a list of the minimum requirements for the function of the system for residual heat removal in the case of leakages in a reference plant pipe system that contains primary coolant.

Thus, it is necessary within the scope of risk studies to establish not only the failure frequencies of active components, but also the probability of an occurrence of leakages or breaks of socalled passive components such as pipes.
Earlier risk studies USNRC /1/, GRS /2/ were based on a very coarse allocation of contributions of the occurrence probability of leakages of various pipes to the defined leakage sizes, confer Table 2. These postulates were based on the then operating experience in the United States with nuclear power plants and ships' reactors as well as the experience with piping systems in conventional plants.

From the point of view of structure and fracture mechanics, it is difficult to allocate leakages to a certain category of sizes, as the size of a leakage is dependent upon the stresses involved and may change in the course of the case under review. An unambiguous allocation is only possible if the full cross section of a pipe is uncovered as a result of a rupture. Thus, it is suggested to subdivide the pipes, including their branches, of the cooling system under review in accordance with the nominal widths concerned. The system layout is depicted in Figs. 1, 2 and 3 and need not be detailed here. With respect to the reference plant, the major systems and their nominal widths are shown in Fig. 4.

Separate investigations are required for the determination of leakage contributions resulting from failures of seals.

3.2 Influencing Factors and Methodical Possibilities

The probability of leakage formation and/or failure is influenced by a number of factors such as

- stresses,
- defects,
- fluid,
- design,
- material,
- manufacture, and
- testing.

A number of different methodical approaches are available for the determination of leakage and break probabilities in piping systems, e.g.

- statistical analyses of operating experience,
- probabilistic fracture mechanics studies,
- probabilistic assessment of limit bearing capacity,
- determination of the occurrence probabilities of individual failure mechanisms.

Within the scope of the investigations carried out, mainly the first two approaches were used (Fig. 5).

An explicit consideration of the various influencing factors is difficult, or even impossible, with respect to the methods referred to above. As far as the probabilistic methods of fracture mechanics are concerned, the distributions of crack and material characteristics that were used have a great influence. As a rule, dependencies which may exist are not taken into account. With respect to the statistical methods, it is often only possible to generate a qualitative comparability between the behaviour of different items since otherwise the reference quantity is restricted too much.

Compared with the nuclear data base used in USNRC /1/, much more comprehensive operating experience is available now. An evaluating survey of the American operating experience is contained in Bush /3/. Reports on cracks in German plants are contained in Rumpf /4/ and Miksch /5/ which deal in particular with the influence of the fluids concerned. The analyses of the operating experience with piping systems have confirmed the general knowledge that for certain equipment and material a pronounced damage may occur. Examples are the comprehensive damage resulting from intercrystalline stress corrosion cracking in primary coolant pipes made of unstabilized austenitic materials in boiling water reactors of US vendors, although the results cannot right away be applied to German plants by analogy, as other materials are used here (stabilized austenitic or ferritic steels). Possible damage to pipes of higherstrength grain refined steels in German plants was ruled out by an exchange of pipes Schulz /6/. The considerations indicate that a detailed review of the influencing factors is needed in order to define the reference quantities for statistical investigations of the applicability of the results.

With respect to damage to smaller pipes, there is a greater number of different causes. Wherever cracks have led to a rupture, vibrations due to flow initiation or valve opening and closing processes have often been contributing causes. The investigations carried out so far are not sufficient to determine significant differences between the different types of plants.

3.3 Bases for the Determination of Failure Probabilities

When determining leakage and failure probabilities of piping systems containing primary coolant, the leak cross sections listed in Table 1 have to be discussed. It follows from this Table that an intervention of the emergency core cooling and residual heat removal system is required in the case of leakage cross sections exceeding 2 cm^2. Apart from the piping sections that cannot be isolated inside the containment, the piping systems in the annulus have to be discussed as well, considering the reliability of the isolating devices.

In view of the great number of piping systems in a nuclear power plant, a differentiated determination of the leakage and failure probability of each pipe, in consideration of the influencing factors referred to before, will not make sense. The data base that would be needed for this purpose is not available at present. For the purposes of a risk study, a less differentiated classification of the piping systems was used in accordance with the following lines.

The classification uses three categories, viz. pipes of large nominal widths, pipes of nominal widths ≤ DN 25 and pipes of nominal widths < DN 25 ... > DN 250. For the large nominal widths, the principles of basic safety and the fracture mechanics analyses are used. The assessment of nominal widths ≤ DN 25 is effected, to a far-reaching extent, on the basis of the statistical investigations. For the intermediate range of nominal widths, additional working hypotheses are used.

In a first step operating experince of german PWR-s has been evaluated with respect to leaks and severances occurring in the structure. Leaking caused by damages of sealing or by a malfunction of fittings has not been the task of this study. In a second step, on the purpose of studying the mechanisms of damages occurring in such systems and to broaden the basis of data further operating experience of PWR-s in other western world countries has been scanned. The experience used is documented in

- Licensee Event Reports of the USNRC and
- the Data Bank of the OECD (IRS-Sytem).

In addition the results of pertinent research have been assessed.

The evaluation of these data suggests, that failures in small size piping are predominantly due to vibration of the piping. We conclude that the conditions of operating of such small size piping are very similar in primary coolant containing systems of all PWR-s that contribute to the entries of the data banks.

The situation for intermediate and large size piping is different. Damages here are determined mainly by the material used, the fabrication and testing standard and the operating conditions (e.g. water-chemistry) specific for the plant under consideration.

The broadening of the amount of data for nuclear piping damages beyond the german experience would be desirable, however it must be confined to small size piping. In our study the data bases mentioned before have been used only for the assessment of the probability of severances of small size piping in those parts of the primary coolant containing system that cannot be shut off from the main primary coolant reservoir.

3.3.1 Pipes of large nominal widths

As compared with the seventies, there is now much more information available, as a result of the experimental and theoretical research performed, to evaluate the failure behavior of the pipes of the primary coolant loops. Comprehensive and detailed reports on these aspects were submitted at the MPA seminars. Ref. Kussmaul /7/, Bartholomé /8/and Schulz /9/ contain the bases and prerequisites which have led to a new definition, in the RSK Guidelines /10/, of the breaks and leakages to be postulated in the incident analyses. Pursuant to RSK /10/, secondary protection measures in order to cope with leakages can be waived with respect to the primary coolant pipes inside the containment.
As far as operating experience with German pressurized water reactors is concerned, in-service inspections have not revealed any findings with respect to piping systems of large nominal widths.

Theoretical investigations on the basis of probabilistic fracture mechanics were carried out by various organizations IABG /11/, USNRC /12/, RWTUEV /13/ with respect to the derivation of failure and leakage probabilities of piping systems, with RWTUEV /13/ dealing especially with the conditions of the reference plant considered in the Risk Study. The methodical approaches and assumptions used in the studies are not discussed here. Various influencing factors such as material toughness, testing procedures, time between inspections, loading collective including external loads (earthquake) were investigated systematically. It should be underlined that, in all studies, the values of the probabilities of leakages are extremely small (10^{-8} ... 10^{-15} per plant and year) thus supporting the conclusion laid down in the RSK Guidelines.

Therefore, a calculatory value of 10^{-7} per year and plant is proposed for the treatment of breaks in large pipes within the scope of risk studies, since a further differentiation is not considered meaningful in the area of such small values.

3.3.2 Pipes of nominal widths ≤ DN 25

With respect to pipes of nominal width DN 25 or less, values of the occurrence probabilities of both leakages and breaks can be determined on the basis of statistical evaluations of the operating experience with German pressurized water reactors. However, the statistical material is insufficient for the derivation of any differentiated statement related to the various operating states. To determine the occurrence probabilities of severances of nominal value DN 25 piping (1") in addition to the German data base the aforementioned data bases can be used, since they contain reliable information on severances of piping of this size.

As a result of a check of these data bases there are zero occurrences of severances of piping of nominal values DN ≥ 25 for power operation. The inferences made here take into account in a conservative manner an occurence, which happened during a test in a PWR.

3.3.3 Pipes of nominal widths > DN 25 ... < DN 250

Based on the minimum requirements for the function of the system for residual heat removal, leakages and breaks in pipes of these nominal widths, which are connected with the primary coolant system and cannot be isolated, are of considerable relevance.

On the basis of an evaluation by GRS of the operating experience with German pressurized water reactors, a statistical survey of piping damage, in terms of systems and nominal widths, could be derived for these systems. The difficulty involved in the preparation of this statistical survey concerned the estimates of the quantities of the pipes of the various systems and various nominal widths in the PWR power plants on which the evaluation was based.

In the statistical material that is available, the number of leakages is small. Their cross section is << 1 cm^2. As no results are available for the break (rupture) of pipes, only zero defect statistics can be used to work on in almost all nominal width ranges which are of interest here. As a result of the short time of observation, these statistics would provide figures for the probability of a rupture which would be of the same order of magnitude as for the smallest leakages. To avoid using such unrealistic figures, working hypotheses were used for the derivation of reference quantities and the relationship between leakage and break probabilities.

As far as the definition of reference quantities was concerned, the general engineering experience was used that damage occurs in particular at nozzles, branches, bends and reductions, and especially at the connecting seams of these components. This fact was used as a working hypothesis. The dominance of these areas, as compared with straight pipes, can easily be explained, since both the forces inherent in the piping systems and the temperature stresses resulting from thermohydraulic mixing processes act upon these points, and additional influences on local stress conditions may result from basic manufacturing data (diameter and wall thickness allowances, material changes, abrupt wall thickness changes, etc.). This procedure is also in compliance with other investigations reported in literature USNRC /1/, Bush /3/.

Proportional numbers are frequently introduced for the description of the engineering experience that, compared with the entirety of all leakage events, only a small percentage relates to the full cross section of the pipe (break). In various statistical investigations of conventional pressurized components USNRC /1/, and ARGE TUEV /14/, proportional numbers ranging from 50 to 100 are found, with no classification as to specific nominal widths. Pipes of smaller nominal widths are not covered.

In view of the requirements for quality and quality assurance in nuclear facilities, as well as the leakage monitoring systems inside the containment, a global application of such values to the proportion between leakage and break probabilities over the entire range of nominal widths is considered as too conservative. For various reasons, it makes sense to introduce different categories of nominal widths. In view of the pipe legs to be investigated at the reference plant, the proportional numbers shown in Table 3 were used. For the derivation of these figures, basic values for pipes of small and large nominal widths were laid down on the basis of statistical evaluations and theoretical considerations.

In the case of pipes of small nominal widths, the dominant percentage of failures is caused by vibrations resulting in a rupture. As a rule, manufacturing reasons lead to the use of greater wall thicknesses than would be necessary to cope with the internal design pressure. This means that considerable crack propagations will be necessary before detectable leakages occur. Moreover, external influences (assembly stresses, consequential damage, etc.) are of greater importance in the case of pipes of small nominal widths. From these points of view, a proportional number of 10 for the ratio P Leak/B Break is appropriate. The factor 10 is in compliance with operating statistics.

For the range of large nominal widths, proportional numbers between 10^3 and 10^5 can be derived on the basis of various considerations. Corresponding examples are the investigations on the basis of fracture mechanics estimates carried out in Bartholome /8/ and RWTUEV /13/. For the present study, an upper basic value of 10^5 was chosen which is allocated to a nominal width of DN 1000.

For the determination of the proportional numbers in the range of nominal widths from DN 25 to DN 250 in the primary system the following quadratic relationship was used:

$$\frac{\text{P leakage}}{\text{P break}} = 9{,}6 \times \frac{DN}{25} + 0{,}4 \times {\frac{DN}{25}}^{2}$$

3.3.4 Allocation of leakage areas to failure causes

There are various possible approaches to determine the piping system sections which make contributions to the leakage cross sections contained in Table 1. A determination on the basis of the critical crack lengths resulting from various stresses cannot be justified because of the time and expense involved, and would not result in any material improvement in view of the overall accuracy of the statements that can be achieved. Starting out from the relationships demonstrated in Kussmaul /7/, and Bartholome /8/ between leakage opening and critical crack length, and the assessment of failure probabilities performed in RWTUEV /13/, a cutoff criterion of 2% of the cross-sectional area of the pipe was introduced as another working hypothesis (Fig. 6). This means that cracks in a pipe which cause leakage openings up to 2% ($f = 0{,}02$) of the cross-sectional area make a contribution to leakages. When 2% of the cross-sectional area are exceeded, the difference in probability between leakage and break is no longer considered to be significant in the investigation performed and the whole pipe cross section is regarded as being the leak areas. Fig. 7 illustrates this fact.

For the determination of scattering ranges for the probability of different leak cross sections it is necessary to vary this value (Table 3). The effect of f turns out to be significant only, in the leak range $2\ \mathrm{cm}^2\ F \leqq F \leqq 12\ \mathrm{cm}^2$.

3.4 Results and Conclusions

Starting out from the bases and working hypotheses described in Section 3, the occurrence probabilities of leakages and breaks of pipes were determined for a PWR reference plant. The results concerning the area inside the containment are compiled in Table 5. Additional investigations were carried out for piping systems in the annulus. The reference quantities concerned were determined on the basis of system plans, isometric drawings and a limited number of inspection rounds. Confidence intervals were determined for the relevant area.

The following conclusions can be drawn from the work carried out:

- The primary coolant pipes do not make any relevant contribution to the various leakage cross sections.
- Leakage contributions from connecting pipes and breaks of pipes of smaller nominal widths are the decisive factors.

In addition, the following statements can be made:

- The worldwide operating experience with light-water reactors is only of limited use as a data source, since, due to differences in materials and designs, the failure mechanisms that occur are partly typical for certain reactor systems.
- The available operating experience with German nuclear power plants shows only a small number of leakage events. However, the data base is small so that large scattering ranges result.
- The working hypotheses chosen lead to consistent results. Further differentiations and verifications may be necessary for a narrowing down of the scattering ranges.

TABLE 1.

MINIMUM REQUIREMENTS FOR AVAILABILITY OF SYSTEMS FOR RESIDUAL HEAT REMOVAL IN CASE OF LEAKAGE OF PIPING CONTAINING PRIMARY COOLANT

leak cross sectn. (cm^2)	System functions required					
	high-pressure injection	injection by pressure accumula-tors	low pressure injection for flooding	low-pressure injection sump operation	admissible delay of secondary side shutdown	feed water supply
> 500	–	–	1	1	∞	–
200–500	1	–	1	1	∞	–
300–500	–	2	1	1	∞	–
80–200	3 or 4	–	2	2	∞	–
	2	–	1	1	60	1 mean feedwater supply or 2 emergency feedwater supply
	1	–	1	1	30	
50–80	2	–	1	1	60	
	1	3	1	1	60	
	1	–	1	1	30	
25–50	2	–	1	1	90	
	1	–	1	1	60	
2–25	1	–	1	1	> 120	
	–	–	1	1	30	

large leak
medium-sized leak
small leak

TABLE 2.

RATE OF OCCURRENCE OF LEAKAGES AT PIPING CONTAINING PRIMARY COOLANT (ACCORDING TO WASH-1400 AND THE GERMAN RISK STUDY, PHASE-A)

Rate of occurrence	per plant and year
very small leak (2 – 80 cm^2)	$1 \cdot 10^{-4} - 1 \cdot 10^{-2}$
medium-sized leak (80 – 400 cm^2)	$3 \cdot 10^{-5} - 3 \cdot 10^{-3}$
large leak (> 400 cm^2)	$1 \cdot 10^{-5} - 1 \cdot 10^{-3}$

Table 3

Quotient P leak/P severance used in calculations for primary coolant circutit

DN	P leak/P severance
25	10
50	25
80	25
100	50
150	50

Table 4

Comparison of mean values of the occurence rate of a leak of size 2 $\text{cm}^2 \leq F_{\text{leak}} \leq 12\ \text{cm}^2$ when making different assumptions about the critical leak area $F_{\text{crit}} = f \cdot F_{\text{pipe}}$

f	leak	leak that cannot be shut off	leak that can be shut off once	leak that can be shut off double-fold
0,02	crack DN ≧ 100	4 E-5	1,5 E-4	1,1 E-4
	severance DN 25	1,7 E-3	1,3 E-3	4,2 E-3
	total	1,7 E-3	1,5 E-3	4,3 E-3
0,1	crack DN ≧ 50	1,3 E-3	2,8 E-4	1,1 E-3
	severance DN 25	1,7 E-3	1,3 E-3	4,2 E-3
	total	3,0 E-3	1,6 E-3	5,3 E-3

Table 5

Statistical paramaters of various contributions to the occurence rate of leaks of different sizes inside the containment and the reactor building respectively (PWR)

leak area [cm]^2	leck category		leak that cannot be shut off	leak that can be shut once	leak that can be shut off double-fold
< 2	crack or break DN 15	λ	1,5 E-1	1,2 E-1	1,3 E-1
		λ_{95}	2,6 E-1	2,0 E-1	2,2 E-1
2-12	crack	λ	4 E-5	1,5 E-4	1,1 E-4
	break DN 25	λ	1,7 E-3	1,3 E-3	4,2 E-3
	total	λ	1,7 E-3	1,5 E-3	4,3 E-3
		λ_{95}	4,4 E-3	2,5 E-3	7,8 E-3
12-25	crack	λ	4 E-7	-	1 E-6
	break DN 50	λ	1,7 E-4	8,7 E-5	5,0 E-4
	total	λ	1,7 E-4	8,7 E-5	5,0 E-4
		λ_{95}	6,5 E-4	1,6 E-4	9,0 E-4
25-80	crack	λ	< 1 E-7	-	-
	break DN 80	λ	5,7 E-5	1,6 E-4	5,0 E-4
	break DN 100	λ	9,6 E-6	8,2 E-5	-
	total	λ	6,7 E-5	2,4 E-4	5,0 E-4
		λ_{95}	2,3 E-4	9,1 E-4	9,0 E-4
80-200	crack	λ	< 1 E-7	-	-
	break DN 125		-	2,6 E-5	1,0 E-4
	break DN 150	λ	1,4 E-5	-	-
	total	λ	1,4 E-5	2,6 E-5	1,0 E-4
		λ_{95}	5,3 E-5	9,9 E-5	3,8 E-4

contd. next page

Table 5 contd.

200-400	crack or	λ	< 1 E-7	-	-
	break DN 250	λ	< 1 E-7	< 1 E-7	< 1 E-7
	total	λ	< 1 E-7	< 1 E-7	< 1 E-7
		λ_{95}	< 1 E-6	< 1 E-6	< 1 E-6
> 400	leak or	λ	< 1 E-7	-	-
	break				
	DN ≧ 300	λ	< 1 E-7	-	< 1 E-7
	total	λ	< 1 E-7	-	< 1 E-7
		λ_{95}	< 1 E-6	-	< 1 E-6

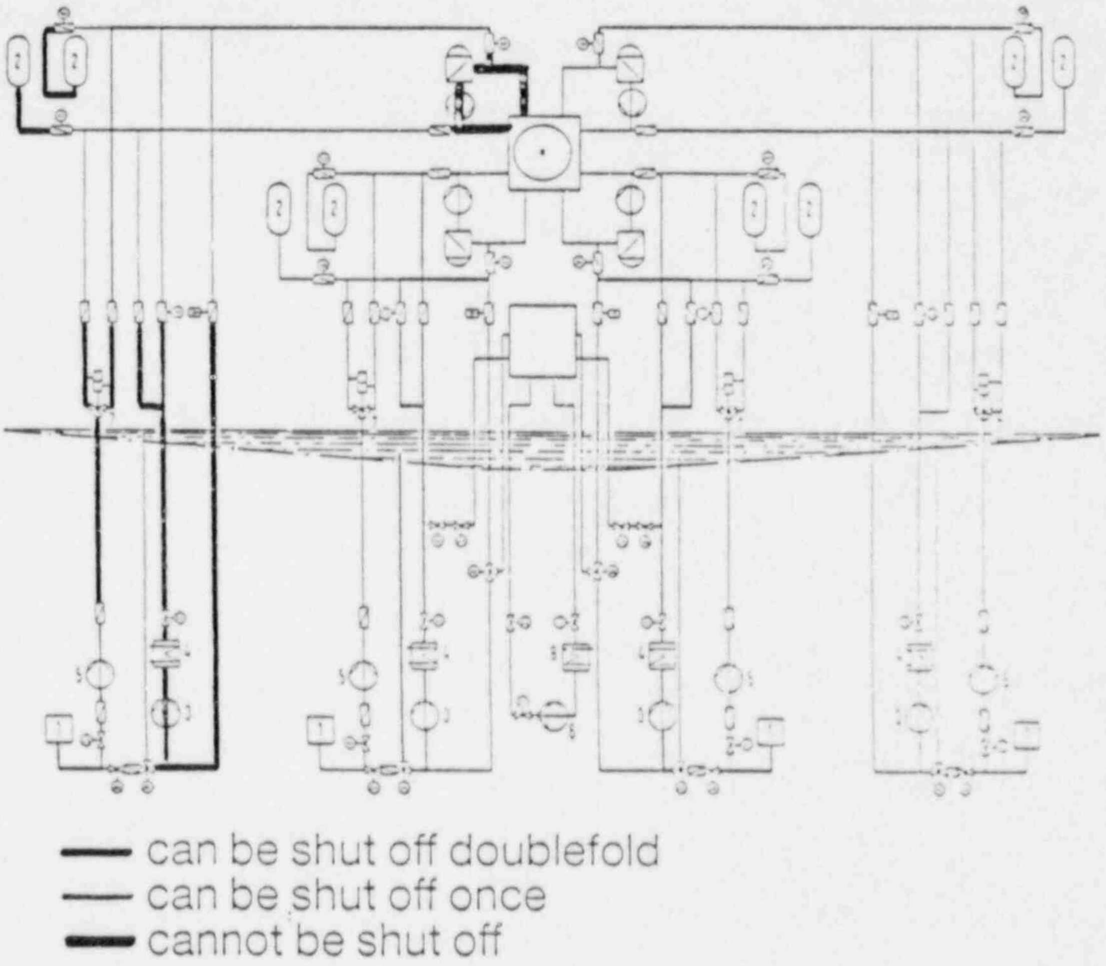
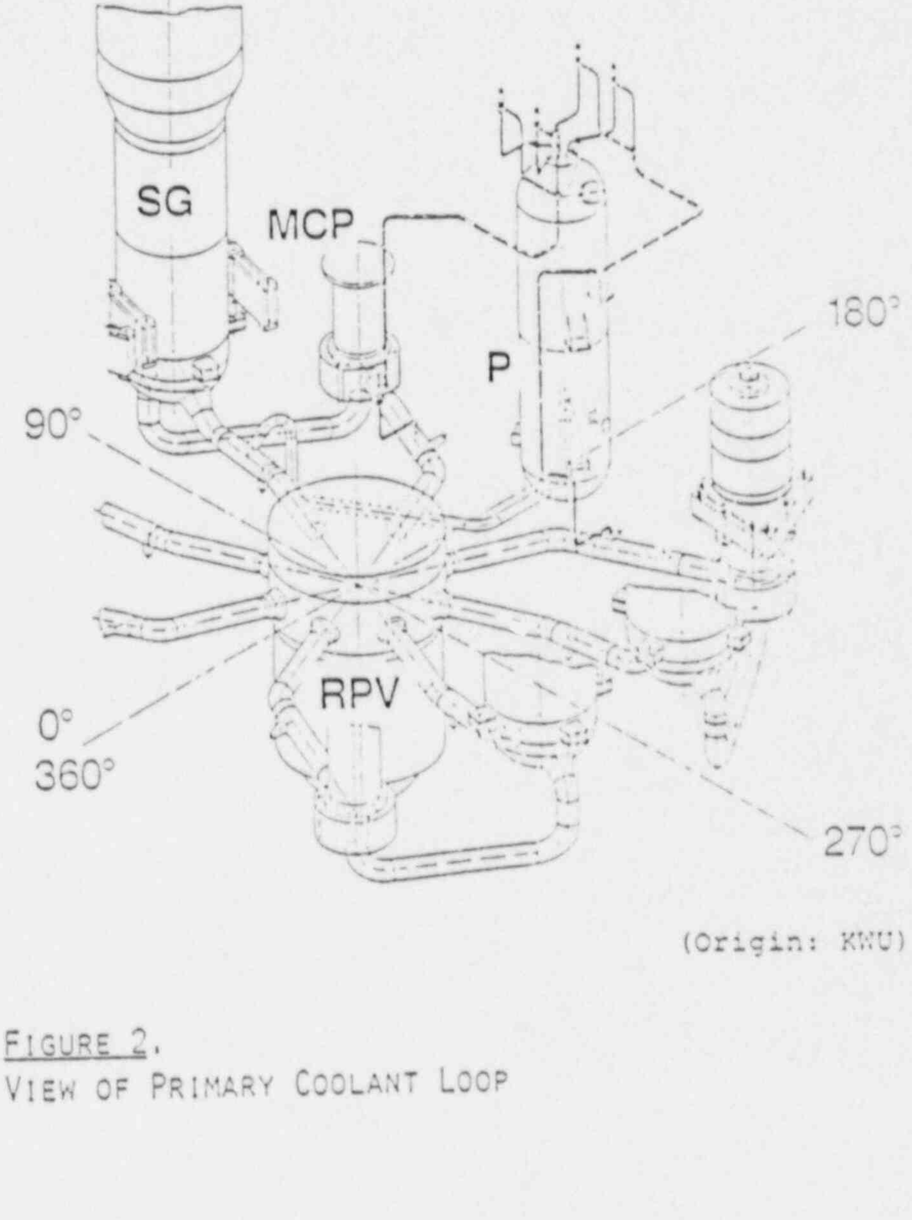


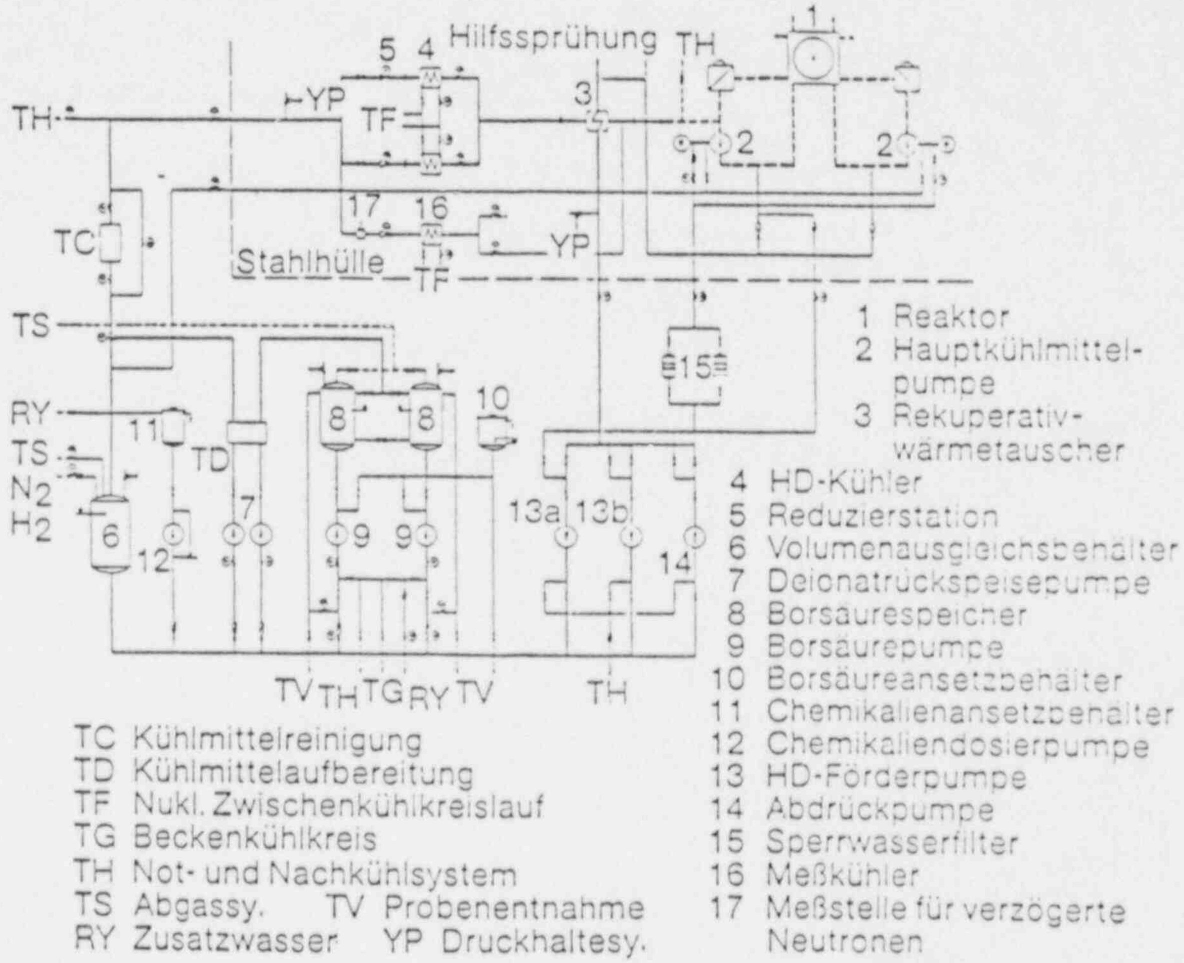
Figure 1.

Emergency Core Cooling (ECCS) and Residual Heat Removal (RHR) System



(Origin: KWU)

FIGURE 2.
VIEW OF PRIMARY COOLANT LOOP



(Origin: KWU)

FIGURE 3.

PRIMARY COOLANT VOLUME CONTROL SYSTEM

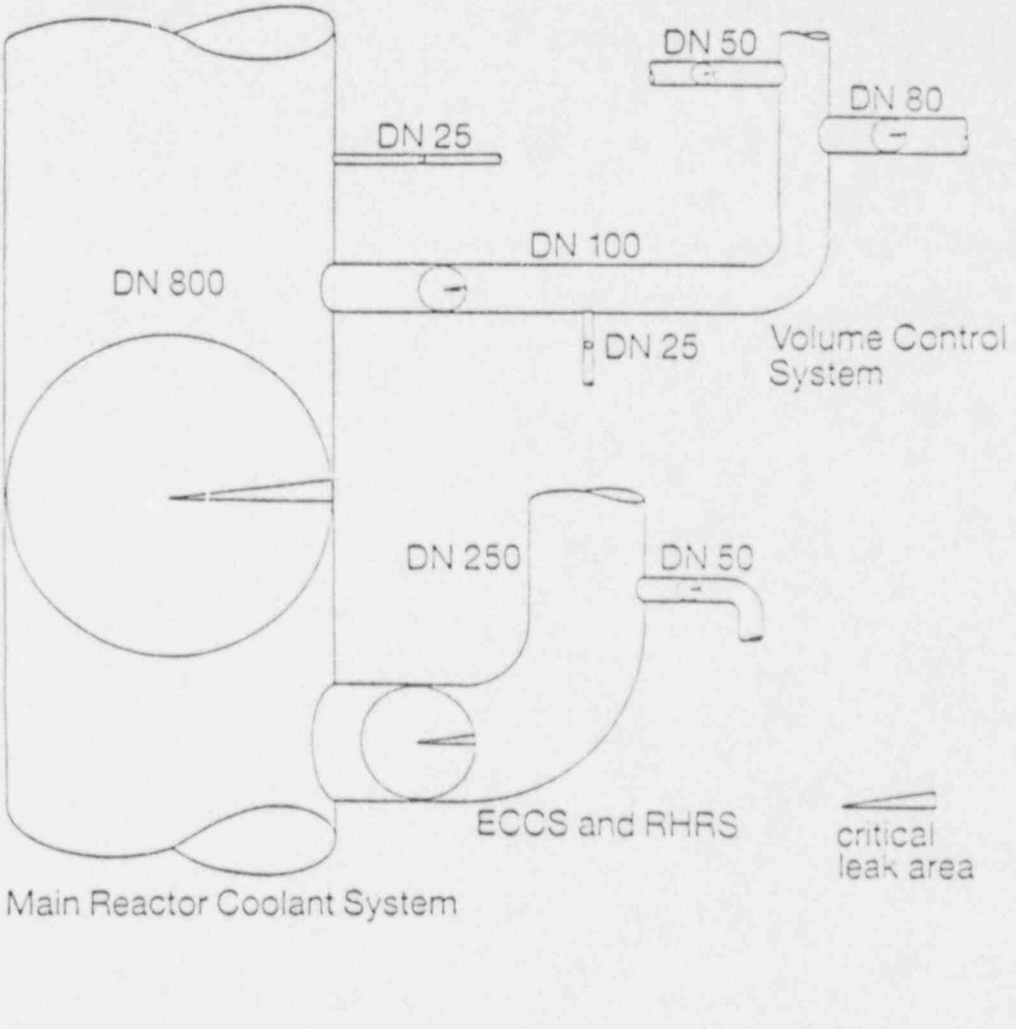
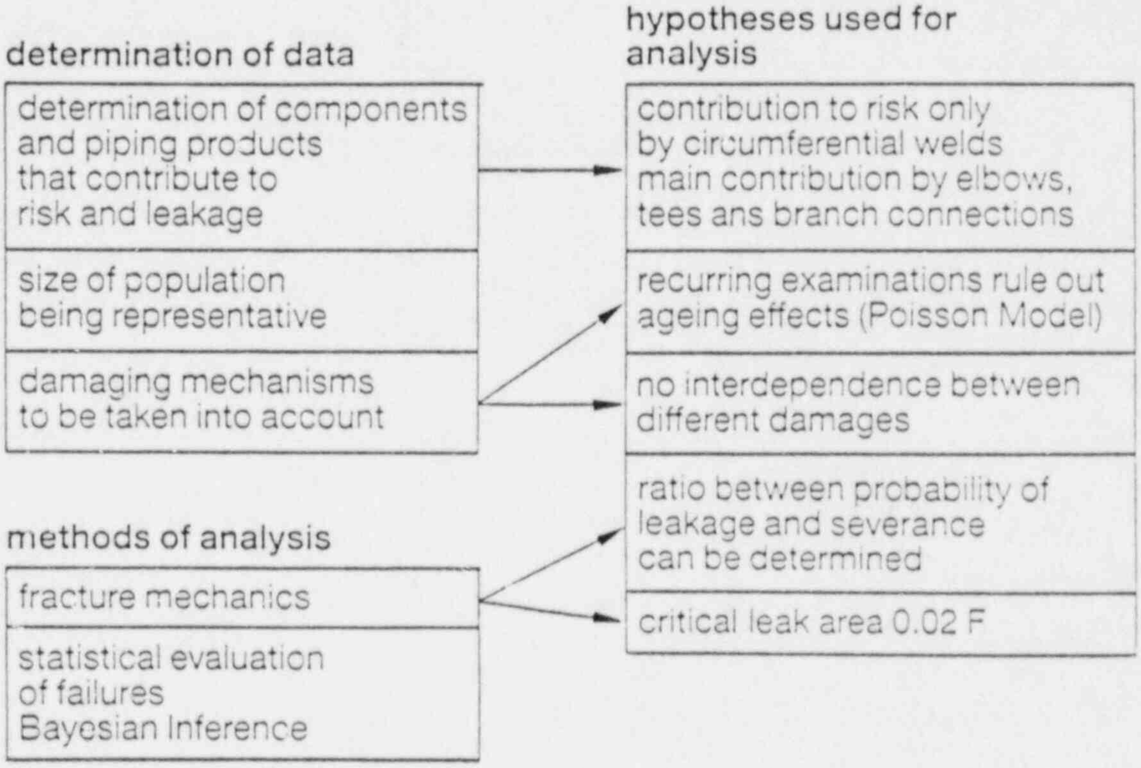


FIGURE 4.
NOMINAL DIAMETERS OCCURRING IN DIFFERENT PRIMARY-COOLANT-CONTAINING SYSTEMS



<u>FIGURE 5</u>.
DETERMINATION OF DATA. HYPOTHESES AND METHODS APPLIED.

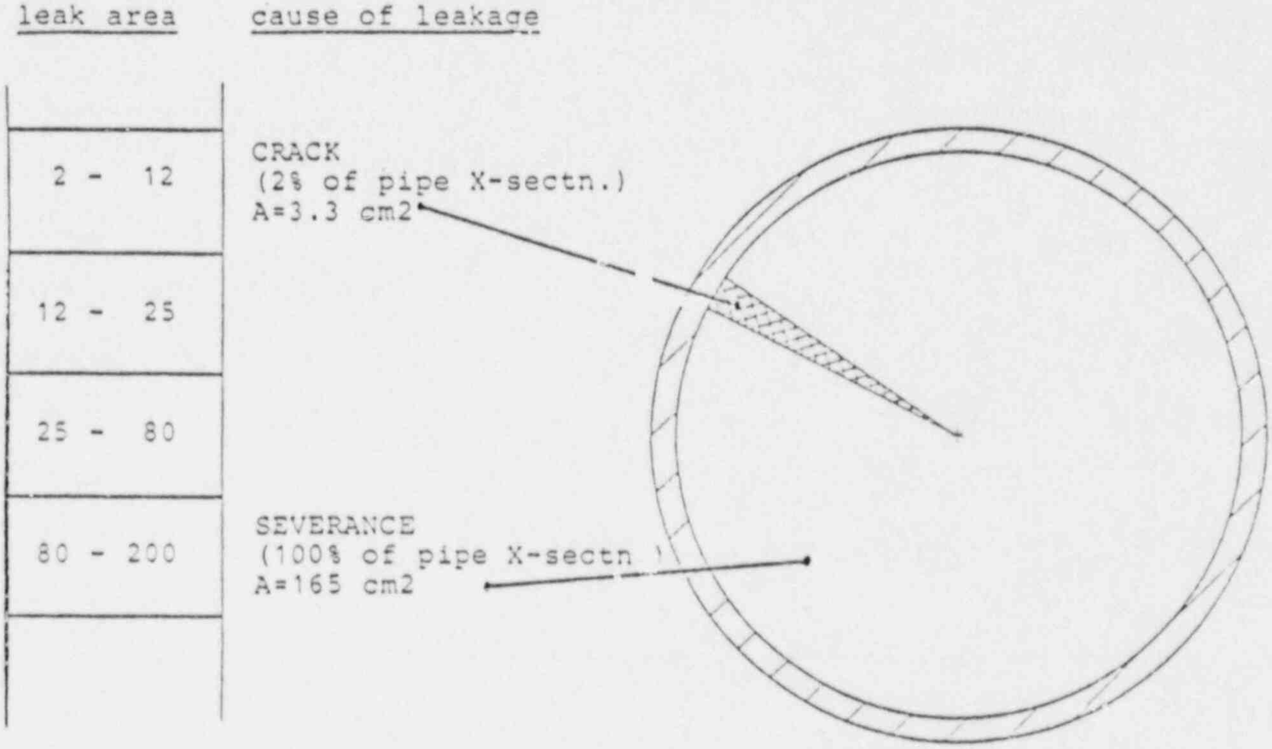
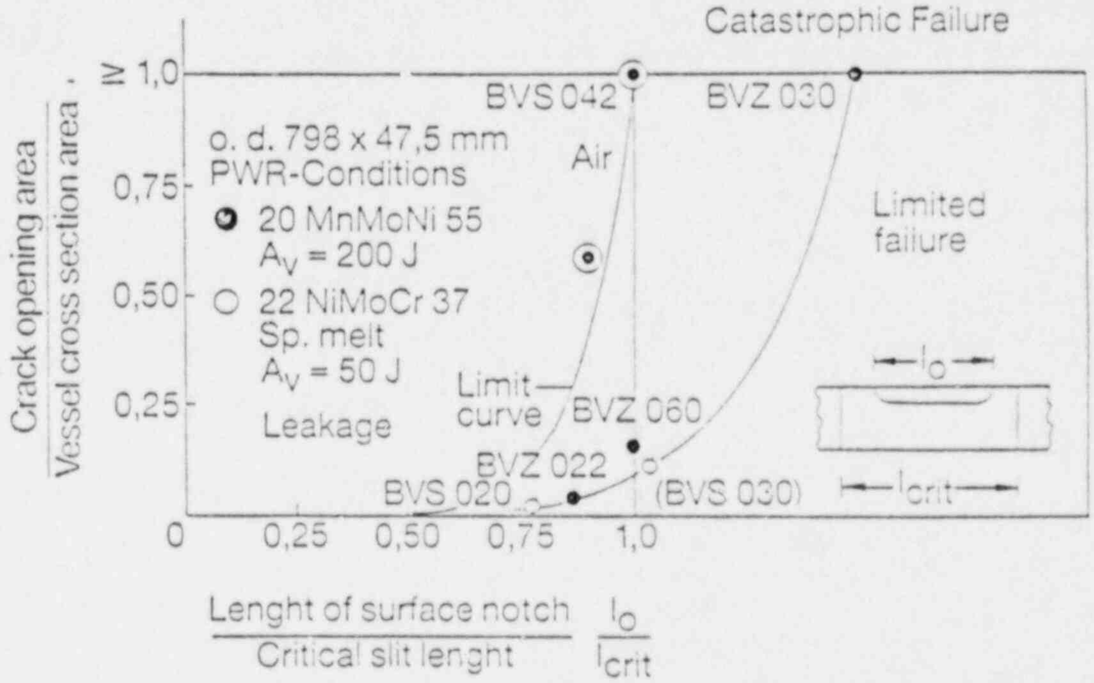


Figure 6.
Relation of possible Leak areas of a Pipe to the Leak Size-Classes Defined by the Needs of Accident Analysis.
(Example: Nominal Diameter 150 mm)



(Origin: MPA)

FIGURE 7.

EXPERIMENTAL EVIDENCE OF FAILURE MODES (FOR AXIAL CRACKS)

4. Further Subjects Of Research

The results of the extensive research on fracture mechanism in the past have supported the increasing acceptance of the LBB approach. A major part of the LBB supporting reseach is presented in this seminar by D. Sturm.

For a more detailed understanding and determination of safety margins future research subjects devoted to LBB are shown in table D.

Further subjects of research

- component testing for crack growth under environmental conditions
- crack opening behavior in elbows and branch connections
- leak rates at transient loading and operating conditions
- continuous evaluation of operating experience
- generic evaluation for load following operation with respect to
    - loads
    - water chemistry
    - operator errors

TABLE D

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/4/ H.G. Rumpf, O. Schadel and W. Hübner, Schäden an dünnwandigen Komponenten von Kernenergieanlagen, Technische Überwachung 22 (1981) 335-338.

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RS 217, June 1978

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Current Status Regarding Policy Making on "Leak Before Break" in Japan

T. Fukuzawa

MITI

A joint program, ongoing since 1975, was implemented by MITI, electric utilities, and reactor vendors to improve and standardize LWR plants. In this context, MITI started a review of LBB related matters in 1984 including information obtained through R&D and operating experiences, and information on policy making on this matter in foreign countries.

This review is being done by a task group of technical advisors on reactor safety and specialists from utilities, vendors, and R&D organizations.

The task group has been reviewing the applicability of the LBB concept to the austenitic stainless steel piping that forms the reactor pressure boundaries of LWRs since 1984. The group has also been reviewing the applicability of the same concept to carbon steel piping since 1985.

The above review on austenitic stainless steel includes:

(A) applicability of the concept

(B) design measures to prevent piping break

(C) impact on safety evaluation.

Concerning Item (A) above, recent studies on crack propagation show that the LBB concept is applicable to BWR and the PWR piping. These studies are on welding defects of steel piping, detection limits of crack, and probability of cracks of various sizes. However, the above conclusion is based on the presumption that (1) selection of piping material, design, fabrication, shipping tests, and inspection shall be done in accordance with technical standards and codes approved by MITI; (2) proper measures to prevent SCC shall be conducted or proper materials shall be selected to prevent SCC; (3) leak detection is possible with existing equipment; and (4) in service inspections shall be carried out in accordance with existing standards.

Concerning Item (B) above, the review group, based on the presumption that the application of the LBB concept has no impact on (1) the engineered safety features, (2) the emergency shutdown systems, and (3) the containment, advised that the size and location of the break be assumed on a best estimate basis.

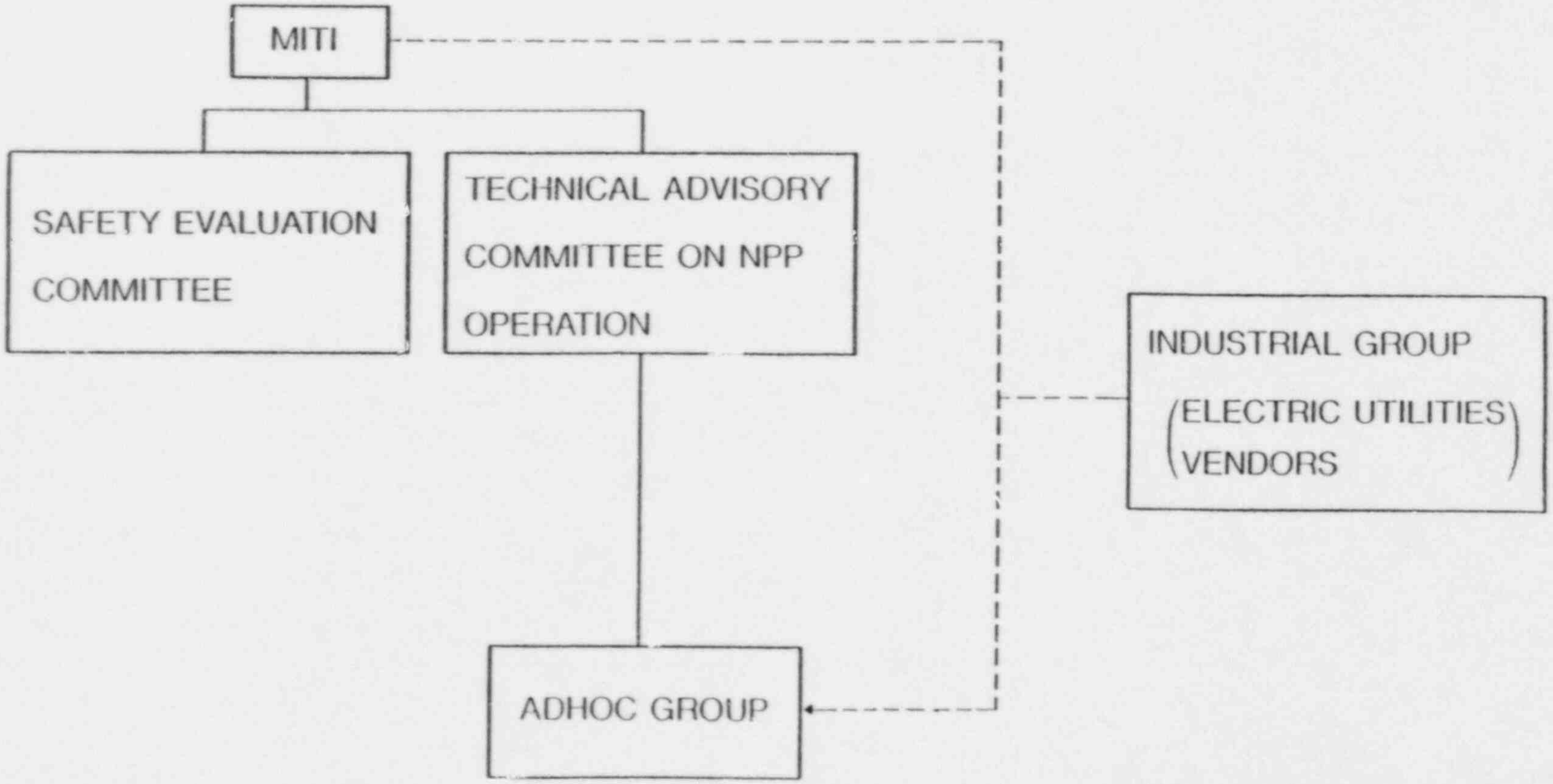
A review concerning Item (C) above is still ongoing, and conclusions and advice have not been drawn. However, it is certain that the group will give advice on Item (C) based on results of the reviews of Items (B) and (C). It is anticipated that piping, such as ECCS piping, will be excluded from application of the LBB concept. It is probable that application of the concept will substantially reduce the dynamic loads of pipe whipping, jet impingement, and the pressure imbalance in the vessel cavity.

SCHEDULE OF ESTABLISHING LBB CRITERIA

- STAINLESS STEEL PIPING
  - MAY, 1984 : SAFETY EVALUATION COMMITTEE WAS ORGANIZED
  - DEC. 1984 : ADHOC COMMITTEE WAS ORGANIZED
  - MARCH, 1986～ : NEW LBB CRITERIA PROPOSED BY ADHOC COMMITTEE
  - APRIL, 1986 : TECHNICAL ADVISARY COMM. ON NPP OPERATION REVIEW
  - MAY, 1986～ : SAFETY EVALUATION COMM. REVIEW [DEVELOPING MITI REGULATORY GUIDE REFLECTING LBB CONCEPT ONLY FOR STAINLESS STEEL PIPING]

- CARBON STEEL PIPING
  - APRIL. 1985 : LBB VERIFICATION TEST (C/S) STARTED
  - MARCH. 1988 : LBB VERIFICATION TEST (C/S) WILL BE COMPLETED
  - END OF 1988 : MITI REGULATORY GUIDE ON LBB DESIGN CRITERIA WILL BE ISSUED (FOR BOTH S/S AND C/S)

ORGANIZATION TO DEVELOP DESIGN CRITERIA
(AUSTENITIC STAINLESS STEEL PIPING IN CLASS 1 SYSTEM)



MITI

SAFETY EVALUATION COMMITTEE

TECHNICAL ADVISORY COMMITTEE ON NPP OPERATION

INDUSTRIAL GROUP
(ELECTRIC UTILITIES
VENDORS)

ADHOC GROUP

REVIEW ITEMS FOR DEVELOPING LEAK-BEFORE-BREAK CONCEPT

- VERIFICATION OF LBB CONCEPT
- DEVELOPING PROTECTION CRITERIA AGAINST DYNAMIC EFFECTS RESULTING FROM POSTULATED PIPING FAILURES
- SAFETY EVALUATION FOR PROTECTIVE DESIGN IN CONSIDERATION OF LBB CONCEPT

MAJOR PREMISE OF LBB VERIFICATION

- PIPINGS IMMUNE TO IGSCC
- QUALITY CONTROL FOR DESIGN, MATERIAL SELECTION, FABRICATION, INSTALLATION, TEST AND INSPECTION BASED ON PRESENT REGULATION, GUIDELINES, ETC.
- LEAK DETECTION SYSTEM BASED ON PRESENT REQUIREMENTS
- NO CREDIT FOR ISI CONSERVATIVELY

Design Criteria for Austenitic Stainless Steel Pipes (BWR)

Pipe Size (B)	2	4	6	10	14	16	20	24
Outer Dia: Do (mm)	60.5	114.3	165.2	267.4	355.6	406.4	508.0	609.6
Pipe Wall Thickness : t (mm)	5.5	8.6	11.0	15.1	19.0	21.4	26.2	31.0
Leakage Crack Length 2 θ (deg.)		25.14	—	—	—	—	—	—
Stress Limit for Crack Stability (×Sm)		2.55	2.82	2.81	2.81	2.81	2.80	2.80

NOTE: Stress limit for crack stability includes membrane and bending stresses.

Sm is design stress intensity ;12.1kg/mm**2.

Design Criteria for Austenitic Stainless Steel Pipes (PWR)

Pipe Size (B)	11/2	2	4	6	10	14	Primary Coolant Loop Piping		
Outer Dia: Do (mm)	48.6	60.5	114.3	165.2	267.4	355.6	836.0	882.0	943.0
Pipe Wall Thickness : t (mm)	7.1	8.7	13.5	18.2	28.6	35.7	68.75	72.7	77.8
Leakage Crack Length 2 θ (deg.)	77.88	—	—	—	—	—	—	—	—
Stress Limit for Crack Stability (×Sm)	1.84	2.44	2.70	2.73	2.71	2.71	2.69	2.68	2.65

NOTE: Stress limit for crack stability includes membrane and bending stresses.
Sm is design stress intensity ;11.7kg/mm**2.

PROTECTIVE DESIGN AGAINST POSTULATED PIPING FAILURE

- FUNDAMENTAL CONSIDERATION

  TO MAINTAIN FUNCTIONAL CAPABILITY OF ;

  –REACTOR CONTAINMENT FACILITY

  –ENGINEERED SAFEGUARDS AND THOSE RELATED SYSTEMS

  –REACTOR SAFE SHUTDOWN SYSTEMS

- DESIGN CRITERIA

  TO CONTAIN ;

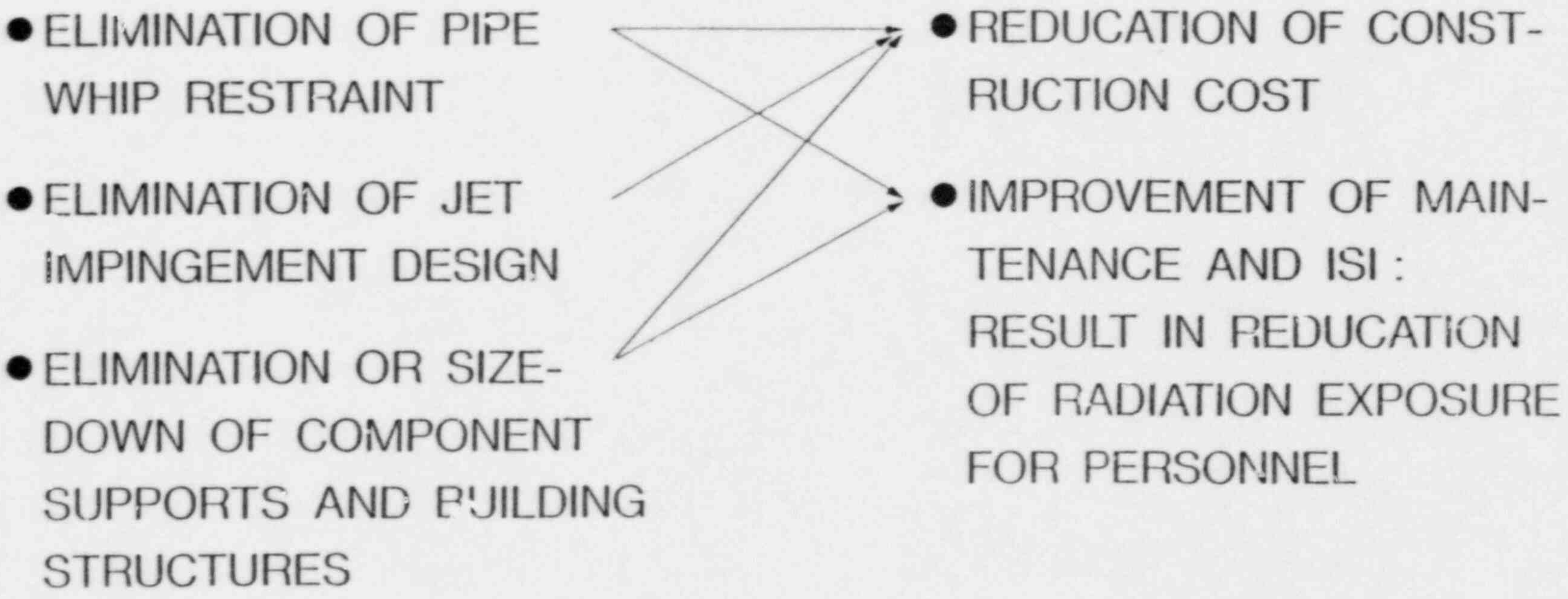
  –PIPINGS IN FLUID SYSTEMS TO BE POSTULATED OF FAILURES

  –POSTULATED FAILURE LOCATIONS, TYPE, SIZE etc.

DEVELOPMENT OF RATIONALIZED DESIGN CRITERIA

- APPLICATION OF "LEAK-BEFORE-BREAK" CONCEPT TO ELIMINATE :
  - INSTANTANEOUS PIPE BREAKS AS DESIGN BASES
  - THEIR CONSEQUENCES AS DYNAMIC EFFECTS
- CONTAINMENT AND ECCS DESIGN REQUIREMENTS, AND ENVIRONMENTAL EQUIPMENT QUALIFICATION BASIS UNCHANGED

RATIONALIZED DESIGN CRITERIA ACCOMPLISHES



- ELIMINATION OF PIPE WHIP RESTRAINT
- ELIMINATION OF JET IMPINGEMENT DESIGN
- ELIMINATION OR SIZE-DOWN OF COMPONENT SUPPORTS AND BUILDING STRUCTURES

- REDUCATION OF CONSTRUCTION COST
- IMPROVEMENT OF MAINTENANCE AND ISI : RESULT IN REDUCATION OF RADIATION EXPOSURE FOR PERSONNEL

SESSION 2: RECENT RESEARCH PROGRAM ON LBB (PART 1)

Chairman: M. Mayfield, U.S. NRC, U.S.

Recent Developments in the

APPROACH TO LEAK BEFORE BREAK BASED ON WORK IN THE UNITED KINGDOM

B J Darlaston
Berkeley Nuclear Laboratories
TPRD
Central Electricity Generating Board
Berkeley
Gloucestershire GL13 9PB
United Kingdom

This paper includes contributions from
Mr R A J Hellen - CEGB BNL,
Dr I Milne - CEGB CERL,
and Dr D G Hooton - NNC Risley

MARCH 1987

SYNOPSIS

Various aspects of leak-before-break have been studied within the CEGB and other U.K. organisations over many years. The work has ranged from full size vessel and pipe tests to the study of crack shape development in small test pieces. More recently the work has focussed on the formulation of a leak-before-break procedure to be included in the R6-CEGB Procedure for the Assessment of Defects (Milne, 1986), and the wider application of the concept to plant assessment.

This paper provides three examples of recent work in the U.K. Studies on surface cracks are aimed at determining the stability of the crack following break through whilst in the applications field, the concept of Leak-before-break has been successfully pursued for a reaction bellows unit. The final example is on the development of an assessment route for the fast reactor primary vessels.

INTRODUCTION

The concept of leak-before-break arises when it is possible to evaluate the boundary between those defects which give rise to a leak type failure and those leading to a disruption or break type failure. This situation may change with time as a result of material degradation and other aspects. Figure 1 indicates that one of several results can be obtained for a leak-before-break prediction and these differ in the number of safeguards against the break.

The emphasis of work in support of the concept varies depending on the attitude of the licencing authority and the needs of the user. As reported at the Seminar in 1985, at Columbus, Ohio (USNRC 1985), in some countries the concept is widely used whilst in others there is only a limited application.

Various aspects of leak-before-break have been studied within the CEGB and other U.K. organisations over many years. The work has ranged from full size vessel and pipe tests to the study of crack shape development in small test pieces. More recently the work has focussed on the formulation of a leak-before-break procedure to be included in the R6-CEGB Procedure for the Assessment of Defects (Milne, 1986), and the wider application of the concept to plant assessment.

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STUDIES ON SURFACE CRACKS (MILNE)

An important component in a leak-before-break analysis is the crack length on breakthrough. This determines whether or not the through-thickness crack will remain stable, and whether or not the leak will be detected. The situation is complicated by the mechanism of breakthrough, breakthrough during fatigue resulting in a different situation than breakthrough under monotonic load. In the latter case the breakthrough may occur due to stress corrosion cracking mechanisms (including intergranular attack) or due to the application of a fault load. In all cases the crack shape development and the cracking pattern must be capable of being predicted to a reasonable level of accuracy.

Pressure Vessel Tests

The CEGB has sponsored a number of pressure vessel tests to validate the application of their fracture mechanics code, R6 (Milne, 1986). These involved the pressurisation of a series of test veseels each containing a semi-elliptical surface crack experiencing the hoop stress of the vessel. To date the tests have been performed at temperatures where the vessel material was ductile, and a leak-before-break analysis was employed in the design of the test, to avoid catastrophic failure. The tests were used for a collaborative exercise in predicting ductile instability under the auspices of the European Fracture Group, and the first of these exercises has been reported by Knee and Milne (1986).

In this particular test, time dependent plasticity prevented the vessel from being pressurised to a leaking condition. Nevertheless it was possible to make statements about the developing shape of the crack, and also about the capability of the fracture mechanics and the participants in the collaborative exercise to predict the cracking events. These statements remained unchanged in principle when evaluating the remaining tests, which were all pressurised to a leaking condition, as evidenced by the following conclusion (Knee, 1986):

> "It was not possible to predict the development of the final crack shape at leakage: this will be unimportant for an integrity assessment for which only a small amount of crack growth is permitted, but it may be significant if leak-before-break arguments are to be constructed".

In the leak-before-break analysis for the validation tests, a pessimistic estimate of the through-thickness crack length was used to demonstrate failure avoidance. However, crack penetration occurred only over a small region of the ligament, so leak rates could not be assessed with any confidence.

Future Programme

Following from this work a collaborative study is being initiated involving work at two CEGB Laboratories, Berkeley Nuclear Laboratories (BNL), and Central Electricity Research Laboratories (CERL), and at IWM Freiburg, under the auspices of the Commission of European Communities, with the following objectives:

(a) Validation of assessment methods for surface defects.

(b) Improving the confidence of J-estimates for surface defects.

(c) Application of conventional specimen tests to the assessment of surface defects.

This will involve a study of existing methods for estimating J and its variation around the crack front and an examination of well defined experiments. It is hoped to develop a better understanding of the factors which control the shape changes in a growing surface crack and to develop improved methods of prediction.

Approval and financial support for the work has been obtained and the project is expected to begin in the near future.

AN APPLICATION OF LEAK-BEFORE-BREAK PRINCIPLES (HELLEN)

An axial defect was postulated on the inner surface of a convolution bend of a reaction bellows in a gas cooled reactor.

A leak-before-break argument in support of the safety case was developed to demonstrate the application of leak-before-break procedures.

Details of the Defect

It was assumed that the defect was located at the first convolution bend from the weld to the end skirt (Figure 2), and was 2mm deep along most of its length with a central section 4mm deep.

Cyclic Stresses

The source of cyclic stress that is of most concern is vibrational. Theoretical analyses indicated that a number of resonant modes existed at and below gas circulator speed. The assessment concentrated on the off-resonant vibrational stress levels on the assumption that on-load monitoring would be pursued.

To account for possible errors in the calculated vibrational stress levels, the values deduced were increased by 20%.

Table 1 : Typical Calculated Vibrational Hoop Stress Levels (MPa)

Operating condition	Stress range $\Delta\sigma$ rms t.a.	1.2 x Stress range $\Delta\sigma$ rms t.a.
Off-resonance	0.33	0.40
1680 rpm resonance	5.32	6.38
1900 rpm resonance (most probable mode)	1.91	2.29

The theoretical spatial pattern of the vibrational stresses in the region of the defect were quite complicated. For example, at resonance, in the region of the crack tip growing up towards the crown of the convolution (crack tip A Figure 2), the vibrational stresses were about an equal mixture of membrane and through-wall bending, whereas at tip B they were mainly through-wall bending. At a lower resonance, tip A was mainly in a bending vibrational stress field whereas tip B was in a marginally bending field.

In order to predict the fatigue flaw shape that developed and the consequent defect semi-length at penetration, it was necessary to know the proportion of bending to membrane fatigue stresses. In the present case the problem was bounded by assuming the vibrational stresses to be either pure bending or pure membrane.

Static Stresses

Finite element analyses showed that the stresses at normal operating conditions due to the internal pressure of 1.85 MPa were predominantly tensile, although there was a through-wall bending component whose sign changes along the length of the existing defect. The static stresses were pessimistically assumed to be a uniform tensile stress of 100 MPa. It was also necessary to calculate the critical defect length under the safety valve lift pressure of 2.18 PMa, which gave rise to a membrane stress of 118 MPa.

Tensile Properties

At the operating temperature of 220°C, the 0.2% Proof Stress for the plate material was 640 MPa. The weld metal 0.2% Proof Stress was 460 MPa.

Fracture Toughness

The initiation toughness K_I both mean and lower bound values, are given below. Also given are values for 0.2mm of stable tearing, as permitted by the CEGB toughness testing procedure. They are considered here to be the most relevant for a Leak-before-Break case.

Fracture Toughness Values for Convolution Material (MPa√m)
(Temperature = 220°C)

	Mean	Lower Bound
Initiation toughness, K_I	85.3	57.6
Tearing toughness, K(0.2mm)	110.6	94.3

Stress Intensity Factor

The deduction of the correct SIF function for the present problem was not simple. An accurate value could only be assured by 3D Finite Element Modelling. Therefore, modelling assumptions were used to bound the problem. The lower bound was the Centre Cracked Plate Model and the upper bound was judged to be the Circumferentially Defect Pipe under Tension.

The Centre Cracked Plate Model (CCPM)

The SIF was given by:

$$K = Sm\sqrt{\pi c} \qquad (1)$$

where Sm was the applied static tensile stress.

The circumferentially Defective Pipe Model (CDPM)

The SIF was given by:

$$K = Sm\sqrt{\pi c} \quad F(c/\sqrt{rt}) \qquad (2)$$

where $F(c/\sqrt{rt})$ was the SIF function (Rooke and Cartwright, 1976). Figure 2 shows the modelling geometry. Figure 3 shows the resulting K(c) curves.

Critical Crack Length, c0

Failure was assumed to be Kr dominated (see R6 terminology, Milne, 1986) following a previous assessment of circumferential defect failure for reactor bellows.

Using the lower bound value for K(0.2mm) of 95 MPa$\sqrt{m}$, and the CDPM, the predicted critical semi-crack length, c0, is 53mm (marked with an open circle on Figure 3).

Defect Development

If the defect grows under the vibrational stresses, and they are assumed to be through-wall bending, the resulting defect aspect ratio, a/c, will be about 0.2, based on experimental observations of fatigue flaw shape development. The resulting initial penetrating defect semi-length, c1, is given by:

$$c1 = t/(a/c) = 7.0 \ / \ 0.2 = 35.0\text{mm} \qquad (3)$$

This penetrating defect is shown to scale (Figure 2).

The aspect ratio may be even larger (and the consequent initial penetrating defect length, c1, smaller) due to the presence of the large static tensile stress of 118 MPa under normal operation.

If the vibrational stresses are assumed to be membrane, then the aspect ratio will be greater than 0.5 resulting in a value of cl given by:

$$cl < t/(a/c) = 7.0 / 0.5 = 14mm \qquad (4)$$

Leak-Before-Break Assessment

Basic Logic

The essential steps in the leak-before-break logic (Connors and Hellen, 1985), are:

(a) Establish that the initial through-wall defect length, cl, is less than the critical length, c0.

(b) Show that the leak that occurs during the post-penetration growth phase can be detected by the leak detection system.

(c) Show that there is sufficient margin of time between leak detection and growth to criticality such that safe shut-down can be performed.

The previous sections have shown that there is a margin between cl and c0, even for the lower bound initiation toughness value and the upper bound SIF curve.

Leak Rate Predictions and Leak Detection

Leak rate predictions were made using a pessimistic model for slit opening (a Centre Cracked Plate Model) and the code DAFTCAB

The axial slit in the convolution is pessimistically under-estimated to be modelled by the expression for the opening of a slit, in a wide plate under tension (Erdogan, 1976) for the slit centre, do, and the slit tip, da.

The leak rate calculation method using the code DAFTCATB requires the calculation of an effective slit width of an equivalent "letter box" slit having a constant width along its length. This is achieved using the concept of hydraulic diameter, dh

$$dh = \frac{4 \times \text{Slit Area}}{\text{Slit Circumference}}$$

The value of dh is approximately given by

$$dh = (do + da)$$

where

$$do = \frac{2c\pi\ Sy\ ln}{E} \left\{ \frac{1 + \sin\ (\frac{\pi\ Sm}{2\ Sy})}{Cos\ (\frac{\pi\ Sm}{2\ Sy})} \right\}$$

and

$$da = \frac{8\ c\ Sy}{\pi\ E}\ ln\ \left\{ \sec \frac{Sm}{2\ Sy} \right\}$$

where Sy is the material yield (0.2% proof) stress and Sm is the aplied membrane tensile stress.

The equivalent slit width, w, is given by

$$w = 0.5\ dh$$

Substituting values of

Sy = 640 MPa
E = 210,000 MPa
Sm = 113 MPa

results in the following values of slit openings

c	(mm)	= 10	20	30	40	50	60
do	(micro-m)	= 56	111	167	222	270	334
da	(micro-m)	= 3	7	10	13	16	19
w	(micro-m)	= 29	59	88	118	143	177

The DAFTCATB program accounts for the effect of surface roughness along the leak path, defined by the Centre Line Average, Ra. Typical upper bound values of 5 and 10µm were assumed for Ra. The flow is predicted to be turbulent and choked. The results (Figure 4) show that the leak rate is sensitive to the value of Ra. For general interest, the predicted flow rates for a smooth surface defect is also shown (dashed line).

For the assumption of pure through-wall bending vibrational stresses, it can be seen (Figure 4) that the leak rate for a fully developed through-wall slit with semi-crack length, c, of 35mm, is about 4.0kg/hr. The leak rate for a value of c of 14mm, resulting from pure tensile membrane fatigue, is 2.2kg/hr.

This should be easily detected using an established leak detection system.

In order to assess the margin of time available between leak detection. Shortly after penetration, and growth to criticality, the time taken to grow a further 1mm was calculated.

Post Penetration High Cycle Fatigue Growth

Membrane Vibration Stress Assumption

Using the vibrational stress levels with an additional error margin of 20% (Table 1) the resulting values of ΔK p.p. t.a. are given in Table 2 along with the times taken, T, to grow a further 1mm.

Table 2 : Typical Vibrational ΔK rms t.a. Values and Growth Rates for a Penetrating Axial Convolution Defect

	Bending Stress cl = 35mm		Membrane Stress cl = 14mm	
Operating Condition	ΔK ($MPa\sqrt{m}$)	T (hrs)	ΔK ($MPa\sqrt{m}$)	T (hrs)
Off-resonance	0.24	No growth	0.27	No growth
1680 rpm resonance	3.9	0.9	4.35	0.9
1900 rpm resonance (most probable mode)	1.4	222.0	1.56	82.7

(Assumed frequency of vibration = 400Hz)

Discussion

The predicted high cycle fatigue crack growth rate is vanishingly small under off-resonance conditions. This indicates that there is little possibility of the defect growing to penetration in the first place.

The worst case growth rate under the 1680 rpm resonance is predicted to require about 0.9 hours for 1mm of growth for both the membrane and bending vibration stress assumptions. Operating rules ensure the avoidance of this condition.

A more likely resonant condition is around the 1900 rpm circulator speed, this being nearer the operating speed. This conditions requires about 222 hours to grow the defect a further 1mm under the bending vibration stress assumption or 82.7 hours under the membrane stress assumption. The margin between the inital penetrating defect length, cl, and the critical value, c0, is predicted to be about 17.5mm for the vibration bending stress assumption and 385.mm for the membrane vibration stress assumption under fault pressure conditions.

The analysis shows therefore that a leak-before-break case can be justifiably argued for this reaction bellows unit.

ASSESSMENT DEVELOPMENT FOR FAST REACTOR PRIMARY VESSELS (HOOTON)

The structural integrity case for safety related structures of the commercial demonstration fast reactor (CDFR) (Mitchell, 1986) has two major elements. The first is the provision of a high inherent integrity, which is achieved by a high quality structural unit fabricated in ductile materials suited to the operating environment. The second is the enhancement of the inherent integrity by an integrated approach to design and monitoring, which establishes confidence levels of defect tolerance. It is as a part of this second element that the establishment of a convincing leak-before-break characteristic for the primary vessel adds significant confidence to its integrity in the role of core support.

It is a common misconception that leak-before-break is an inherent characteristic of a ductile material such as stainless steel used in the fabrication of the primary vessel. It is the result of a particular set of initial defect size and location, structural geometry and types of loading as well as material properties. It is consideration of these aspects of leak-before-break, applied to the CDFR primary vessel, that are summarised in this paper.

CDFR Core Support Path

The structures comprising the CDFR core support path are shown in Figure 5. The core weight is carried by the diagrid and transferred to the primary vessel by the core support structure. The primary vessel then serves as a single load path member, and the weight of the core is transmitted from the primary vessel to the vault through a compressively loaded support cone.

Primary Vessel Leak-Before-Break

The leak-before-break characteristic is relevant to a containing system where failure of the system will be preceded by a leakage giving adequate time for remedial action to be taken. For a pipe or pressure vessel this refers to a leak before catastrophic loss of pressure. For the CDFR primary vessel it is a very different role and refers to a leak before loss of the core support capability of the vessel.

Critical Defect Size

The first element of a convincing leak-before-break case is a requirement that the through-thickness critical crack size must be large compared to the plate thickness. Such a large crack will open considerably allowing large sodium flows with little risk of bl·kage by debris.

The evaluation of critical crack sizes in the vessel is dominated by residual stresses, the effects of which have been resolved both by experiment and analysis. Consideration has also been given to the effects on critical crack size of material degradation in service.

Residual Stresses

The magnitude of weld residual stresses (Leggatt, R.H., 1986) has been evaluated by an experimental programme covering a range of weld constraint. This has provided a knowledge of the high local stress distributions, which are balanced across the weld, and also the lower magnitude longer range stresses with a greater amount of elastic follow-up due to the constraint. The experimental data have been used to verify predictive techniques which allow extrapolation to other weld configurations.

A series of wide plate tests (Quirk, 1982) has provided and continues to provide experimental data which serves as a straightforward demonstration of the effects of residual stresses. The residual stress predictive techniques are used to design tests with severe short range stresses. These tests compliment calculations (Hooton, 1984) which account for the local residual stresses in conservative manner in deriving the defect driving forces. It is the intention to treat the long range residual stresses as primary stresses,because of the greater amount of elastic follow-up.

The tests and analysis give confidence that ultimate failure will not be effected by the larg. but short range residual stresses, and calculations using the CEGB's R6 failure assessment procedure indicate that through-wall defects of 300mm are tolerable.

Material Degradation

The mechanisms by which the structural material fracture properties may be degraded are cold work, thermal ageing and irradiation. The stainless steel will be supplied in the solution heat treated condition but will receive some cold work during forming. It is known that 20% cold work can reduce the parent plate properties to those of weld metal (Chipperfield, 1976). Generally, this level of cold work is not anticipated, even local areas are not expected to approach this figure but further consideration is required.

Thermal ageing tests at cold pool temperatures so far have shown no effects on the toughness of 316 steel and 17.8.2 weld metal with less than 8% delta ferrite. At higher levels of delta ferrite it can be affected but controls on composition should be adequate to preclude the degradation at service temperatures (Picker, 1983).

Similarly, it can be concluded that irradiation levels experienced will not adversely affect stainless steel plate and 17.8.2 weld metal toughness. The tests have shown that irradiation damage of less than 2 dpa has no significant effect (Picker and Cocks, 1983). The highest level in the permanent safety related structures is 0.5 dpa and considerably less for most of the structures.

The conclusion which can be drawn is that degradation of the toughness properties appears avoidable by cold work being at a low level, control of delta ferrite levels in weld metal and ensuring irradiation damage is less than 1 dpa, all of which are readily achieved.

Defect Growth

The second essential element of leak-before-break is that defect growth will cause all credible and low probability defects to grow through the wall before the critical through-thickness defect length is attained.

The potential growth mechanisms are stress corrosion, fatigue and stable tearing. The first of these must be avoided in service by design and material condition. The environmental conditions are selected to prevent potentially hazardous conditions, and are monitored to ensure that the design intent is achieved throughout the life of the plant. Growth by fatigue has been studied under normal duty cycle loading and gives confidence that fatigue growth for defects which are at or below the detection limit of volumetric inspection will be small. There remains the prospect of fatigue growth by the less frequent loading cycles and by the normal duty cycle on low probability but larger defects which have failed to be detected by volumetric inspection. The amount of growth will be either small or of low probability. The ongoing activity is to establish the threshold condition for defect size and loading which will lead to leak-before-break and those which will not. Initiation and subsequent growth by stable tearing is the mechanism considered in determining the critical defect sizes discussed in the previous section. For defects at or below the detection limit there is confidence that they will not initiate growth even under low probability load conditions. Further work is required to resolve the effects on crack growth of combined tearing and fatigue as the critical crack size if approached under earthquake loading.

Discussion

The steps described will provide a demonstration of the CDFR primary vessel leak-before-break characteristic to add significant confidence to the structural integrity case for the core support.

Work to date has given confidence that the vessel critical defect size will be large, with correspondingly large openings for leakage. Further work to remove remaining uncertainties on critical defect sizes and defect growth is being pursued.

OVERVIEW AND CONCLUDING COMMENTS

In certain circumstances the leak-before-break concept can be used with confidence to establish a safety case. This is shown in the example of the reaction bellows. For some applications such as the fast reactor primary containment it is necessary to establish and validate a rather more specialised approach. In pursuing the application of leak-before-break to a range of plant components there is the CEGB-R6 procedure to provide the analytical framework. For some applications elements of the approach are very conservative due to limited understanding or lack of validation. The research work is therefore focussed on these issues and the example of the work on growth of surface cracks is one such issue.

There is a great temptation in structural analysis to seek a solution for a wide range of problems. It appears that when considering the concept of leak-before-break it is essential that a case by case approach is adopted with the necessary development and validation to meet the specific need of the user and the requirements of the licencing body.

Acknowledgement

The author wishes to thank Dr I Milne of the Central Electricity Research Laboratories, Mr R A J Hellen of Berkeley Nuclear Laboratories, and Dr D G Hooton of The National Nuclear Corporation, Risley, for their contributions to the paper. This paper is published by permission of the Central Electricity Generating Board.

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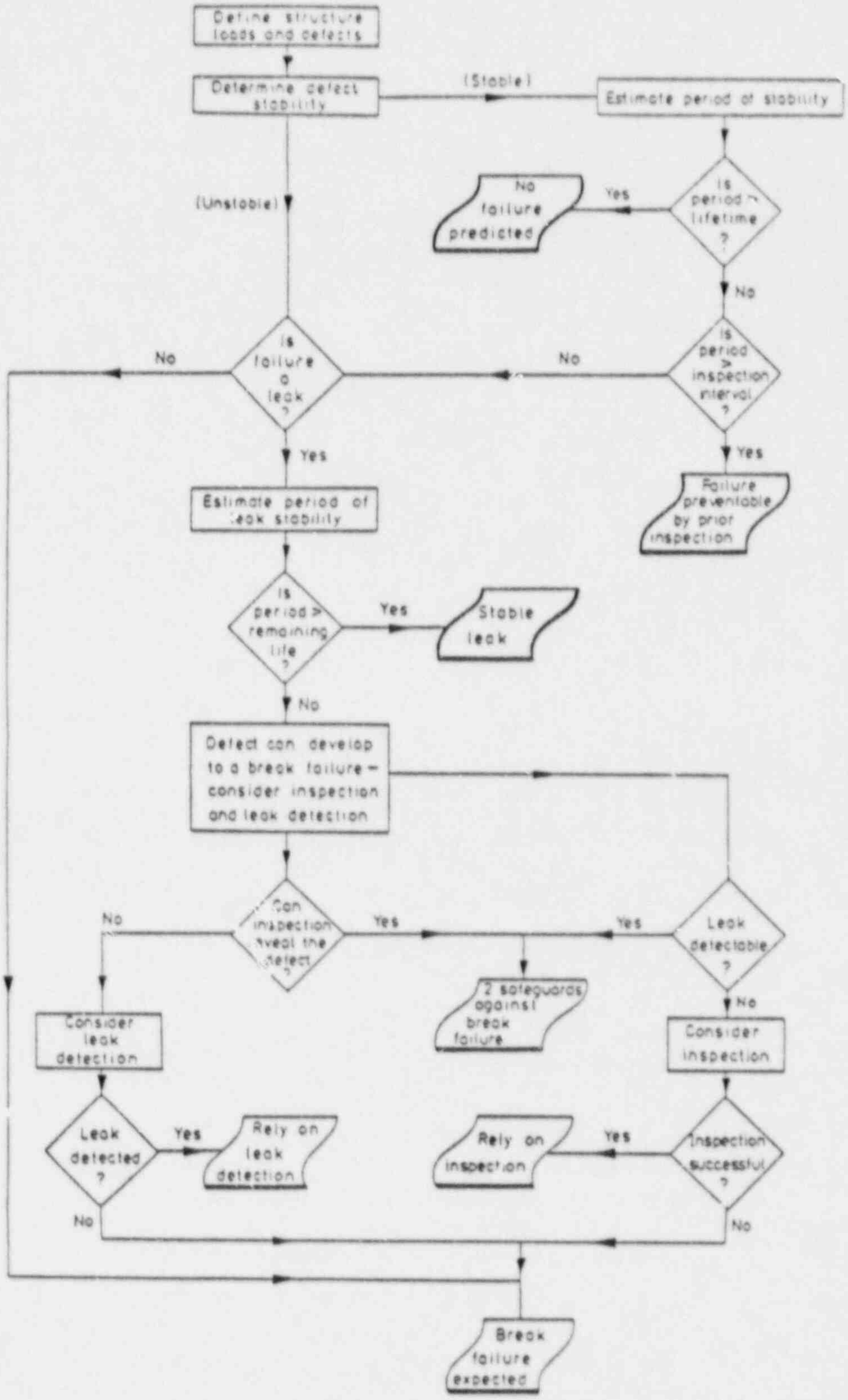
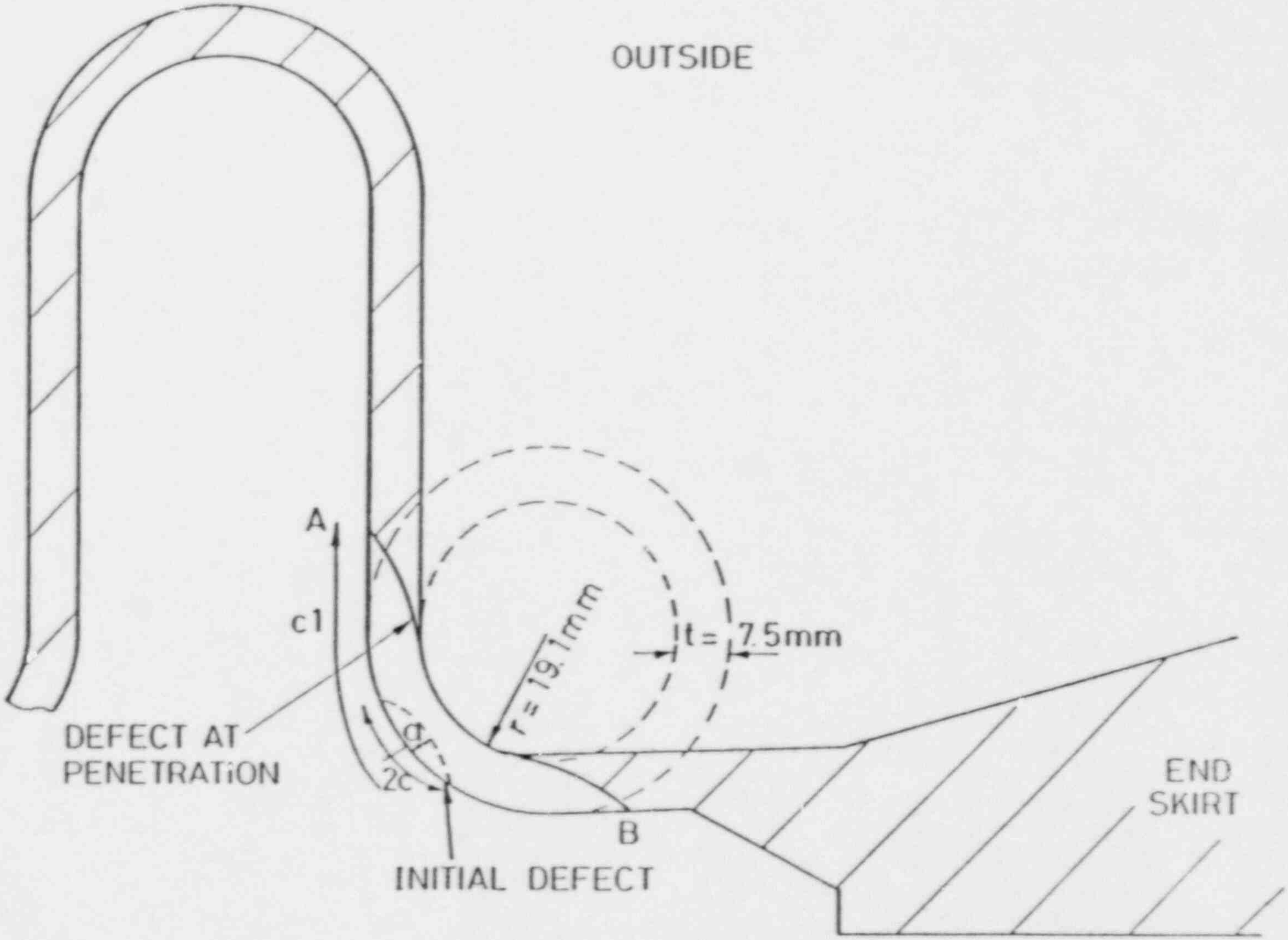
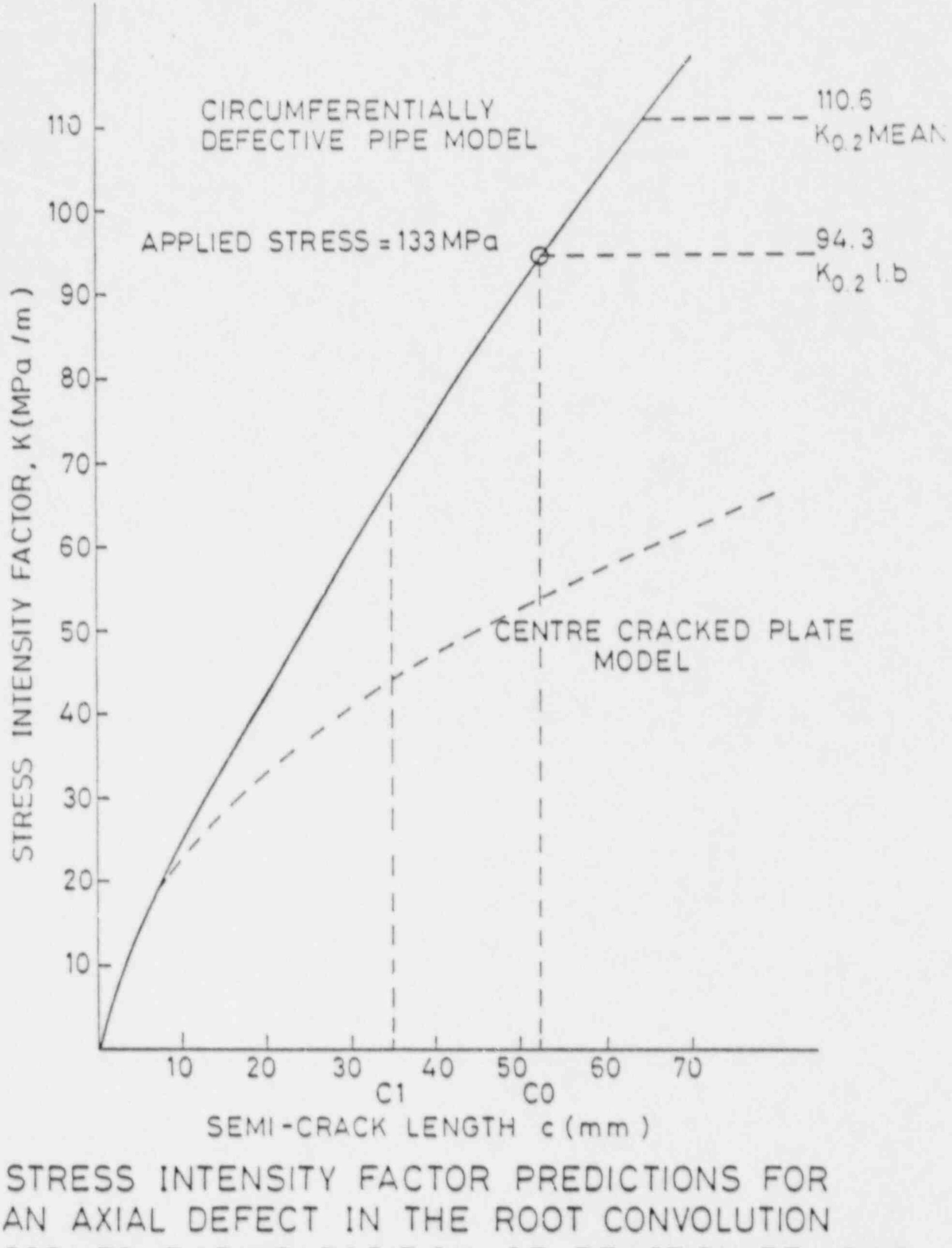


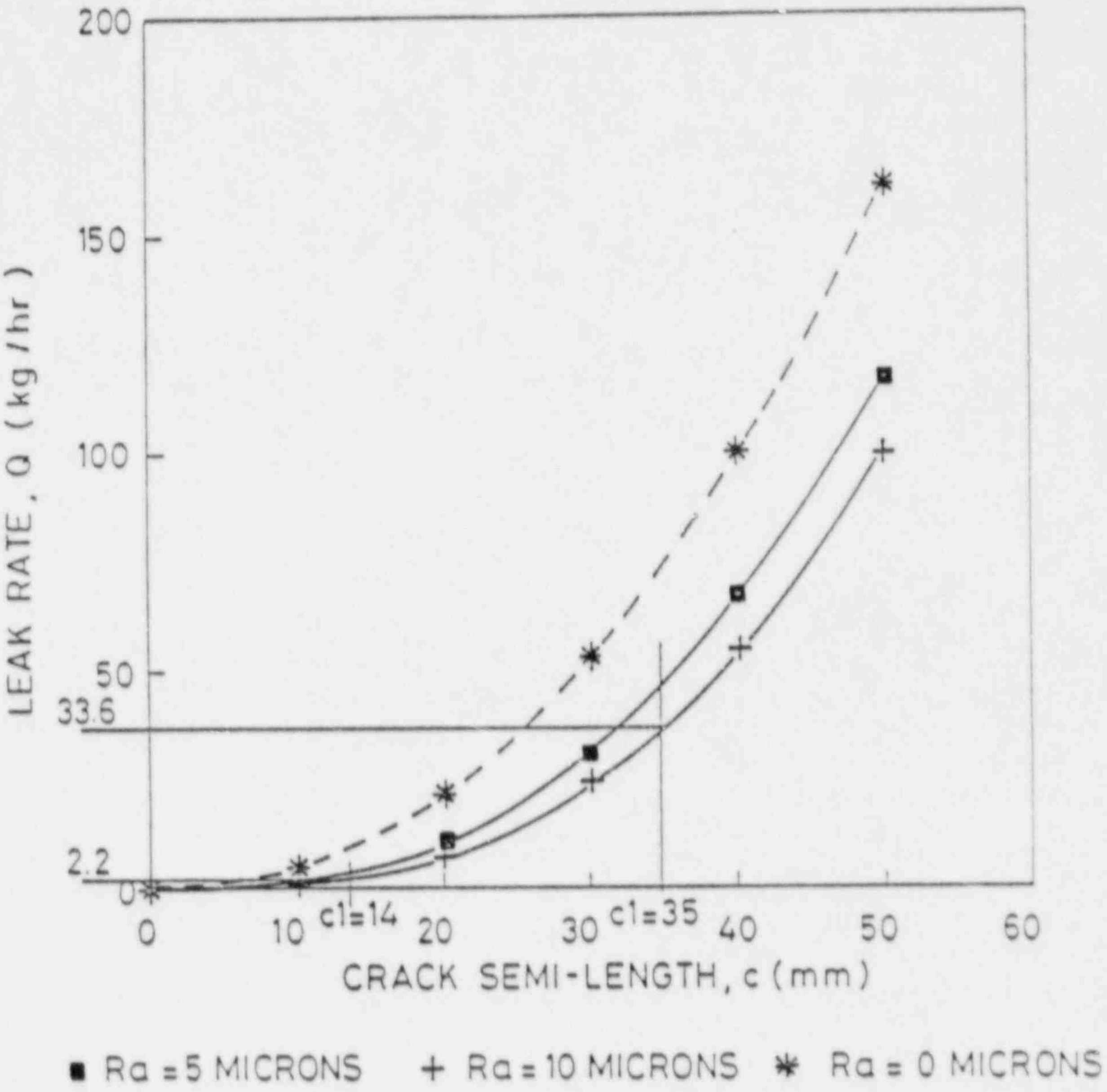
The Leak–before–Break Concept.



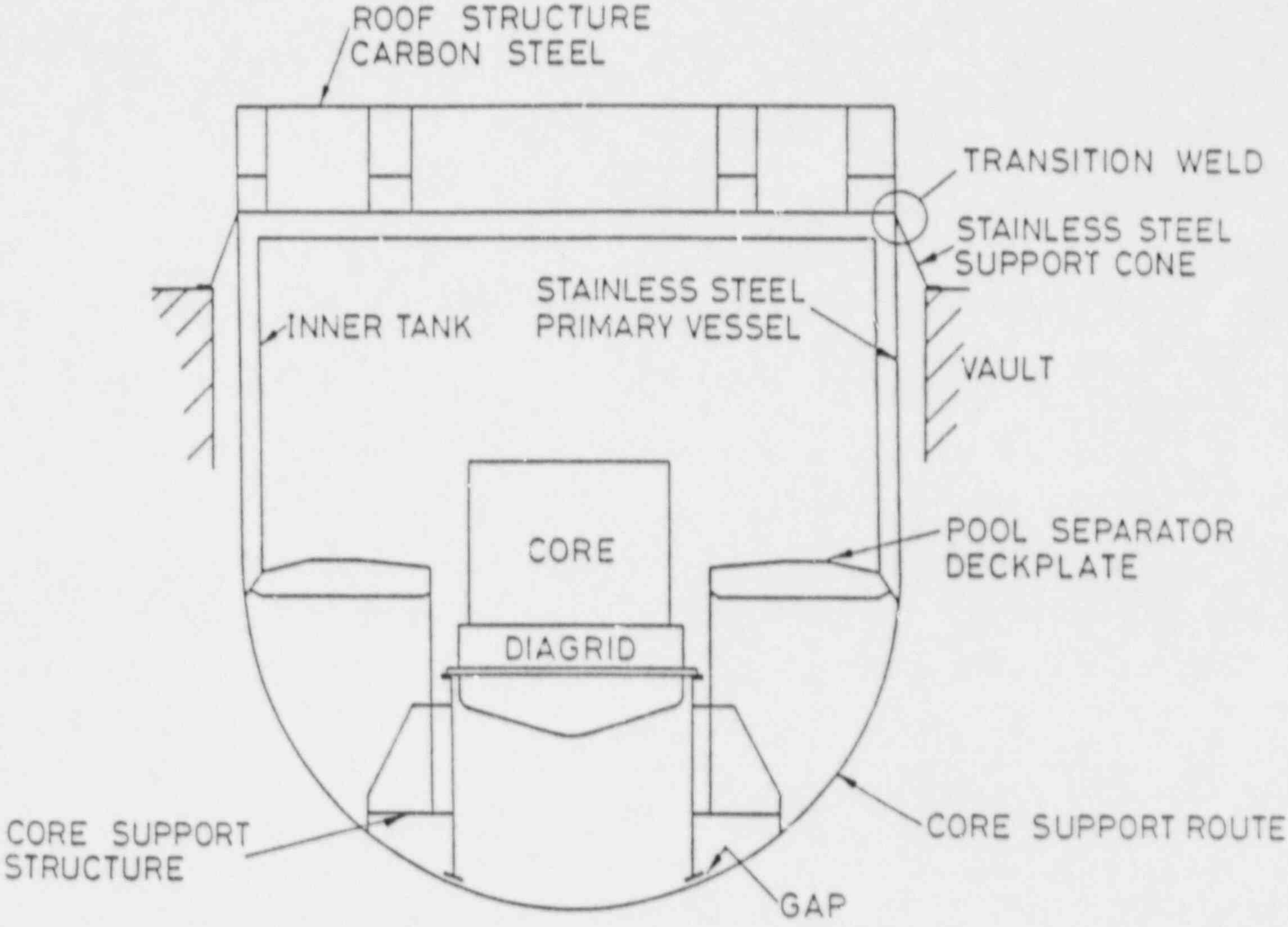
CONVOLUTION AXIAL DEFECT IN BELLOWS C1B4



STRESS INTENSITY FACTOR PREDICTIONS FOR AN AXIAL DEFECT IN THE ROOT CONVOLUTION CORNER RADIUS POSITION OF REACTION BELLOWS



LEAK RATE THROUGH AN AXIAL DEFECT



BASIC CORE SUPPORT CONCEPT (CDFR)

DEVELOPMENTS IN LEAK BEFORE BREAK APPROACH IN FRANCE

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EDF/SEPTEN

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CEA/CEN SACLAY

S. BHANDARI
FRAMATOME

The Leak Before Break (L.B.B.) concept is now considered for potential application in French plants. The main objective of L.B.B. is to suppress the double ended guillotine break or the catastrophic longitudinal break with respect to their mechanical consequences.

The general French approach considers preliminary studies on some lines in old plants, feasibility studies on plants under construction with the corresponding improvement in material charateristics and knowledge of complex loadings. Cost-benefit analysis and safety consequences are also carried out in order to prepare the inclusion of L.B.B. at the design level of new plants. A large research and development program completes these actions to validate step by step the fracture mechanics methodology. The French safety authorities are periodically informed of the development of these actions and their final approval will be the ultimate step to apply L.B.B. in French PWR plants.

1. OBJECTIVES

The main objectives of the L.B.B. is not to take into account double ended guillotine break or catastrophic longitudinal break for different studies :

- mechanical consequences : simplification of pipe supports (no pipe whip restrain, no jet impingement shield, ...) and consequences on the civil work due to the decreasing of load on supports
- system analysis : this important point cannot be analysed at the present time without large modifications in the global satefy approach of the PWR French plants. There are no work and no reflexions in this field at the present time in France.

2. GENERAL APPROACH

The french approach is established step by step :

- pre-study on some piping of old plants : one ferritic pipe (steam line) and one austenitic pipe (primary loop)
- feasibility study on complete primary and secondary lines of plant under design. This study concerns the primary and auxiliary lines (class 1) with a diameter greater than 6" and secondary (steam and feedwater) lines inside the containment. A rough study is done on steam and feedwater lines outside the containment up to the main valve. All these feasibility studies are done with the actual design rules (RCCM), the actual materials, the actual leak detection devices, and the actual pre-and in-service inspection programs.
- cost-benefit analysis specially if some need of modifications appears at the previous step (feasibility study).

In parallel to these different steps, we continue and increase :

- studies on technological progress like toughness level of materials or initial flaw sizes or knowledge of complex loading like stratification phenomena or sudden valve closure
- large research and development program with tests and numerical approach to validate the methodology.

Finally, we periodically report the progress of the work to safety authorities to obtain their approval before proceeding to different applications that we would be interested in.

3. BASIS OF THE DEMONSTRATION

The different classical steps of the demonstration of applicability of L.B.B. are :

- initial defects : geometry and size
- fatigue crack growth studies for the plant life
- stability studies of the end of life part-through- crack in level D conditions (seismic loads)
- through-wall crack stability studies in level D conditions
- crack area for normal operating load and corresponding leak rate
- comparison with the detectable leak rate.

4. POST RESEARCH AND DEVELOPMENT PROGRAMS

Different research and development programs were run during the last 10 years between EDF-FRAMATOME and CEA. The main programs are :

4.1 - Aquitaine I Réf. /1/ /2/

This test and analytical program was undertaken to validate on pipes the fatigue crack growth criteria under large strain amplitudes and PWR environment. Tests have been done on part through semi elliptical longitudinal and circumferential cracks under cyclic pressure or static pressure and cyclic moment. The Paris law has been validated for 3-D situations in PWR environment for 3-D craks.

4.2 - Aquitaine III Réf. /3/ /4/

This test and analytical program was undertaken to determine ductile fracture criteria applicable to primary austenitic piping. These tests have been performed on 316 L and 3" diameter pipes in two phases :

- specimen testing to determine scale, notched versus precraked and geometry effects on CT and CCP
- burst tests of pipes to confirm the validity of the criteria.

A large geometry effect on J resistance curve has been obtained and, due to the small ligaments and the high level of toughness, burst by plastic instability has been very well correlated for burst tests.

4.3 - Thermal aged austeno-ferritic steel program Réf. /5/

Two aspects are considered in this program : metallurgy and material characteristics on one side and mechanical tests and analysis on the other side. The corresponding objectives of these aspects are :

- the effect of aging temperature on the toughness of these materials and the relation betwen resilience and toughness
- the fracture criteria of specimen compared with a real structure (pipe burst test).

The first part of the program is still going on in France. The burst test of a 3 daJ/cm² resilience thermal aged austeno ferritic piping has been done and has justified the ductile tearing resistance of these material. A complete computation of the test has shown a geometry effect on the J resistance curve but the use of CT J resistance curve is conservative.
A last test is planned to take place at Battelle Columbus Laboratory in 1988 (4 point-bending test at 300°C with through wall circumferential crack).

4.4 - Stability analysis of pipes Réf. /6/

To run the different studies of stability analysis of through wall cracked pipe, we have compared engineering methods (R6, EPRI, Paris method) to finite element method (3-D computation using shell elements and virtual crack extension methodology).

The validation of the CASTEM computer system has been obtained on the Battelle benchmark (4 point-bending problem), and comparison between FEM and engineering methods has been done on :

- austenitic (316) 32" (PWR hot leg) pipe with circumferential crack and complex loading
- ferritic (A106) 32"(Steam line) pipe with circumferential crack and complex loading.

Our conclusion for these different exercices are :

- R6 and EPRI method work well but refinements are needed for complex-loading and some stress-stain curve modelisations to decrease the conservatism,
- the critical crack sizes are very encouraging concerning L.B.B. demonstration.

4.5 Other research and development programs

To complete this program, there are some other programs related to L.B.B. but not specific to L.B.B.

- leak rate model (ref. to the presentation in session 3)
- leak rate monitoring : global and local measurements
- non destructive examinations ; specially ultrasonic techniques in stainless steels
- technological aspects like limitating the number of welds or increasing the quality of some special weld joints.

5. NEW FRENCH LEAK BEFORE BREAK PROGRAM

A common EDF, CEA and FRAMATOME program of research and development has been decided in 1986 with a specific french program and a participation to the IPIRG program.

The french program covers : static and dynamic tests, development of specific computer models, validation and comparison of engineering methods and formalisation of recommandations for L.B.B. analysis.

5.1 Prototypical tests (tables 1 and 2, Figure 1)

These concern 4 point-bending tests without pressure on carbon and stainless steels up to 700 mm external diameter and different radius/thickness ratios at 300°C with circumferential through wall cracks and part-through cracks in base metal and welded joints. About 20 tests are scheduled. The objective is to verify some conclusions reached in the Degraded Piping Program for materials and pipes used in french PWR plants.

5.2 Analytical tests (Figure 2)

These 4 point-bending specific experiments, without pressure on 100 mm stainless steel pipe are scheduled to verify the collapse load and the J estimation directly from experiment to validate a 1-D cracked element in finite element method.

5.3 Static tests on singularities

Some tests are proposed on reducers, elbows or other pipe singularities but the detailed experimental program is not completely defined.

5.4 Dynamic tests (Figure 3)

Some analytical tests on simple are components under dynamic loads (sinusoïdal or seismic) proposed on straight pipes, elbows or junctions and probably a complete analytical test on a simple line.

The objectives of these different tests are :

- to complement some detailed points of IPIRG task 1
- to demonstrate the applicability of our global methodology to safety authorities.

5.5 Computer code developments (Figure 4)

Two types of development are on going :

- coupling of 1-D beam element analysis with 3-D shell element near the crack to compute J by virtual crack extension method
- development of a hinge element with constitutive equations taking into account the crack, the stable crack growth, the plasticity and the different contact problems.

5.6 Engineering methods

All the available tests in France or in litterature (DP III, IPIRG, other programms) will be used to validate the different engineering J estimation schemes (R6, EPRI, Paris methods).

A special treatment is planned to be done on some specific difficult issues in applying the engineering methods like :

- prediction of initiation before the limit moment
- classification of secondary and primary stresses
- superposition of loadings
- strain hardening effects
- behaviour under torsion loading
- leak area evaluations.

6. CONCLUSIONS

The French utility (EDF) has decided with the manufacturer (FRAMATOME) and the French Atomic Energy Commission (CEA) to study the applicability of the concept of Leak Before Break on its PWR nuclear power plants (in service, under construction or under design). The approach is based on two steps :

- feasibility study for plants under construction without specific modifications due to L.B.B. application,
- cost-benefit studies including safety consequences, inspection program modifications or design changes needed for L.B.B. application.

In parallel, a large Research and Development program is started under static and dynamic load in France ; the french three parties (EDF-FRAMATOME-CEA) have joined the IPIRG program and try to use the different international Research and Development programs to validate the applicability of leak before break situation for primary and secondary piping.

All the developments are presented and discussed periodically with safety authorities to obtain their approval on this approach.

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SMIRT 1983 - paper G/F 2/6

4 Evaluation of critical sizes of part-through cracks on 3 inch austenitic stainless steel piping
GARCIA - M'BARKI
SMIRT 1985 - paper G5/4

5 Burst test on austeno ferritic pipes
(to be published)

6 Application of leak before break concept on a 4 loop PWR
CHARRAS - JAMET - BHANDARI - TAUPIN
SMIRT 1987 - paper GF6/1

TABLE 1 - 6" (168.3 mm) PIPE TESTS

1) Acier A 42-1A
Épaisseur 21 mm

N°	Type défaut	Angle 2 α	Profondeur (d/t)	Température (°C)	Chargement	Observations
1	Traversant	120°	1	300	Monotone	
2	Traversant	60°	1	300	Monotone	
3	Traversant	120°	1	300	Monotone	Joint soudé.
4	Surface, interne	120°	0,66	300	Monotone	Usinage par électro-érosion.
5	Traversant	30°	1	300	Monotone	Joint soudé.
6	Traversant	30°	1	300	Type «séisme»	

2) Acier Z 3 CND 17.12 Azote contrôlé
Épaisseur 40,5 mm

N°	Type défaut	Angle 2 α	Profondeur (d/t)	Température (°C)	Chargement	Observations
1	Traversant	40°	1	300	Monotone	
2	Traversant	120°	1	300	Monotone	
3	Traversant	40°	1	300	Monotone	Joint soudé.
4	Surface, interne	120°	0,66	300	Monotone	Usinage par électro-érosion

TABLE 2 - 16" (406.4 mm) PIPE TESTS

1) Acier TU 48C
Épaisseur 11 mm

N°	Type défaut	Angle 2 α	Profondeur (d/t)	Température (°C)	Chargement	Observations
1	Sans	–	–	300	Monotone	
2	Traversant	30°	1	300	Monotone	
3	Traversant	120°	1	300	Monotone	
4	Traversant	30°	1	300	Monotone	Joint soudé
5	Traversant	30°	1	300	Type «séisme»	

2) Acier Z 2 CN 18.10
Épaisseur 18,2 mm

N°	Type défaut	Angle 2 α	Profondeur (d/t)	Température (°C)	Chargement	Observations
1	Traversant	40°	1	300	Monotone	
2	Traversant	120°	1	300	Monotone	
3	Traversant	40°	1	300	Monotone	Joint soudé
4	Traversant	40°	1	300	Type «séisme»	

FIGURE 1 - EDF TEST FACILITY

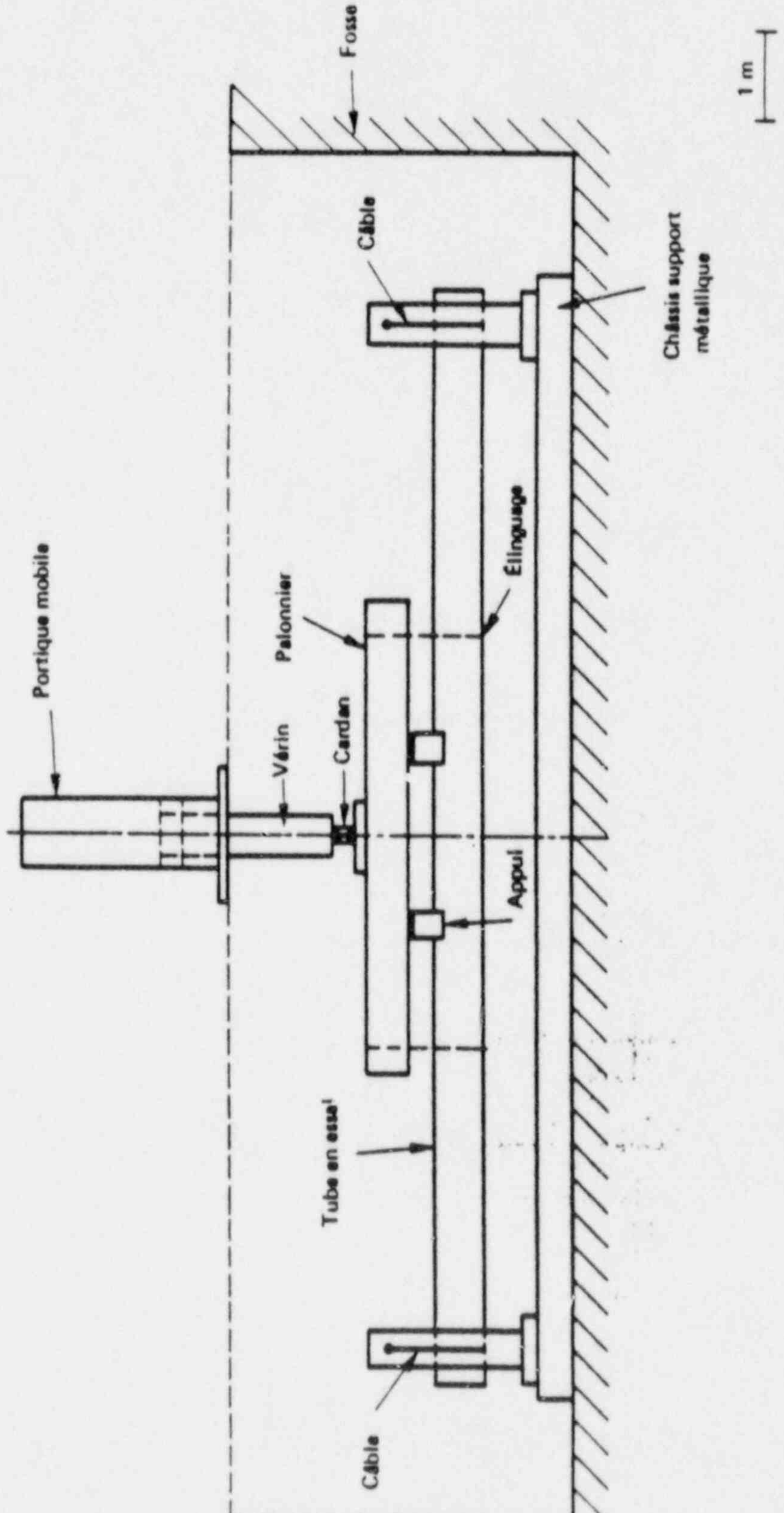


FIGURE 2 - CEA TEST FACILITY

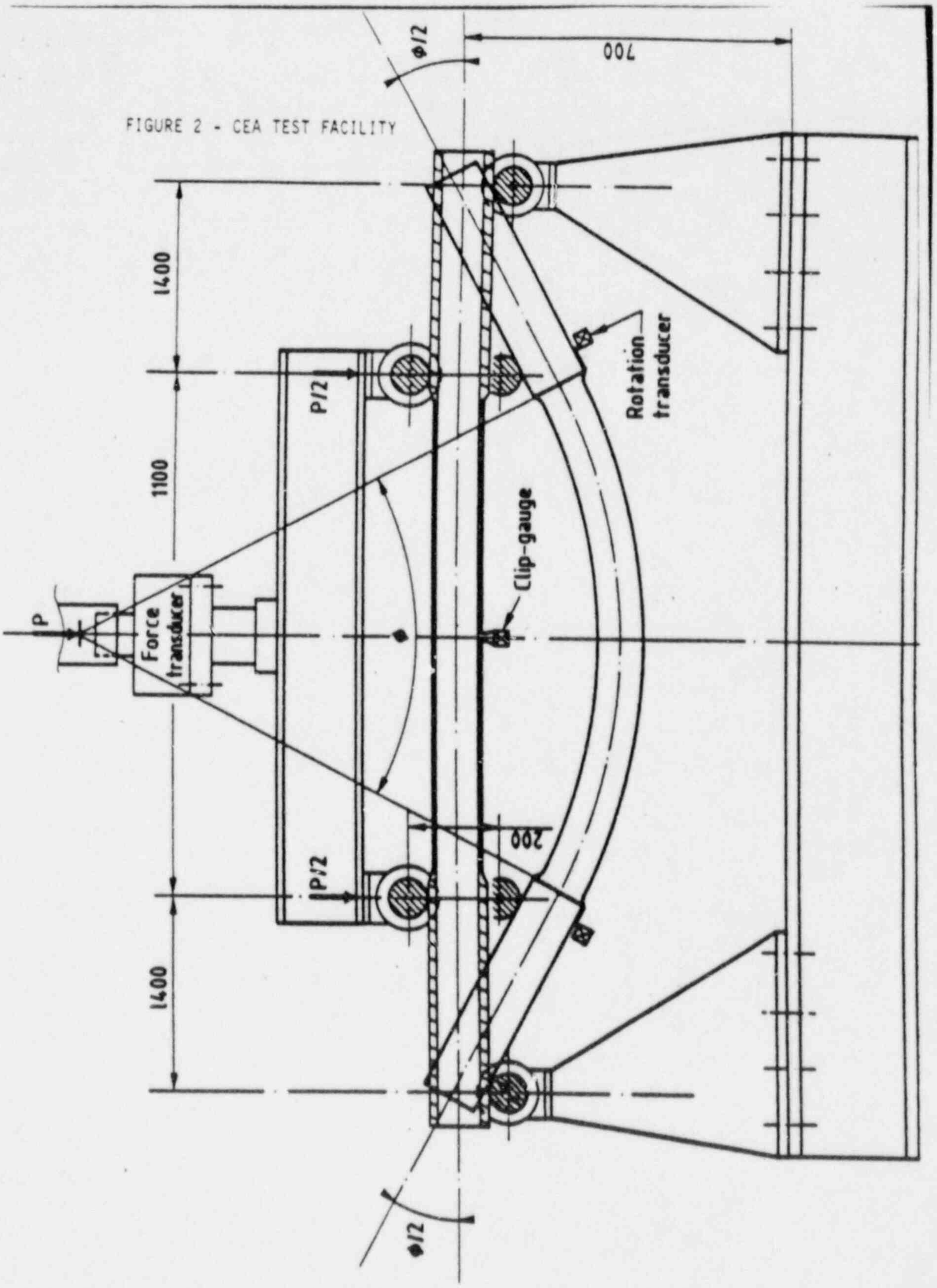


FIGURE 3 - CEA SHAKE TABLE

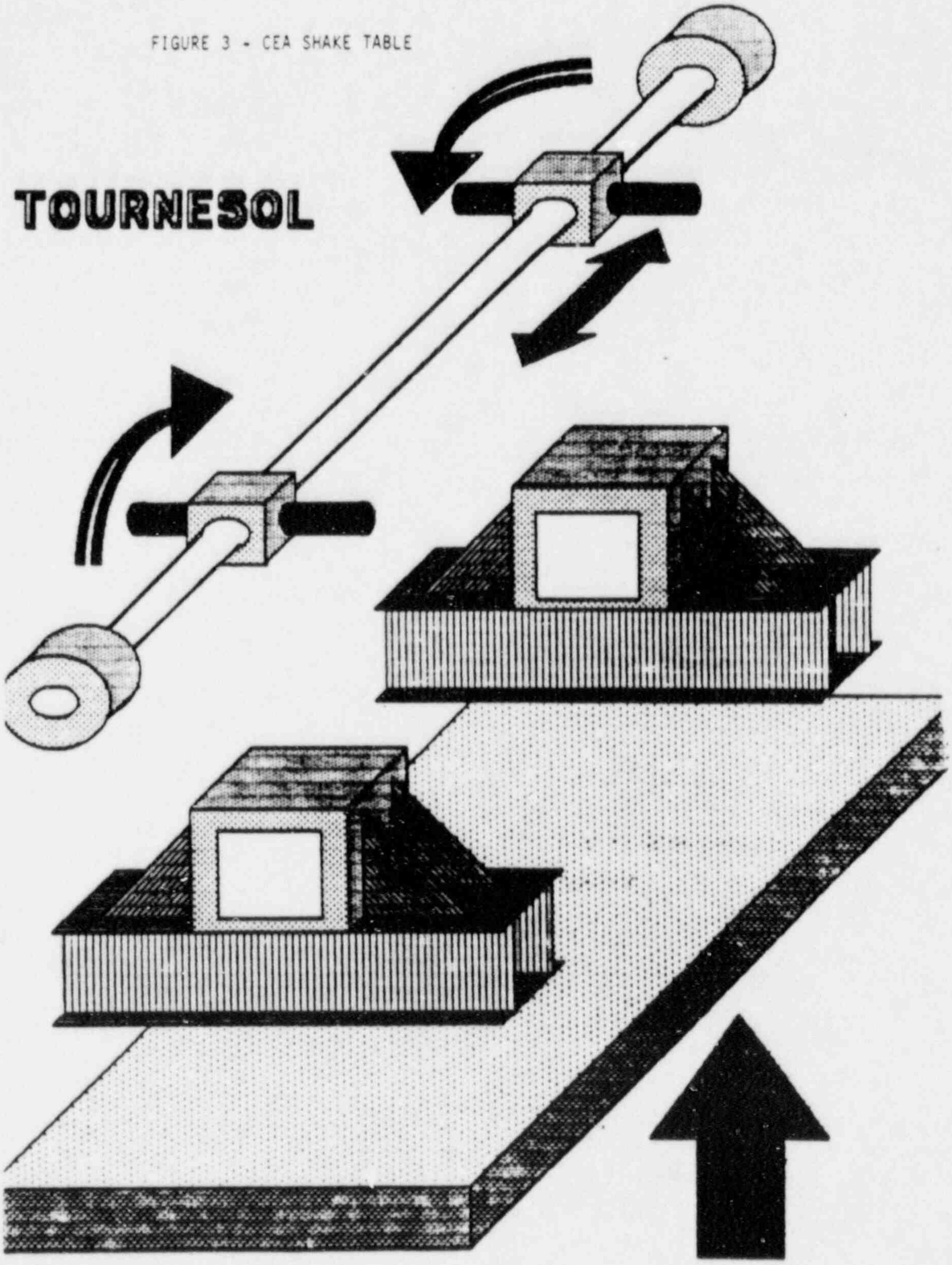
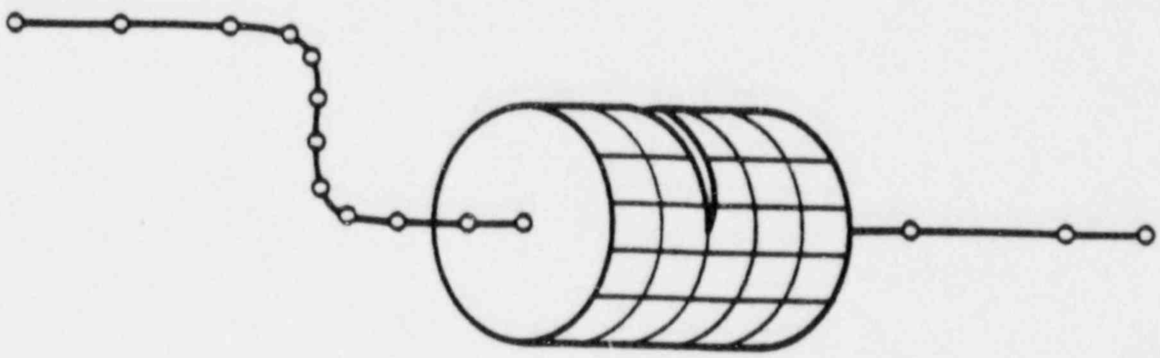
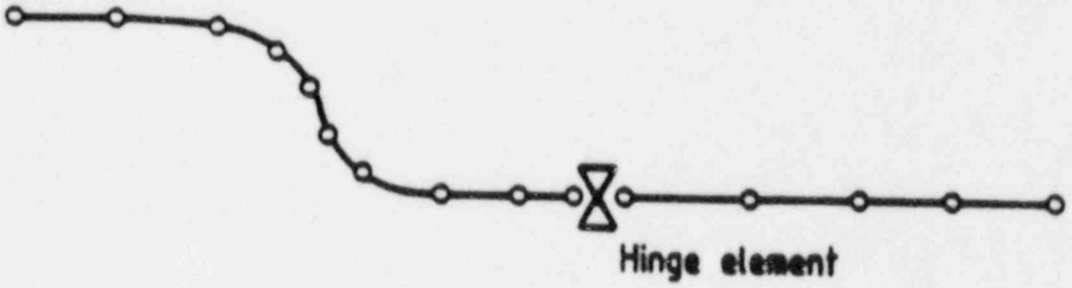


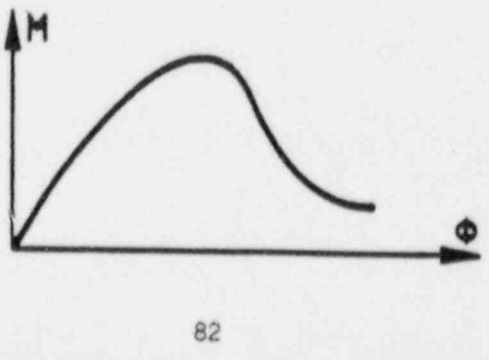
FIGURE 4 - COMPUTER CODE MODELS

* 3-D ANALYSES



* 1-D ANALYSES : Development of an hinge element





VERIFICATION TEST PROGRAM ON INTEGRITY OF CARBON STEEL PIPING IN LWR PLANTS

Y. Asada *

Verification Test Committee on Integrity of Carbon Steel
Piping and Weldment

NUCLEAR POWER ENGINEERING TEST CENTER **

This verification test is started in April 1985 and scheduled to be completed in March 1989 under the sponsorship of MITI.

Objectives of this verification test program are to demonstrate leak-before break concept that instantaneous pipe rupture cannot occur under the actual plant operating conditions in carbon steel piping with high quality, and to contribute to establishing rationalized design criteria on postulated pipe rupture as structural design basis in LWR plants.

The test program is planned to obtain basic materials properties and pipe rupture behavior for the carbon steel piping which are representative of actual plants and to develop an acceptance criteria for fracture evaluation.

The test program comprise of information survey, material property and pipe rupture experiments and fracture mechanics analysis.

Information survey include not only information for domestic and foreign countries, but also plant data required for fracture evaluation such as piping route, design conditions, design load, material, welding, system compliance of piping, crack growth and others.

Basic materials property tests, e.g. tensile test, fracture toughness test, center-cracked-panel test etc., are performed for 5 pipe materials and 2 weld metals. Pipe rupture experiments are also performed for base metal and weld metal of 6 inch and 16 inch seamless pipe, STS42, which are representative of LWR plants' pipings in Japan.

As for the fracture mechanics evaluations, the following are performed.

(1) Stress intensification, K-values, of inside surface of piping based on the FEM are compared with Newman-Raju Solution which is convenient engineering method. The engineering method will be used to predict crack growth.

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(2) Crack stability is evaluated using plural fracture criteria as net section collapse failure, J-integral, R-6 method comparing with pipe rupture experiments. This is to establish the appropriate fracture criterion applicable to carbon steel piping.

Finally, applicability including limiting conditions, if any, of LBB concept is evaluated for some typical LWR pipings by verified evaluation procedure above, and based on the results, a draft of rationalized design criteria of postulated pipe rupture will be proposed.

This presentation gives abstract of the verification test program, typical data of material property test, pipe rupture experiment data of 6 inch pipes, and preliminaly analysis of K-value and crack stability as of March, 1987.

Following are members of Verification Test Committee on Integrity of Carbon Steel Piping and Weldment.

Y. Asada ; Chairman, University of Tokyo
G. Yagawa ; Vice-chairman, University of Tokyo
H. Kobayashi ; Vice-chairman, Tokyo Institute of Technology
K. Shibata ; Japan Atomic Energy Research Institute
K. Kuwabara ; Central Research Institute of Electric Power Industry
M. Koyanagi ; Tohoku Electric Power Company
A. Minematsu ; Tokyo Electric Power Company
T. Kurogochi ; Chubu Electric Power Company
T. Nakamura ; Kansai Electric Power Company
M. Okinaga ; Chugoku Electric Power Company
M. Watanabe ; Shikoku Electric Power Company
M. Fukunaga ; Kyushu Electric Power Company
M. Hirata ; Japan Atomic Power Company
Y. Yamamoto ; Toshiba
T. Yoshinaga ; Hitachi
Y. Toyoda ; Mitsubishi Heavy Industry
T. Umemoto ; Ishikawajima-Harima Heavy Industry
T. Okazaki ; Babcock Hitachi
H. Hata ; Mitsubishi Atomic Power Industry

K. Takumi ; Nuclear Power Engineering Test Center
T. Maruo ; Nuclear Power Engineering Test Center
T. Toyodome ; Nuclear Power Engineering Test Center
H. Sakamoto ; Nuclear Power Engineering Test Center

SCOPE OF PRESENTATION

- Background
- Schematic View of Program
- Materials and Basic Properties
- Pipe Rupture Test and Results
- Flaw Propagation Analysis and Prediction of Rupture Behavior
- Future Developments

SAFEGUARD DESIGN GUIDE AGAINST PIPE RUPTURE IN LWR PLANTS

—Experimentally examined by NUPEC and Industrial Group, Drafted by ADHOC Committee and Issueing by MITI—

PREMISE

- Being Applied to Stainless Steel Piping
- Being Applied to Piping in which the Possibility of SCC is Eliminated
- Not Applied to Safety System Design (Containment Vessel,....)
- Independent of PSI and/or ISI
- Leak Monitoring Capable to Find 5GPM Leakage

ACCOMPLISHES ELIMINATION OF

—Pipe Whip Restraints

—Jet Impingement Design

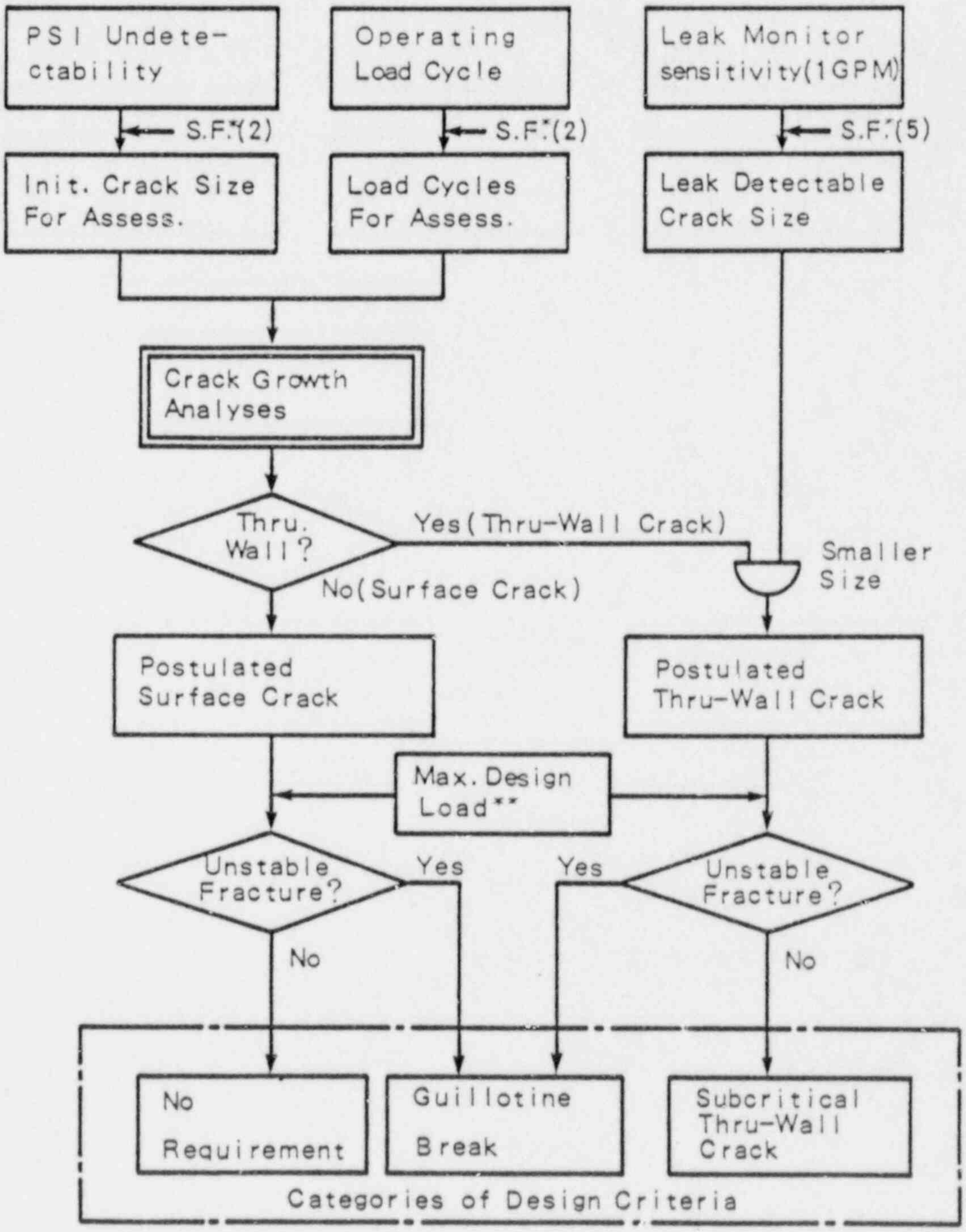
—Sophisticated Evaluation and Design Works

...or SIZE DOWN OF

—Components Supports

—Building Structures

SCENARIO OF LBB CRITERIA

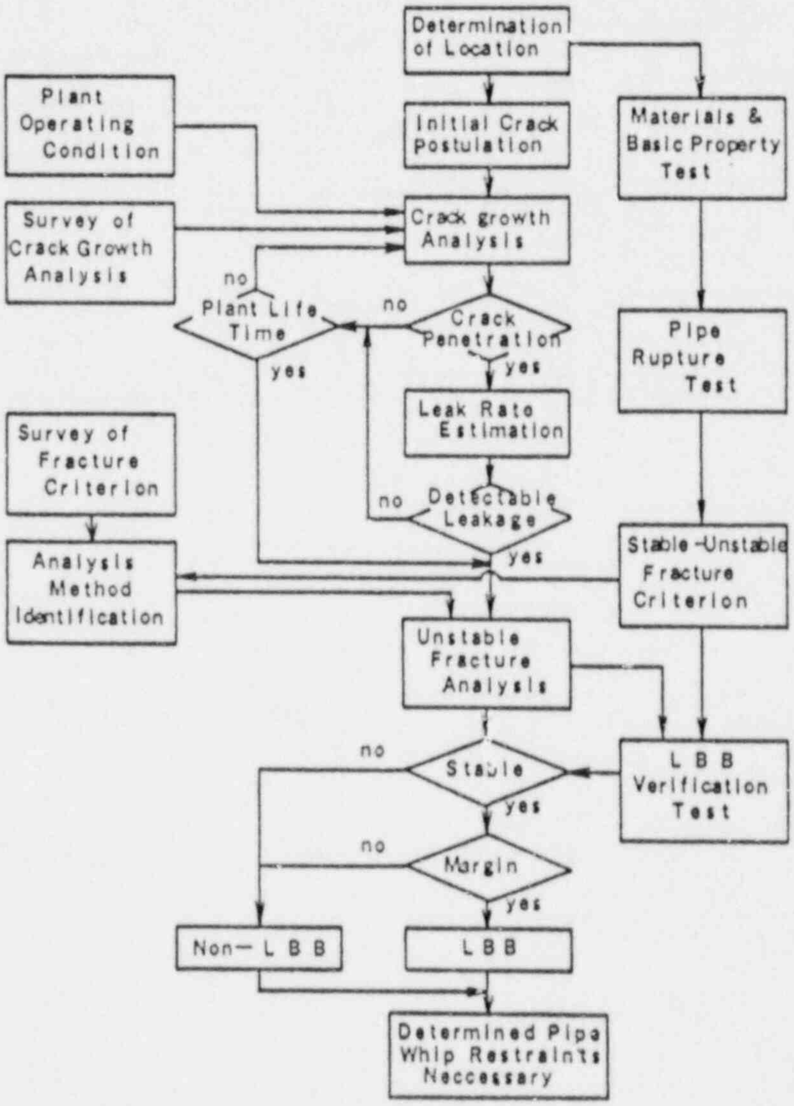


*: Safety Factor

**: Normal Operating Load Plus Design Earthquake Load

POLICY OF PROJECT

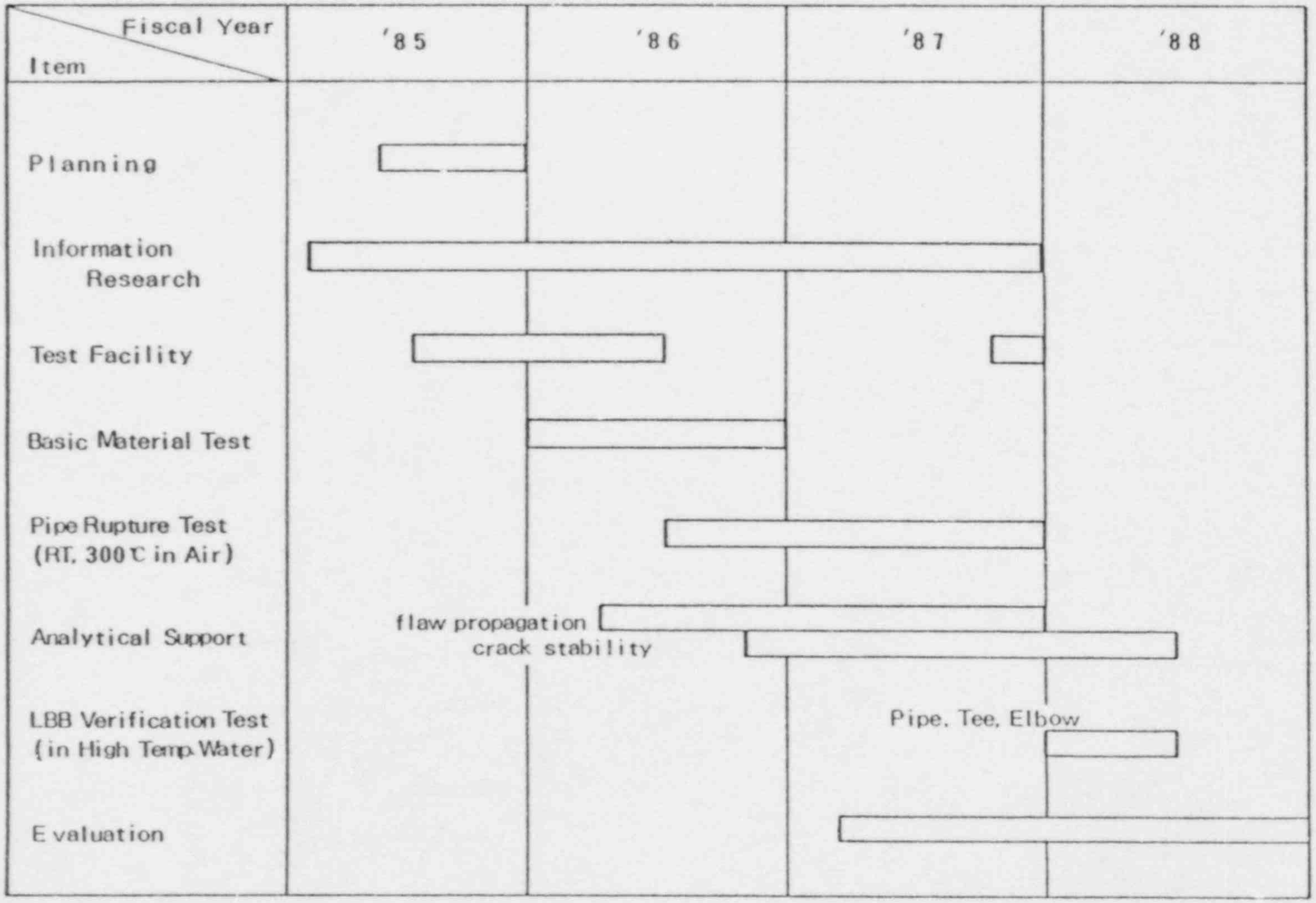
- Additional Supplements Safeguard Design Guide Against Pipe Rupture
- Evaluation Concept be Compatible to that for Stainless Steel
- Selected Materials be One of the Most Popular Use Rupture Properties with Average or Lower Trend to Those of Current Use



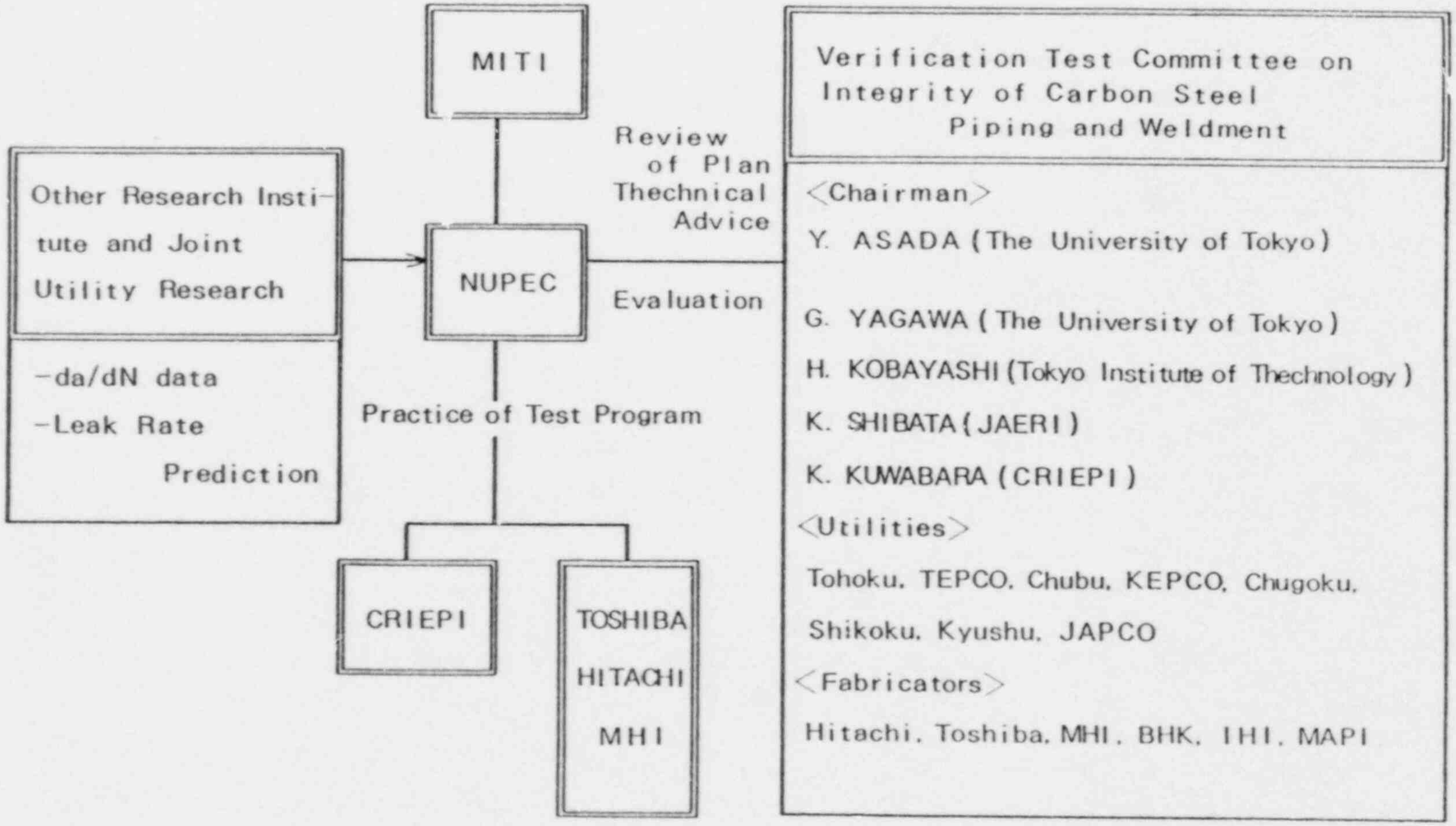
- Object
  - -environment
  - -material
  - -temperature
  - -pipe size/dimention
- Initial Crack
  - -NDI detectability
  - -Flaw type
  - -number
- Crack growth (da/dn)
  - -analysis code
  - -environmental cond.
  - -metallurgical cond.
  - -loading cond.
  - -Plant life time
- Leak rate
  - -crack opening area
  - -leak rate
- Leak detectability
- Unstable Fracture
  - -criteria
  - -crack type/number
  - -loading cond.
  - -load type (tension, bend,…)
  - -compliance
  - -metallurgical cond.
  - -pipe dimention
- Margin
  - -load
  - -crack size

ITEM OF EXAMINATION AND TEST FLOW

TEST SCHEDULE



ORGANIZATION



MITI

Review of Plan Thechnical Advice

Evaluation

Other Research Institute and Joint Utility Research

- -da/dN data
- -Leak Rate Prediction

NUPEC

Practice of Test Program

CRIEPI

TOSHIBA
HITACHI
MHI

Verification Test Committee on Integrity of Carbon Steel Piping and Weldment

<Chairman>

Y. ASADA (The University of Tokyo)

G. YAGAWA (The University of Tokyo)

H. KOBAYASHI (Tokyo Institute of Thechnology)

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<Utilities>

Tohoku, TEPCO, Chubu, KEPCO, Chugoku, Shikoku, Kyushu, JAPCO

<Fabricators>

Hitachi, Toshiba, MHI, BHK, IHI, MAPI

MATRIX OF THE PROVING TEST

Test Condition			Material Properties													Pipe Fracture Behavior													LBB Verification		
			Cv	Tensile Test				Fracture Toughness Test				Center Cracked Panel Test				Bending(Low Comp.)				Bending(high Comp.)					Tension(high Comp.)				High Compliance		
																Quasi-Static				Quasi-Static					Quasi-Static			DL	Cyclic Load		
				Round Bar				CT				Panel				6B		16B		6 B			16B		6 B			6B	Pipe	Elbow	Tee
Material (equivalent)			Full Curve	RT	100℃	200℃	300℃	RT	100℃	200℃	300℃	RT	100℃	200℃	300℃	RT		RT		RT		HT	RT	HT	RT		HT	HT	High Temp. water		
																c/t=1	c/t=0.5	c/t=1	c/t=0.5	c/t=1	c/t=0.5	c/t=0.5	c/t=0.5	c/t=0.5	c/t=1	c/t=0.5	c/t=0.5	c/t=0.5	c/t =0.5		
STS42	6B	ASTM A 333 Gr. 6	●	●	●	●	●	●			●	●$_1$			●$_1$	●$_3$	●$_3$			●$_4$	●$_4$	●$_2$			●$_1$	●$_1$	●$_1$	●$_1$	●$_1$		
	16B		●	●	●	●	●	●	●	●	●	●$_4$	●$_1$	●$_1$	●$_4$				●$_2$				●$_1$	●$_1$							
STS49	26B	ASTM A 333 Gr. 7	●	●	●	●	●	●	●	●	●																				
SFVC2B		ASTM A266 Cl.4	●	●	●	●	●	●		●	●	●$_4$			●$_4$																
SGV42		ASTM A516 Gr.69	●	●	●	●	●	●		●	●																				●$_1$	●$_1$
SMAW weld Metal		—	●	●	●	●	●	●	●	●	●	●$_1$			●$_1$		●$_2$													●		
SAW weld Metal		—	●	●	●	●	●	●	●	●	●																					

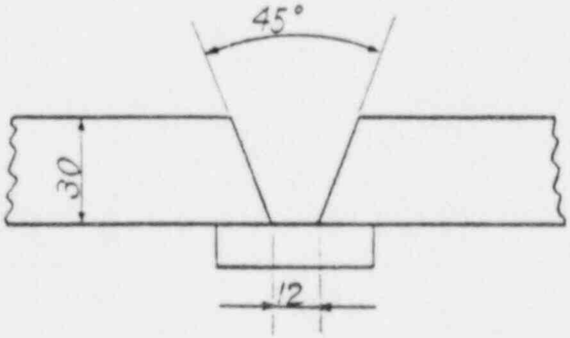
Cv ; Charpy Impact Test. DL; Dynamic Load. RT ; Room Temperature. HT ; 300℃. c/t ; (Crack Depth/Wall Thickness) Ratio

Applied Systems of the Materials

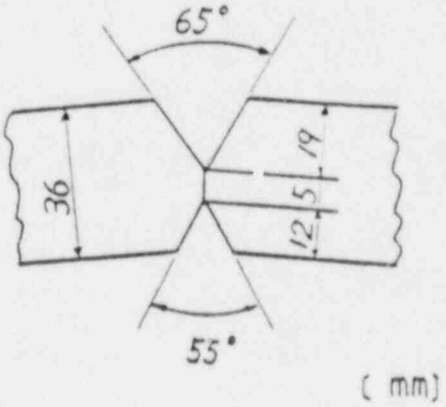
STS42 ; Feed Water, Core Spray, Main Steam, High Pressure Injection, Residual Heat Removal, Reactor Water Clean Up
STS49 ; Feed Water, Main Steam
SFVC2B ; Elbows and Tees of the above pipings
SGV42 ; Main Steam

WELDING CONDITIONS

Welding Method	SMAW	SAW
Position	Flat	Flat
Electrode	LB-52 (ϕ5mm)	MF-38 flux US-36 (ϕ4mm)
Pre-heat Temp.	100 ℃	100 ℃
Welding Current	180 ~260 A	500 ~750 A
Welding Voltage	21~23 V	32~38 V
Welding Speed	-	30 cm/min.
Inter-pass Temp.	190 ℃max.	300 ℃max.
P W H T	620 ℃ ×1.2hr FC	625 ℃ ×1.4hr FC



SMAW

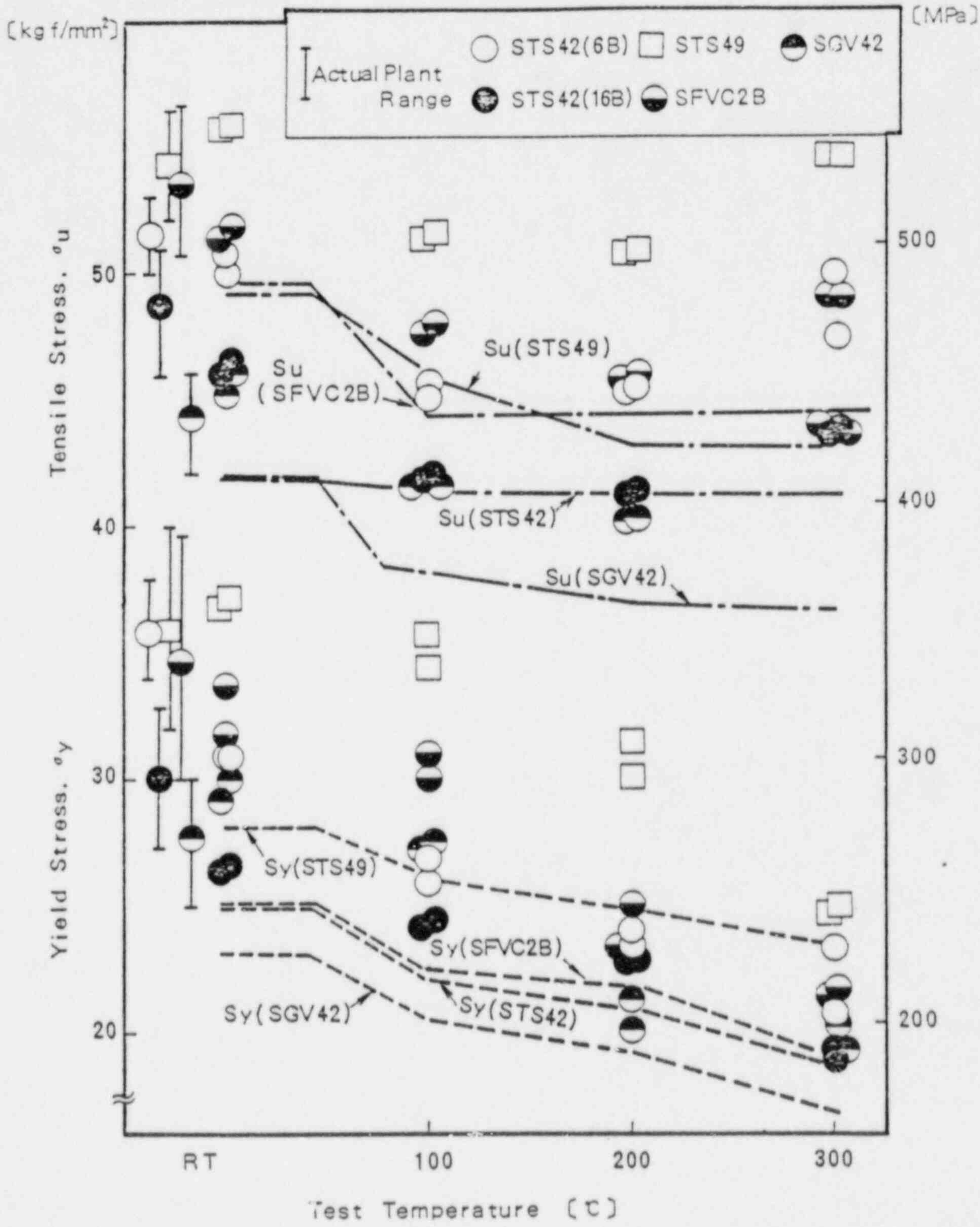


SAW

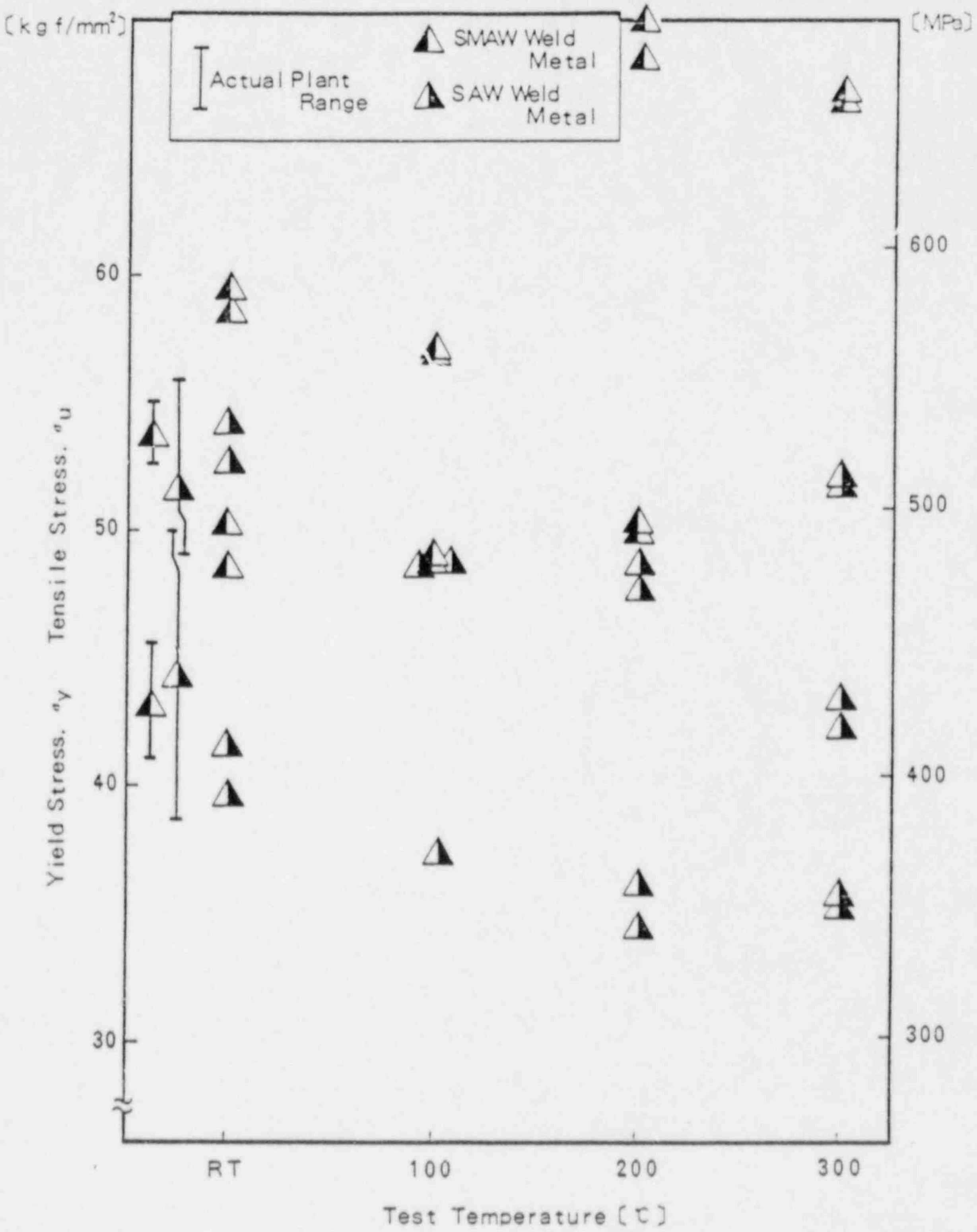
CHEMICAL COMPOSITIONS

Welding Method	SMAW		SAW	
Spec.	JIS Z 3212 D5016		JIS Z 3311 YSW-41	
Trade Disignation	LB-52		US-36(Wire) MF-38(Flux)	
Chemical Compositions	Spec.	Weld Metal	Spec.	wire
C	*	0.07	≦0.17	0.13
Si		0.63	≦0.05	0.02
Mn		1.08	1.8~2.2	1.93
P		0.015	≦0.03	0.010
S		0.004	≦0.03	0.009
Ni		0.01	-	-
Cr		0.01	-	-
Mo		0.01	-	-
V		0.01	-	-
Cu		-	-	0.11
Ni+Cr+Al		-	-	0.03
Epuivalent to	ASME SFA 5.1 E7016		ASME SFA 5.17 F7A6-EH14	

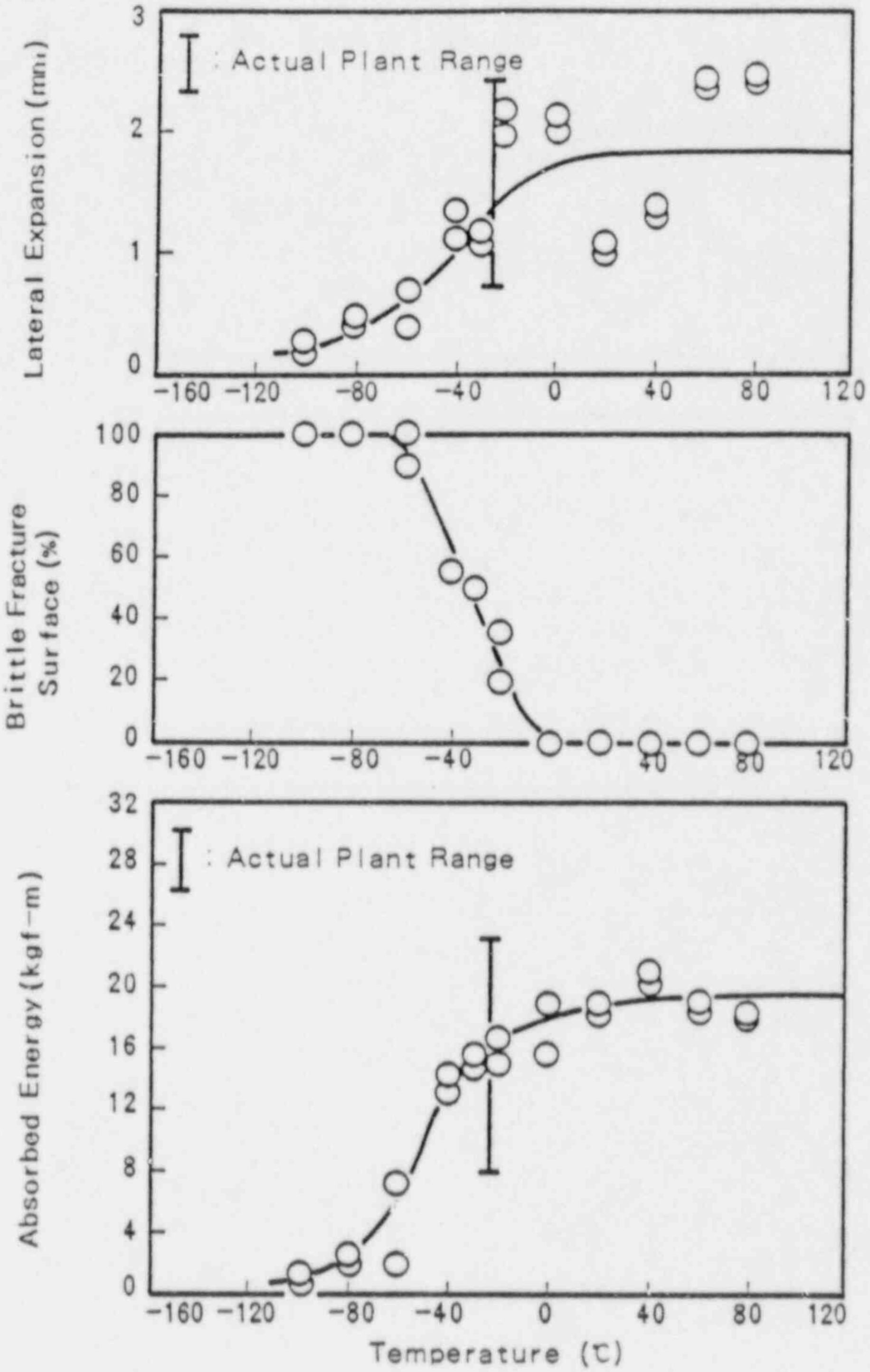
* not specified



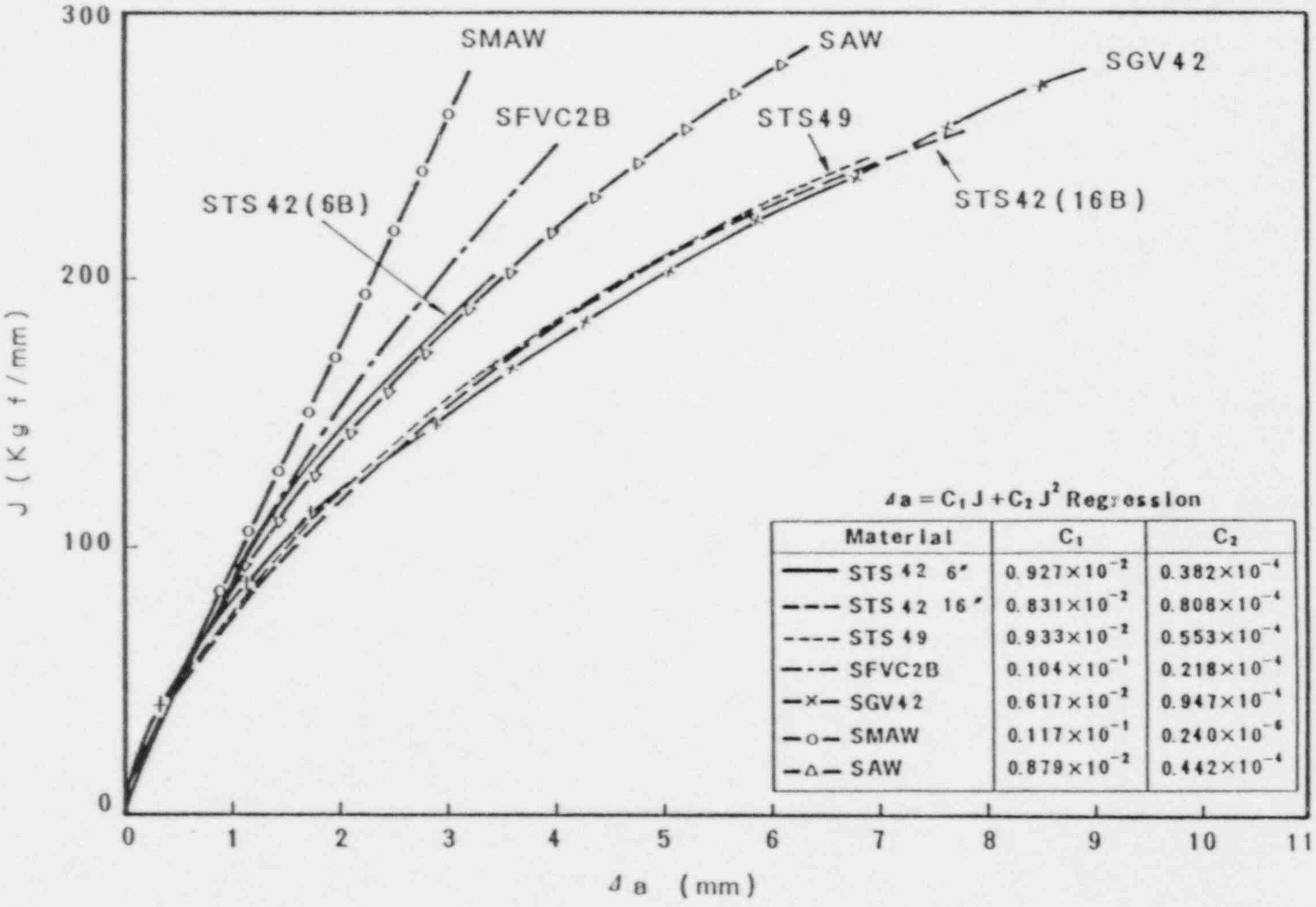
TENSILE PROPERTIES OF PIPE MATERIALS



TENSILE PROPERTIES OF WELD METAL



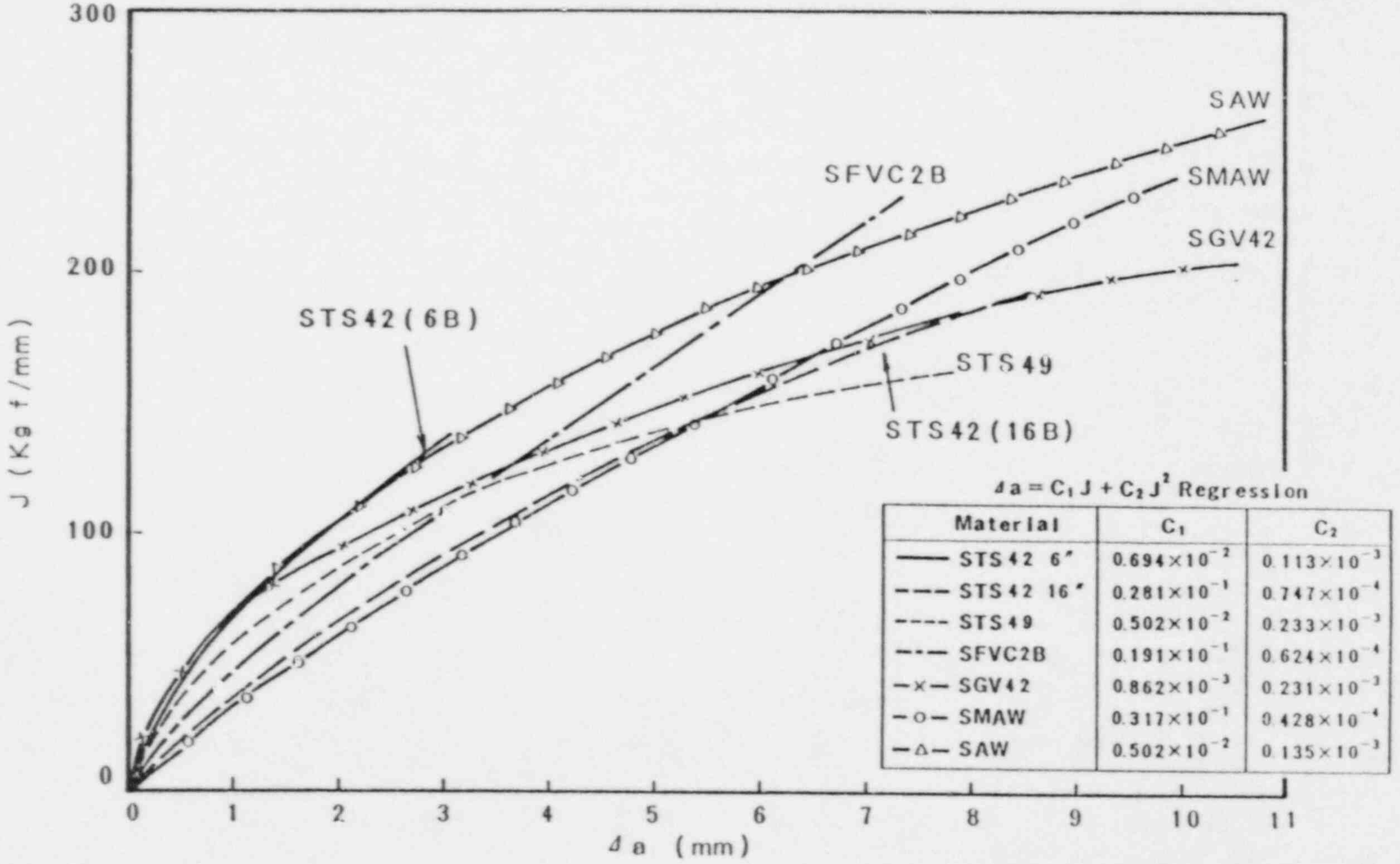
CHARPY-IMPACT TEST RESULT OF STS49



$\Delta a = C_1 J + C_2 J^2$ Regression

Material	C_1	C_2
—— STS 42 6″	0.927×10^{-2}	0.382×10^{-4}
— — — STS 42 16″	0.831×10^{-2}	0.808×10^{-4}
---- STS 49	0.933×10^{-2}	0.553×10^{-4}
—·— SFVC2B	0.104×10^{-1}	0.218×10^{-4}
—×— SGV42	0.617×10^{-2}	0.947×10^{-4}
—○— SMAW	0.117×10^{-1}	0.240×10^{-6}
—△— SAW	0.879×10^{-2}	0.442×10^{-4}

J-Δa CURVES FOR PIPE MATERIALS(RT)



$\Delta a = C_1 J + C_2 J^2$ Regression

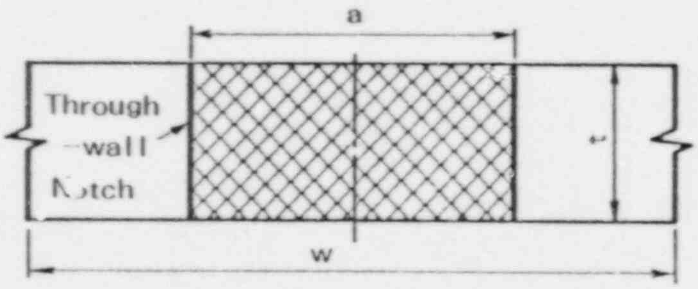
Material	C_1	C_2
—— STS42 6″	0.694×10^{-2}	0.113×10^{-3}
— — STS42 16″	0.281×10^{-1}	0.747×10^{-4}
---- STS49	0.502×10^{-2}	0.233×10^{-3}
—·— SFVC2B	0.191×10^{-1}	0.624×10^{-4}
—×— SGV42	0.862×10^{-3}	0.231×10^{-3}
—○— SMAW	0.317×10^{-1}	0.428×10^{-4}
—△— SAW	0.502×10^{-2}	0.135×10^{-3}

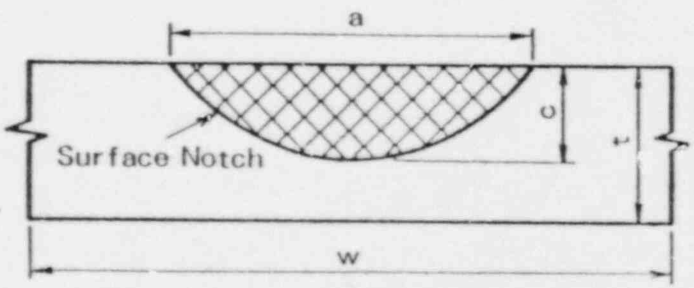
J-Δa CURVES FOR PIPE MATERIALS(300°C)

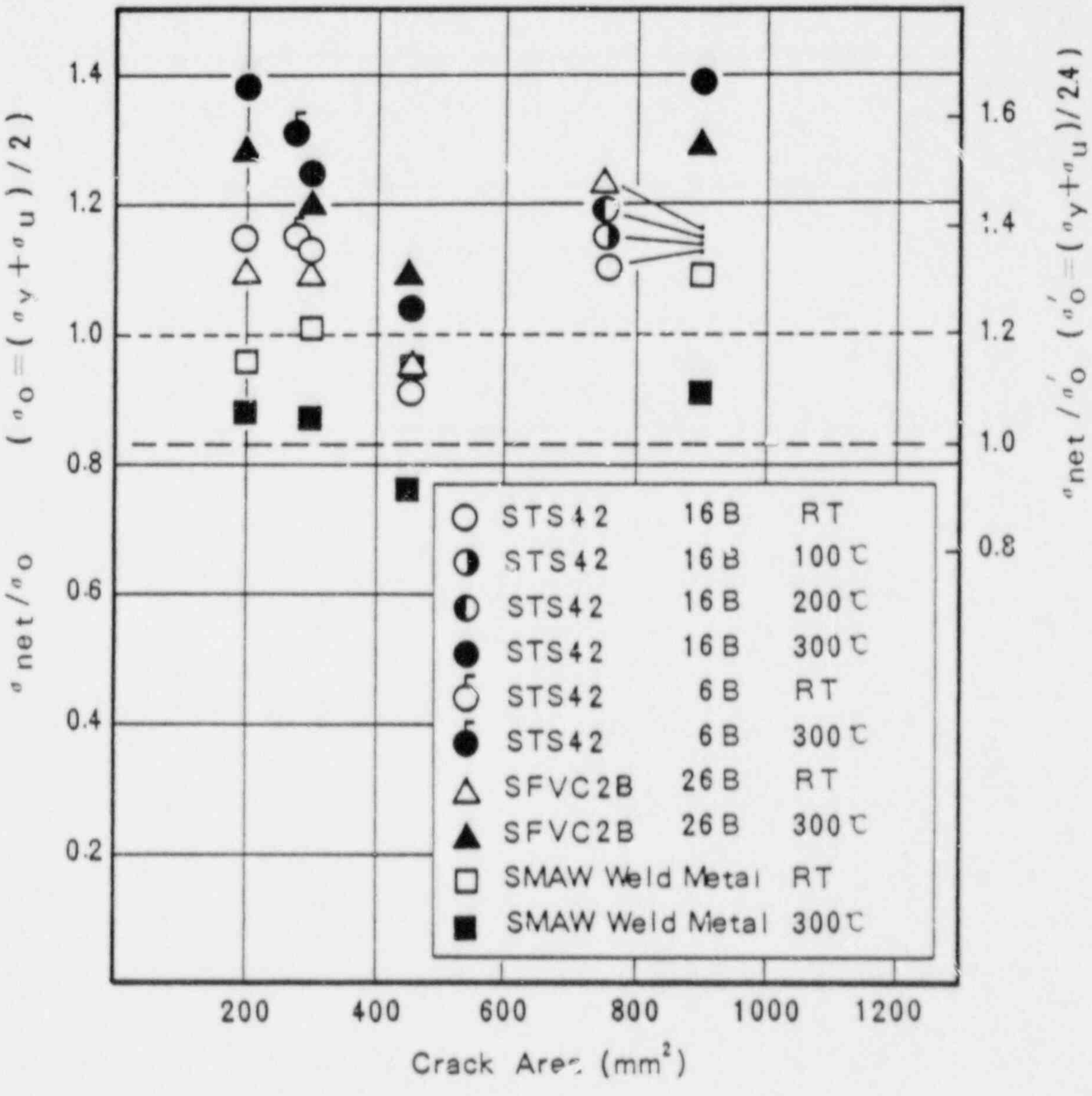
DIMENSION OF SPECIMEN AND NOTCH OF CENTER CRACKED PANEL TEST

Notch Type / Material	Through-wall Notch	Surface Notch						Specimen size	
	I	II		III		IV			
	a	a	c	a	c	a	c	Width (w)	Thickness (t)
STS42(6B)	50	—	—	—	—	—	—	100	5.5
STS42(16B)	90	90	5	90	7.5	60	5	180	10.0
SFVC2B	90	90	5	90	7.5	60	5	180	10.0
SMAW Weld Metal	90	90	5	90	7.5	60	5	180	10.

(m







NET SECTION STREES AT MAX. LOAD

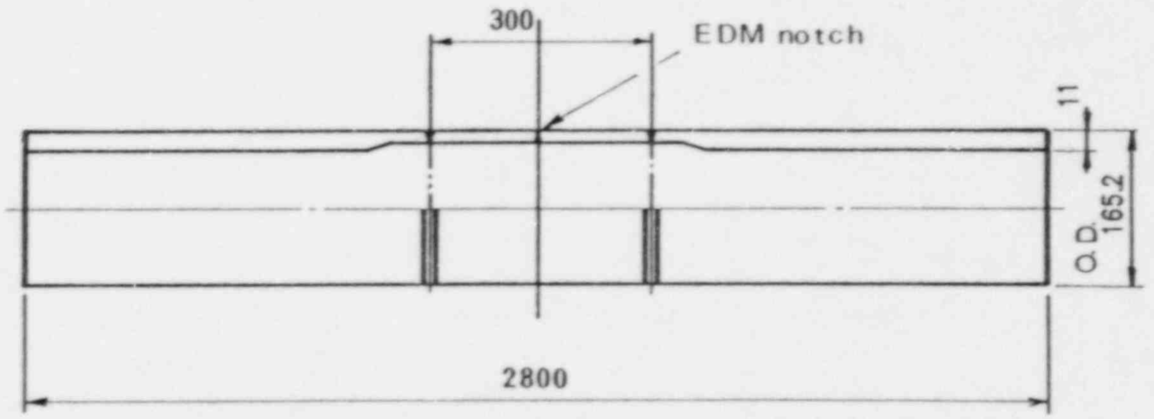
PIPE RUPTURE TEST RESULTS (4-POINT BENDING)

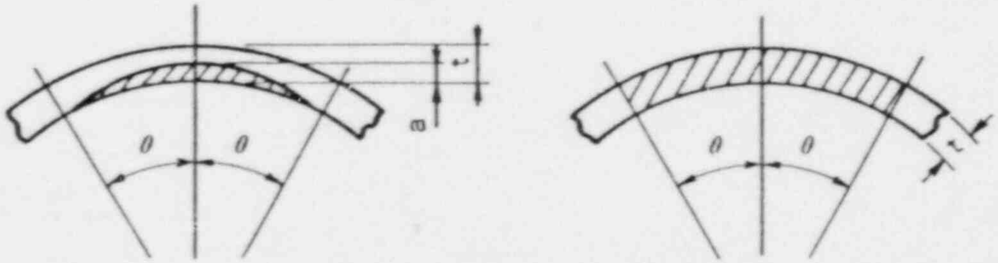
	Compliance	Pipe Dimension and Notch shape					Results								
		Outer-Dia. D_0 (mm)	Inner-Dia. d_0 (mm)	wall Thickness t (mm)	Crack Angle 2θ (°)	Crack Ratio c/t	Max. Load (kgf)	Max. Moment (kgf-m)	Net-Sec. Stress (kgf-mm)	at Maximum Load			at Crack Initiation		
										Δa (mm)	COD (mm)	** δd (mm)	Load (kgf)	COD (mm)	** δd (mm)
MB - 01	high *	165.2	145.6	9.8	60	1.0	7,200	6,660	39.3	7	5.56	94.1	6,980	3.2	68.0
MB - 02		165.2	145.6	9.8	120	1.0	4,980	4,610	44.9	10	8.03	88.3	4,580	3.3	50.0
MB - 11		165.2	145.6	9.8	60	0.51	9,420	8,710	42.6	—	0.76	239.0	9,400	1.2	254.0
LC - 1	Low	165.2	147.6	8.8	60	1.0	35,000	7,525	48.7	—	—	32	—	—	-
LC - 2		165.2	147.6	8.8	120	1.0	18,700	4,020	43.1	6	8	18	17,800	4.9	10.2

* ~ 1×10^{-3} mm/kg

** Load point Displacement

DIMENSION OF TEST PIPE AND NOTCH

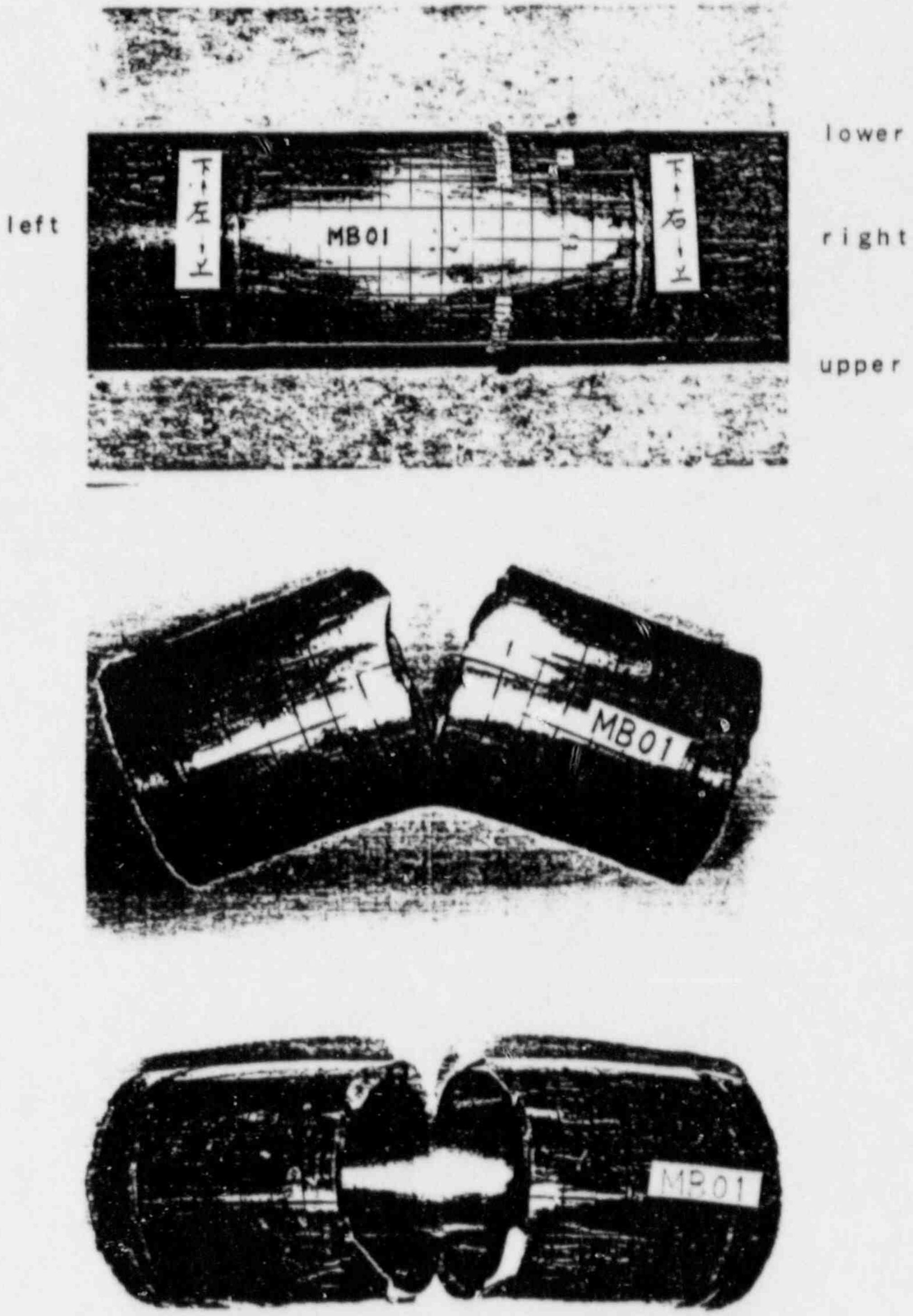




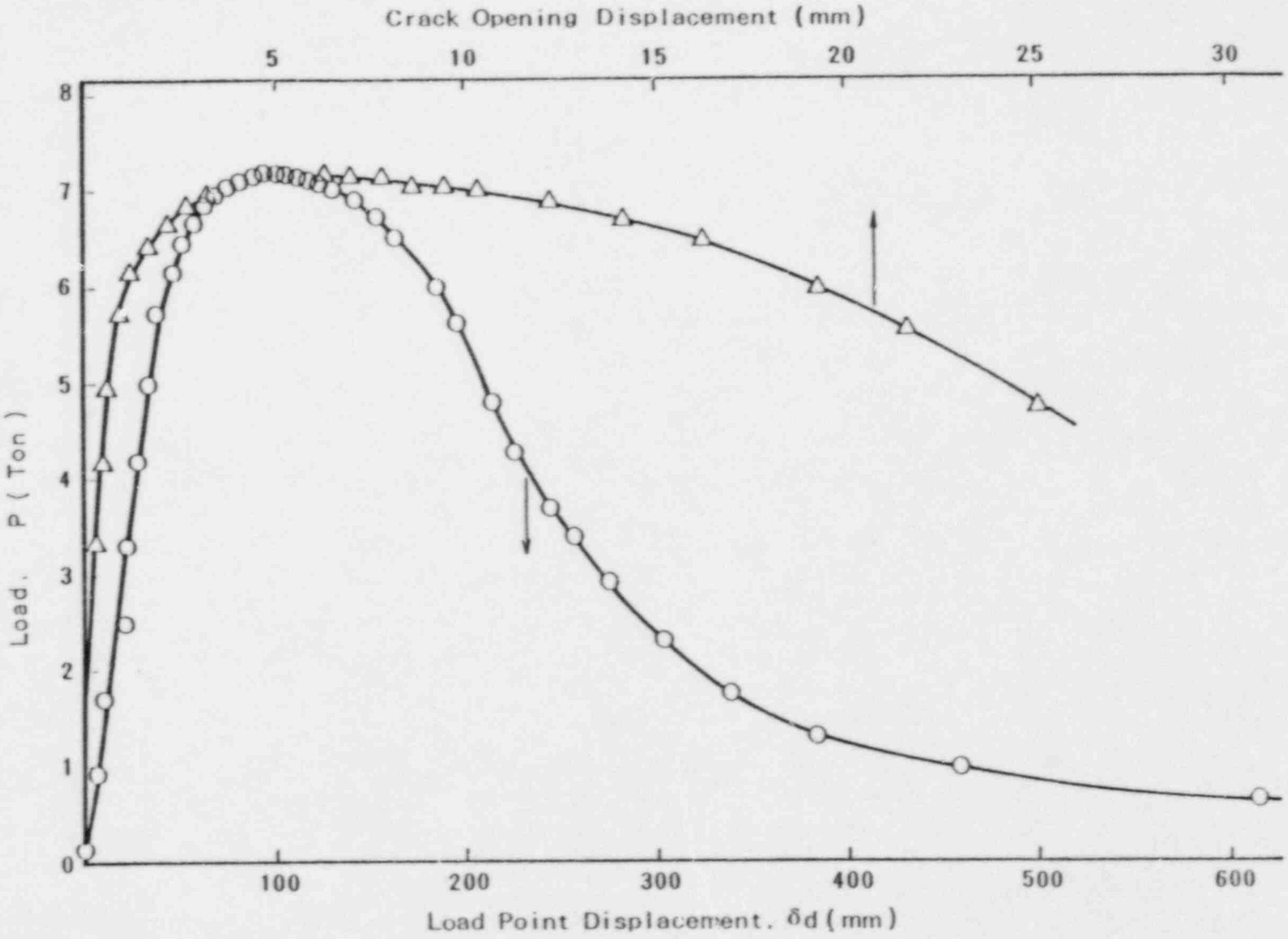
Type of crack	high compliance test[*1]			low compliance test[*2]		
	t (mm)	a (mm)	2θ (degree)	t (mm)	a (mm)	2θ (degree)
Type I (surface crack)	9.8	4.9	60	8.8	4.4	60
II (〃)	9.8	4.9	120	8.8	4.4	120
III (through wall crack)	9.8	9.8	60	8.8	8.8	60
IV (〃)	9.8	9.8	120	8.8	8.8	120

[*1] with disk spring ($C_M \sim 1 \times 10^{-3}$ mm/kg)

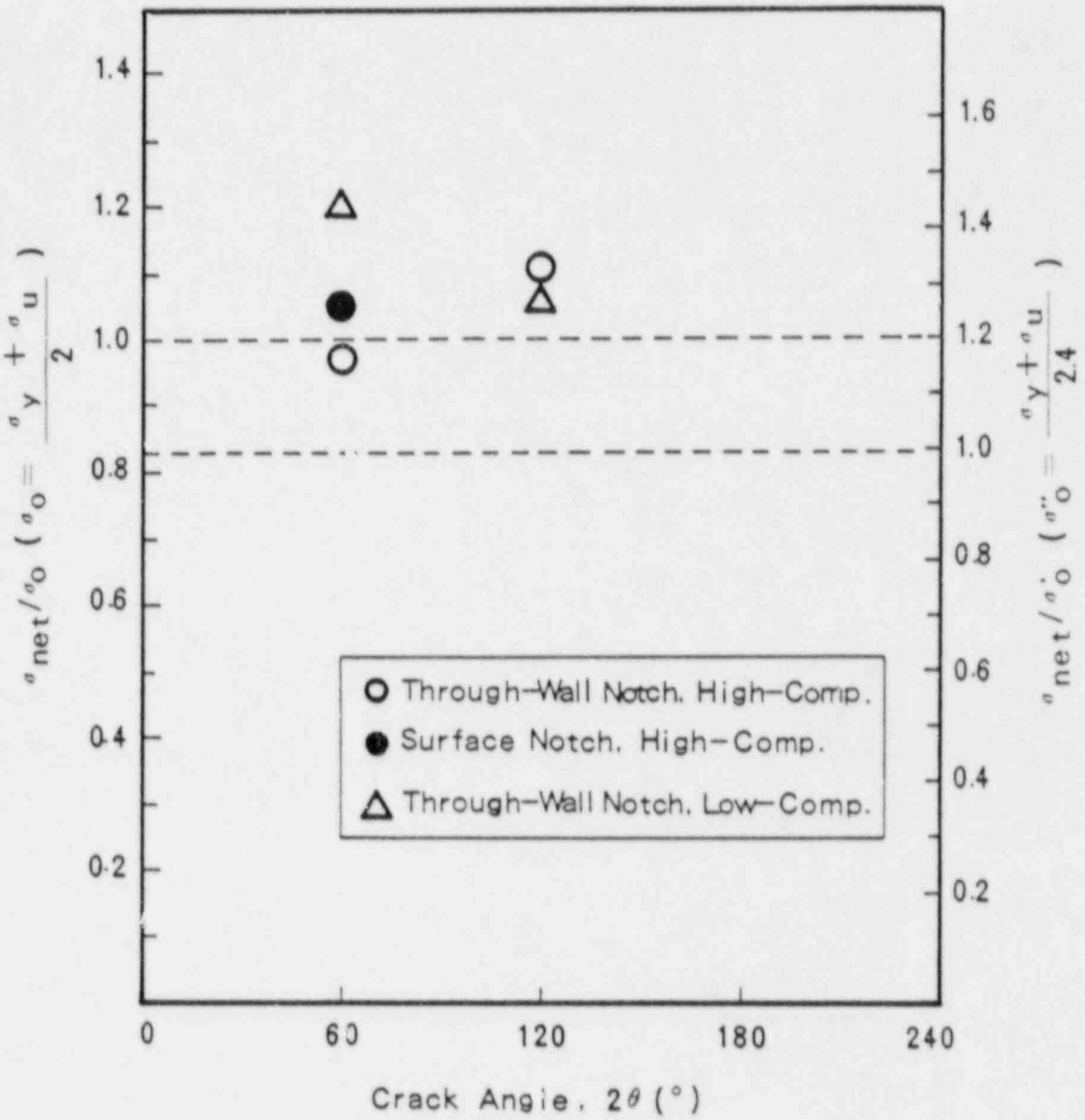
[*2] without disk spring



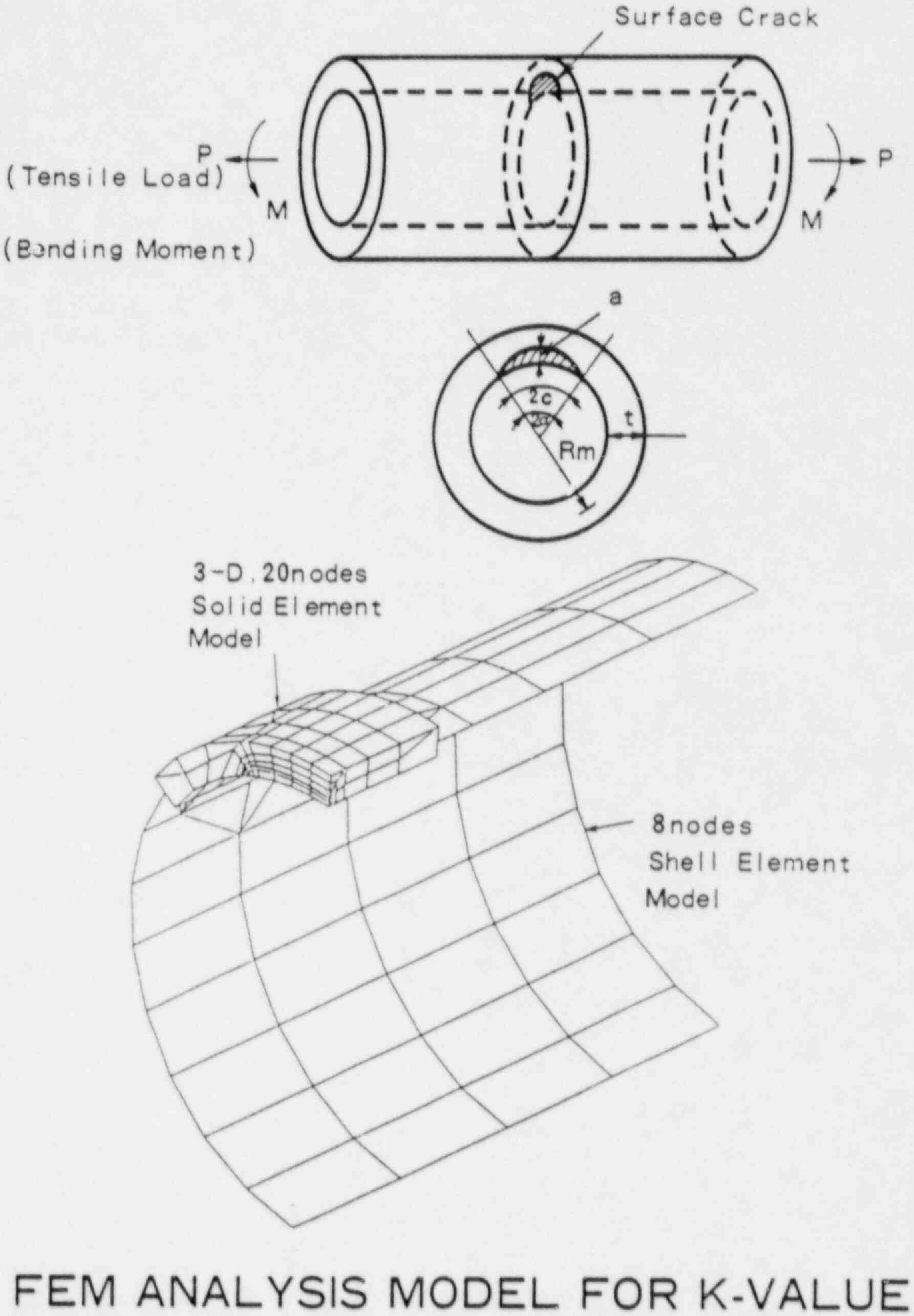
(STS42 6B PIPE, $2\theta=60^\circ$, $c/t=1$, RT)



LOAD-DISPLACEMENT CURVE(PIPE MB-01,2θ=60°,c/t=1)



NET SECTION STREES AT MAX. LOAD
(STS42,6B, 4-POINT BENDING, RT)



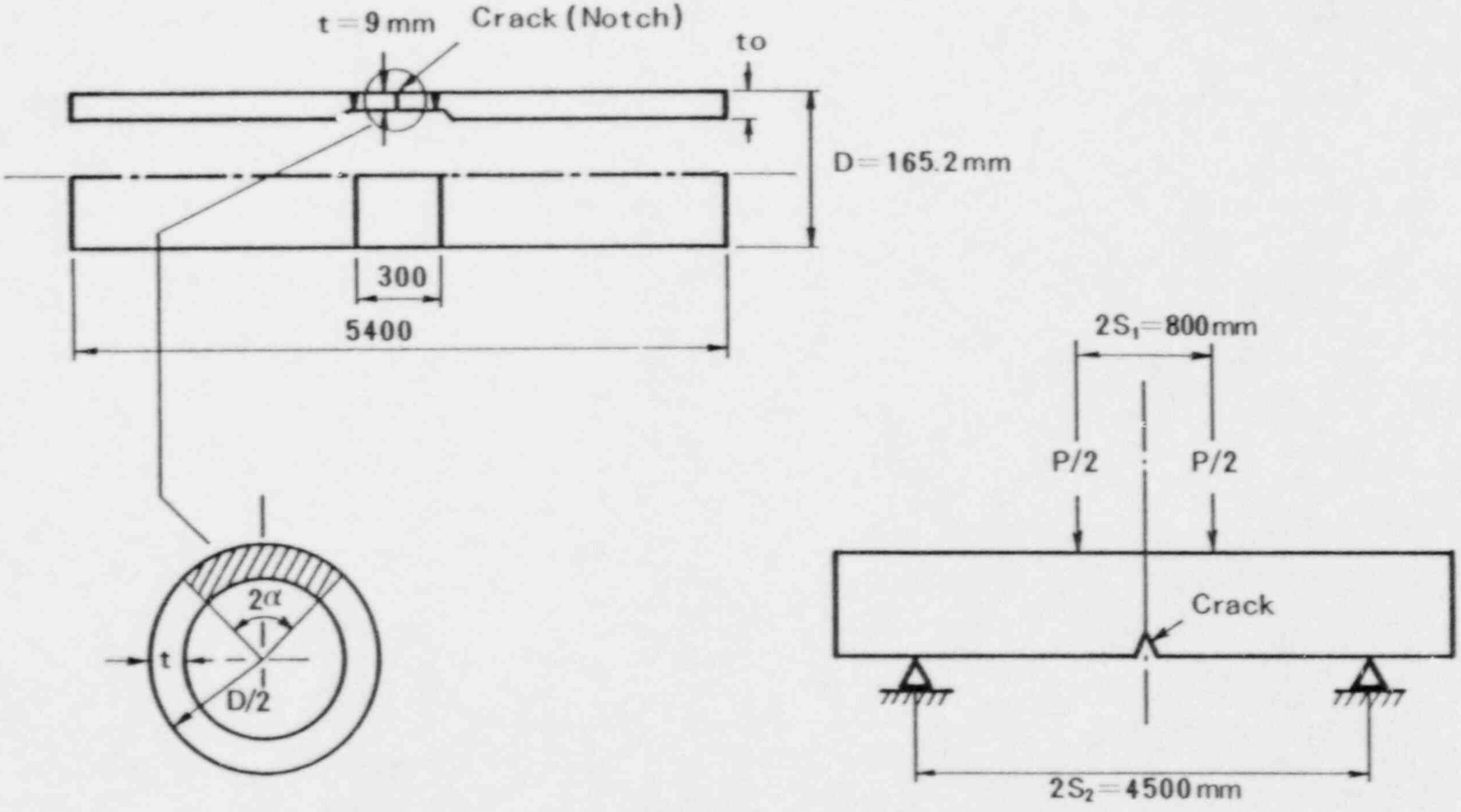
FEM ANALYSIS MODEL FOR K-VALUE ANALYSIS

RESULTS OF K-VALUE ANALYSIS

Case No.	Nominal O.D.cf Pipe	Crack Angle 2α(deg·)	Crack Depth (a)	Tensile Load(σ_t=10kg f/mm^2)		Bending Load(σ_b=10kg f/mm^2)	
				FEM (Pipe)	Neuman-Raju (Panel)	FEM (Pipe)	Neuman-Raju (Panel)
1	6 B (Rm/t=7.0)	8.8	0.2 t	22.5	25.9	19.6	25.1
2		60	0.25t	36.6	37.9	31.4	36.9
3			0.8 t	74.6	(91.2)	66.8	(85.0)
4		120	0.25t	38.5	40.0	33.1	39.0
5			0.8 t	98.7	(136.6)	86.2	(128.2)
6	16 B (Rm/t=9.0)	60	0.25t	46.9	53.9	42.7	53.8
7			0.8 t	102.7	(144.5)	94.4	(138.5)
8		120	0.25	62.5	56.6	54.9	56.0
9			0.8 t	163.5	(227.5)	145.8	(219.1)
10	26B (Rm/t=10.3)	60	0.25t	66.6	65.1	60.4	64.5

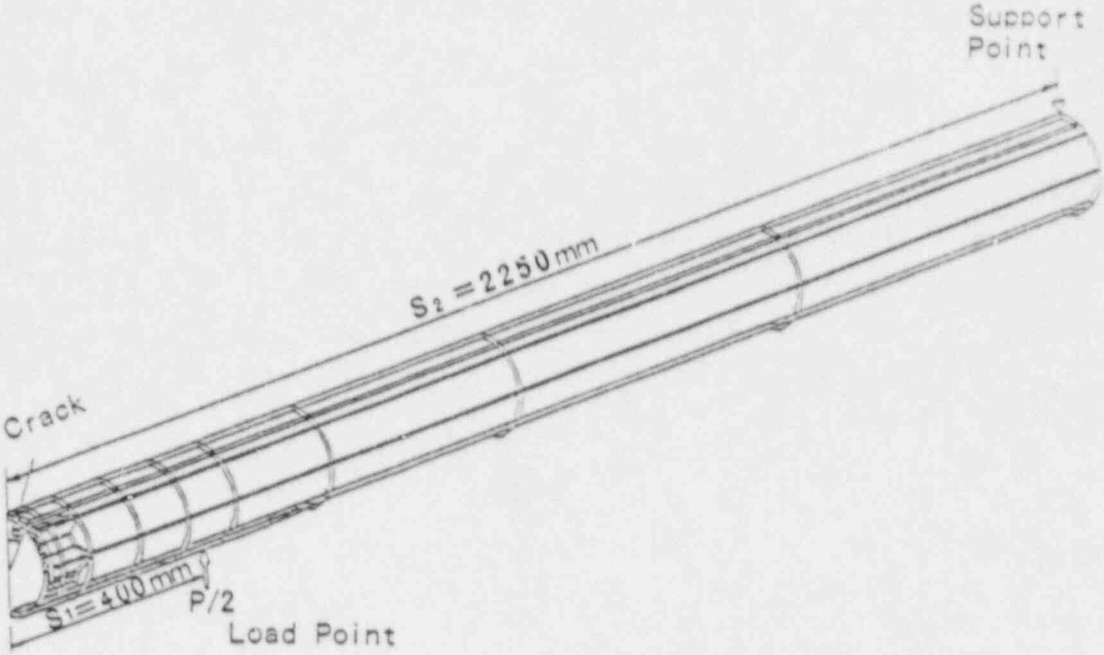
〔kg f / mm$^{3/2}$〕

note:(   ) solution are obtained from un-applicable crack size for its formula

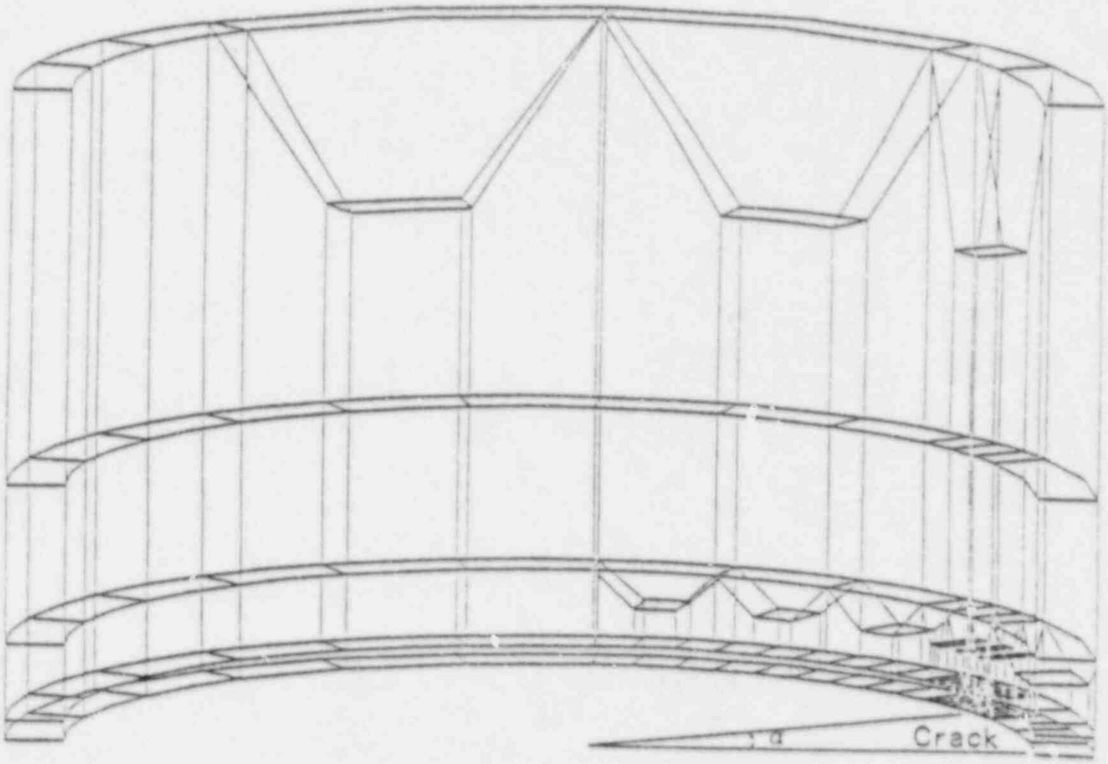


(i) Shape of Specimen ($2\alpha = 60°$)   (ii) Loading Condition

PIPE SPECIMEN MODEL UNDER BENDING LOAD



ELEMENT DIVISION MODEL



ELEMENT DIVISION MODEL

PREDICTED MAX,MOMENT OF PIPE RUPTURE

		Predicted Max. Moment (Crack Growth at Max. Moment)
FEM	Application Phase Simulation (J/T Criteria)	7.03 $\mathrm{ton}f\cdot\mathrm{m}$
	Generation Phase Simulation	6.11 $\mathrm{ton}f\cdot\mathrm{m}$
J—Estimation Scheme by GE—EPRI		6.56 $\mathrm{ton}f\cdot\mathrm{m}$
Net Section Collaps Criteria (note)		6.37 $\mathrm{ton}f\cdot\mathrm{m}$ (0 mm) 6.18 $\mathrm{ton}f\cdot\mathrm{m}$ (3 mm) 5.93 $\mathrm{ton}f\cdot\mathrm{m}$ (7 mm)
Two Parameter (R6) method		6.19 $\mathrm{ton}f\cdot\mathrm{m}$ (3 mm)
Experimental Result		6.66 $\mathrm{ton}f\cdot\mathrm{m}$ (7 mm)

note ; $Mf = 4\sigma_f Rm^2 t\left(\cos\frac{\alpha}{2} - \frac{1}{2}\sin\alpha\right)$, σ_f ; flow stress

FUTURE DEVELOPMENTs

- Pipe Rupture Experiments 1987 FY
  - —6B, 16B Pipe
  - —RT, 300°C
- Finite Element Analysis 1987 FY
  - —K-value
  - —Stability of Crack
- LBB Verification Test 1988 FY
  - —Straight Pipe, Tee, Elbow
  - —High Temperature Water Environment
  - —Cyclic Load
- Finite Element Analysis 1988 FY
  - —K-value
  - —Stability of Crack
- Evaluation and Standardization 1988 FY
  - —Case study
  - —Proposal Guide

SESSION 3: RECENT RESEARCH PROGRAM ON LBB (PART 2)

Chairman: P. P. Milella, ENEA, Italy

ONTARIO HYDRO's LEAK BEFORE BREAK APPROACH FOR DARLINGTON NGS A

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ACKNOWLEDGEMENTS

The co-operation, technical input and assessment, and significant contributions from many Departments and Divisions within Ontario Hydro resulted in a successful development of the LBB approach for Darlington NGS A. This paper has been prepared with input from C.S.Kim and B.L.Kee (Nuclear Studies and Safety Department), M.Kozluk (Mechanical Design Department), B.Mukherjee (Metallurgical Research Department) and M.Flaman and J.M.Boag (Mechanical Research Department).

1.0 INTRODUCTION

A "Leak-Before-Break" (LBB) approach has been developed for application to the large diameter piping in the heat transport (HT) system for Ontario Hydro's most recent four unit (881 MWe per unit) Darlington Nuclear Generating Station A (Reference 1). The first of the four Darlington units is scheduled to go critical in May, 1988.

Application of the LBB concept is, at this time,limited to primary heat transport piping larger than 21 inched diameter piping*. A pipewhip assessment performed prior to development of the LBB concept (Reference 2) had shown that the consequences of pipe-whip for all other pipes inside containment were either acceptable, or, there were adequate provisions in the design to protect against the dynamic effects of rupture.

The objective here is to illustrate how each of the major elements in the overall leak-before-break approach provides the framework for the assessment of piping integrity. The status of the work completed to date and current efforts underway to support the program are also presented.

A systematic review and a critical evaluation of the failure mechanisms and causes which could jeapordize integrity of the specific piping is, in our view, an important first step in establishing the scope for application of the LBB concept. This was completed for Darlington NGS A (Reference 1). The intent is to provide assurance that adequate protection from failures attributable to each relevant potential failure mechanism is provided, or, that sufficient provisions are incorporated in the design to cater to each failure mechanism evaluated as being credible. The conclusion is that, of all the possible failure mechanisms, fatigue can be considered to be active in the piping system. The existing design practices, commissioning checks and periodic inspections all contribute to minimizing the likelihood of failures from this cause. However, fatigue is a plausible crack growth mechanism for the primary heat transport piping and application of Elastic Plastic Fracture Mechanics (EPFM) methods for demonstrating the safety margin is appropriate.

The elements integral to the Ontario Hydro LBB approach are shown in Figure 1. Generally, the elements can be grouped into two areas : (a) those related to demonstration of crack stability utilizing fracture mechanics; and (b) those related to "leakage".

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*The piping is ASME Class 1 and is a seamless, fine grain SA 106 Grade B carbon steel.

2.0 FRACTURE EVALUATION

The proposed approach incorporates several levels of defence-in-depth. It is demonstrated that the largest inaugural part-through flaw, which can be detected, will not grow through the pipe during its design life and that such flaws are stable for the maximum credible piping loads. At a further level of conservatism, analyses show that a postulated through-wall crack will not extend in an unstable manner and that the leakage rate is well within the capabilities of the system to detect.

For evaluation of crack stability, the J-integral/tearing modulus (J/T) approach is used. The J/T approach has been selected since it is a general procedure that incorporates a rational crack tip parameter and it can discriminate between materials of different toughness and tensile properties.It can also accommodate various boundary conditions such as load vs displacement control and pipe system characteristics (Reference 3).

2.1 Materials Test Program

An extensive material test program (Reference 4) to determine the J-resistance curves (J-R) and J-T curves from actual Darlington NGS A large diameter heat transport piping, forgings, associated welds and heat affected zones has been completed. The test program was designed to take into account the effect of factors such as test temperature, crack plane orientation, and welding effects which can have an influence on fracture properties. The objective has been to identify those factors which tend to lower the J-R curves and, subsequently, to apply the appropriate lower bound curves obtained from the test program as input to EPFM analyses.

The test results (Reference 4) show that piping used in the construction of Darlington HT piping and all welds and heat affected zone materials within the scope of the LBB program exhibit uppershelf toughness behaviour. All specimens show high crack initiation toughness J_{Ic}, rising J-resistance curve and stable and ductile crack initiation. Toughness of product forms depends on the direction of crack extension (circumferential versus axial crack orientation). For a given orientation and temperature, toughness of all product forms was reasonably uniform. Additionally, the material test program has been validated by comparing six test results against tests conducted by an independent materials testing laboratory in the U.S. The J_{Ic} and J-resistance curves obtained by Ontario Hydro and the external test laboratory were comparable.

2.2 Elastic Plastic Fracture Mechanics(EPFM) Modelling

The finite element program ABAQUS (version 4.6), a general purpose linear/non-linear structural analysis code (Hibbitt, Karlson and Sorensen) is being used to perform the EPFM analyses. The analysis is being performed not only for circumferentially oriented cracks at girth-butt welds but also longitudinally oriented cracks in fittings, namely, elbows, tees and branch connections.

Benchmarking of the ABAQUS code has been performed against published finite element solutions and alternative methods of analyses. Some of the factors considered are (Reference 5):

- element selection; choices include the use of shell versus solid elements, full versus reduced integration, and compressible versus incompressible material behaviour;

- crack-tip mesh discretization including the use of spider-web versus rectangular meshes and the use of collapsed elements for the crack-tip, with and without 1/4 point edge nodes;

- mesh refinement studies;

- acceptable nodal tolerances for the convergence to equilibrium in elastic-plastic analyses; and

- use of incremental versus deformation theories of plasticity.

The effect of crack lipping on leakage rate, the effect of including the hoop component of stress and the effect of pressure acting on the crack faces have been considered. The results show that crack lipping can significantly influence leakage rates whereas the hoop component of stress has a negligible effect on the crack opening area, leakage rate or J-integral for circumferential cracks in pipes. However, the pressure acting on the faces of the crack can significantly increase crack opening area, leakage rate and the J-integral (Reference 5).

3.0 LEAKAGE

3.1 Leakage Detection Capability

With respect to leakage, the approach taken in developing the LBB concept for Darlington was to assess the capability of existing systems to satifactorily detect the required levels of leakage from the heat transport system. Current operating policies and principles at similar Ontario Hydro facilities (viz. Bruce NGS A and Bruce NGS B) require immediate shutdown action to be initiated upon detection of a 0.5 kg/s leak rate from the heat transport system. This requirement, based on an extensive operating history and the proven capability of leakage detection systems, has been adopted by Darlington Operations. Leak rates from the heat transport system significantly less than 0.05 kg/s (i.e.,10% of the immediate shutdown action limit) are within the capability of the leakage detection systems. For a CANDU type design, the motivation to react to low levels of leakage are dictated both by stringent emission limits (Derived Emission Limits for tritium) and economic penalties which result from lost or downgraded heavy water. No modifications to existing systems used to detect leakage from the HT system were required to support the LBB concept for Darlington NGS A. However, operating procedures and checks were formalized to support LBB.

3.2 Leak Rate Models

A leak rate estimation code, LEAK-RATE, has been developed by Ontario Hydro. The code has been verified against available experimental data and provides a good estimate of the leak rates through cracks in piping to within 15%. The code also predicts pressure profiles which include exit pressures and location of the point of flashing. Conservative methods are used in conjunction with the use of LEAK-RATE in order to obtain a lower bound estimate of the leak rate for a given crack size. The crack opening (width) used in the LEAK-RATE code is calculated by assuming that only normal internal pressure in the pipe acts to open the crack (bending moments are not credited). This approach assures margins on leak rate and thus provides confidence that the LBB assessment is conservative.

Since there is a limited amount of experimental data available for leakage rates through actual cracks, Ontario Hydro is scoping out further experimental work in support of leak-rate model validation. Further work with fluid representative of heat transport coolant leaking through cracks in carbon steel piping similar to that used in the Darlington heat transport circuit is being planned. It is judged that the efforts to support the computer code development and verification of the critical assumptions employed in the code would significantly improve our capability to accurately model and predict the leakage flow through cracks in piping.

3.3 Leak Rate Tests

A major Burst Test Facility (BTF) has been designed, approved and will be built at the Ontario Hydro Research Division in late 1987 for full-scale high energy testing of pressure vessels and piping components (Reference 6). This facility is designed so that it will be able to accommodate representative lengths of full-scale heat transport piping sizes used in Darlington and other CANDU nuclear stations.

The facility will be an above-ground self-contained heavily-reinforced concrete structure comprised of a Control Room, a Mechanical Service Room, and Burst Containment Area (testing area). The Burst Containment Room will be a long, enclosed volume with inside dimensions of approximately 12m long x 4m wide x 3m high. Massive reaction steel beams will be installed into and flush with the floor so that loads can be applied to the piping during testing, as required. Such loads would include large forces and moments as well as vibration. By means of external heating equipment, thermal loads may also be imposed. Other loading equipment will include a pressurization system and a water heating and circulation system.

From the perspective of leak rate testing, one of the most important- and virtually unique - features of the Ontario Hydro facility is the capability (from the existing Pump Test Building) to supply large quantities of high-temperature pressurized water (i.e.,at HT system operating conditions of up to 13.8 MPa and 300 C). This hot, pressurized water can be supplied to the piping test component at a continuous rate of 0.5kg/s for more than 15 hours or at substantially higher rates for correspondingly shorter periods of time. The storage reservoir has 26,500 litres of hot, pressurized, conditioned water which can be drawn upon with a continuous make-up capability of 1000 litres/hour. The control of water chemistry is within the present capabilities of the existing Pump Test Building.

Precise water flow measurements at HT system operating conditions, required to determine crack leak rates, will be achieved by directing flow through control pipes of different diameters. The leak rate can then be determined with full flow turbine meters. Typical turbine flow meter ranges will include 0.01 - 0.1kg/s; 0.05 - 0.5kg/s; 0.25 - 2kg/s; and 1-10 kg/s. Because of the continuous supply of hot pressurized water to the test component, steady state leak conditions can be established and maintained. Therefore, precise leak rates can be accurately measured by the "cascade" turbine flow meter approach. Even small changes in leak rates during the progress of a test can be continually measured throughout the test duration (possibly several hours per test). This is seen as an important capability for LBB tests and will be unique in North America.

In Summary, a significant amount of work has been completed at Ontario Hydro to support the LBB Program. The progress in this area has also resulted in a significant development of our capabilities within our organization. Based on the work completed, we are confident that the overall strategy provides a reliable method for demonstrating piping integrity.

REFERENCES

1. Kee, B.L. and Nathwani, J.S., "Darlington NGS A Leak Before Break Approach for Large Diameter Heat Transport (HT) Piping", Nuclear Studies & Safety Department, Ontario Hydro Design & Development Division. D&D Report No.85371, December, 1985.

2. Creates, D.H. and Kee, B.L., "Darlington NGS A Heat Transport Qualitative Pipewhip Assessment Report (For Pipe sizes 6 inch and Greater)", Ontario Hydro Design & Development Division, D&D Report No.85167, April, 1985.

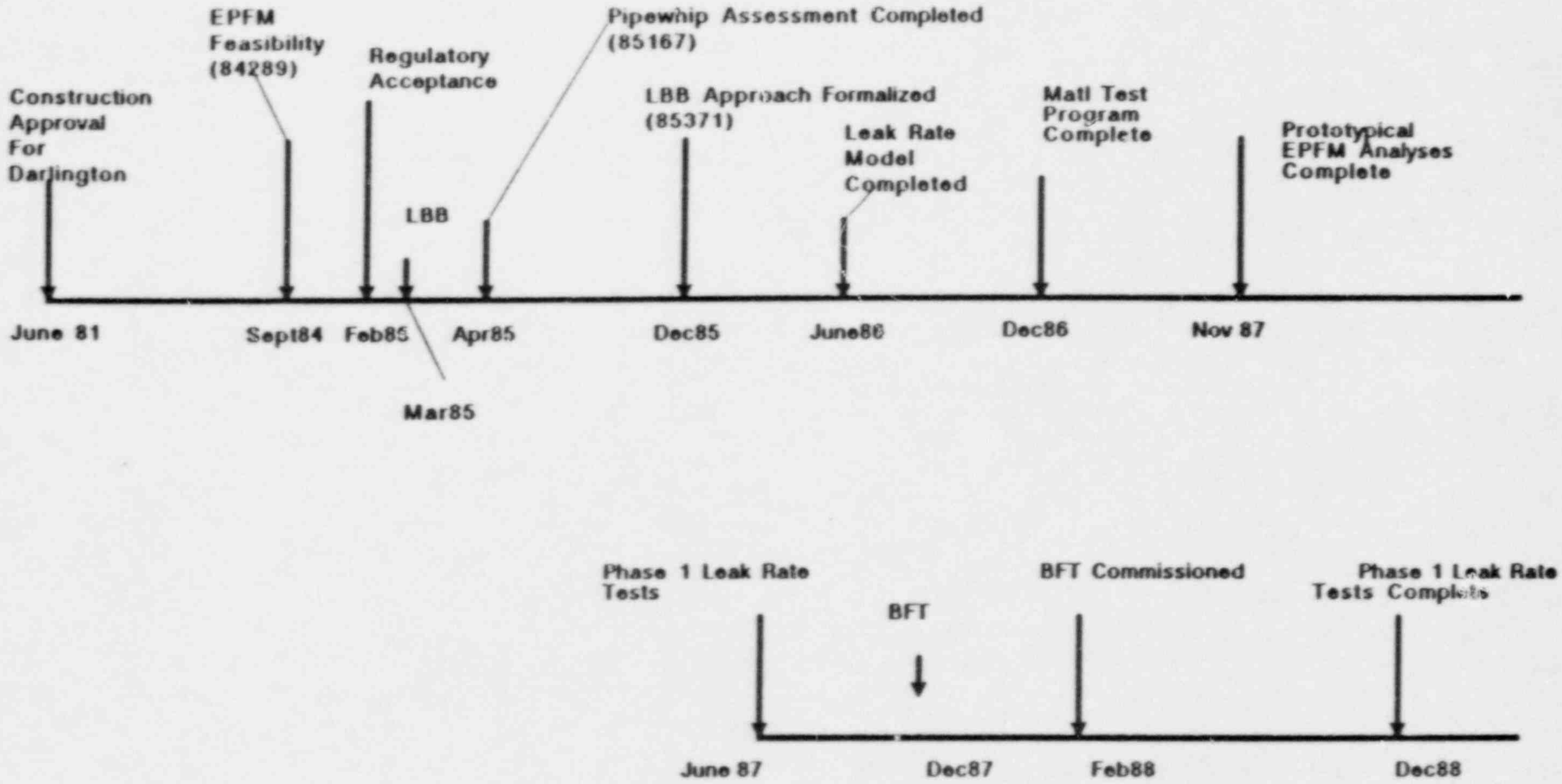
3. USNRC, "Evaluation of Potential Pipe Breaks", Report of the U.S. Nuclear Regulatory Commission Piping Review Committee, NUREG-1061, Volume 3, November,1984.

4. Mukherjee, B and Carpenter,D., "Darlington Leak Before Break Material Test Program J-Resistance Curves: Volume 1- General Summary, Results and Conclusions," Ontario Hydro Research Division, Report No. 87- K --, April 1987.

5. Kozluk, M.J., Lin, T.C., Manning, B.W., Scarth, D.A., and Vanderglas, M.L.,"Effects of Pressure Loading on Throughwall Cracks", Transactions of the 9th International Conference on Structural Mechanics in Reactor Technology, Lausanne, Switzerland, Paper G/F 11, August, 1987.

6. Flaman, M.T. and Boag J.M.,"Experimental Program in Support of the Leak Before Break Approach for Darlington NGS HT Piping-Phase 1/ Carbon Steel Pipe Leak Rate Measurements", Ontario Hydro Research Division Report No 387-16-K, April 1987.

ONTARIO HYDRO's LBB PROGRAM



June 81 – Construction Approval For Darlington

Sept84 – EPFM Feasibility (84289)

Feb85 – Regulatory Acceptance

Mar85 – LBB

Apr85 – Pipewhip Assessment Completed (85167)

Dec85 – LBB Approach Formalized (85371)

June86 – Leak Rate Model Completed

Dec86 – Matl Test Program Complete

Nov 87 – Prototypical EPFM Analyses Complete

June 87 – Phase 1 Leak Rate Tests

Dec87 – BFT

Feb88 – BFT Commissioned

Dec88 – Phase 1 Leak Rate Tests Complete

ONTARIO HYDRO's LBB PROGRAM

BRANCH	DIVISION	DEPARTMENT
1 DESIGN & CONSTRUCTION	Design & Dev – Gen Projects & Const.	Nuclear Studies & Safety Mechanical Design Nuclear Systems Instrumentation & Control Quality Engineering Darlington Engineering, Construction and Generation Services
2 OPERATIONS – PRODUCTION	Nuclear Generation	Darlington Operations
	Technical & Training Services	Central Nuclear Services
3 POWER SYSTEM PROGRAM	Research	Metallurgical Research Mechanical Research

External Consultants – Novetech Corporation (R.Gamble and A.Zahoor)

Hibbitt, Karlsson and Sorensen (H.Hibbitt, C.F. Shih and D.M.Parks)

TOTAL COSTS : LBB Program $ 2,960,000 ($ 2.2 M Estimated in March 1985)

EPFM for LBB $ 1,900,000
(Development, Verification and Application)

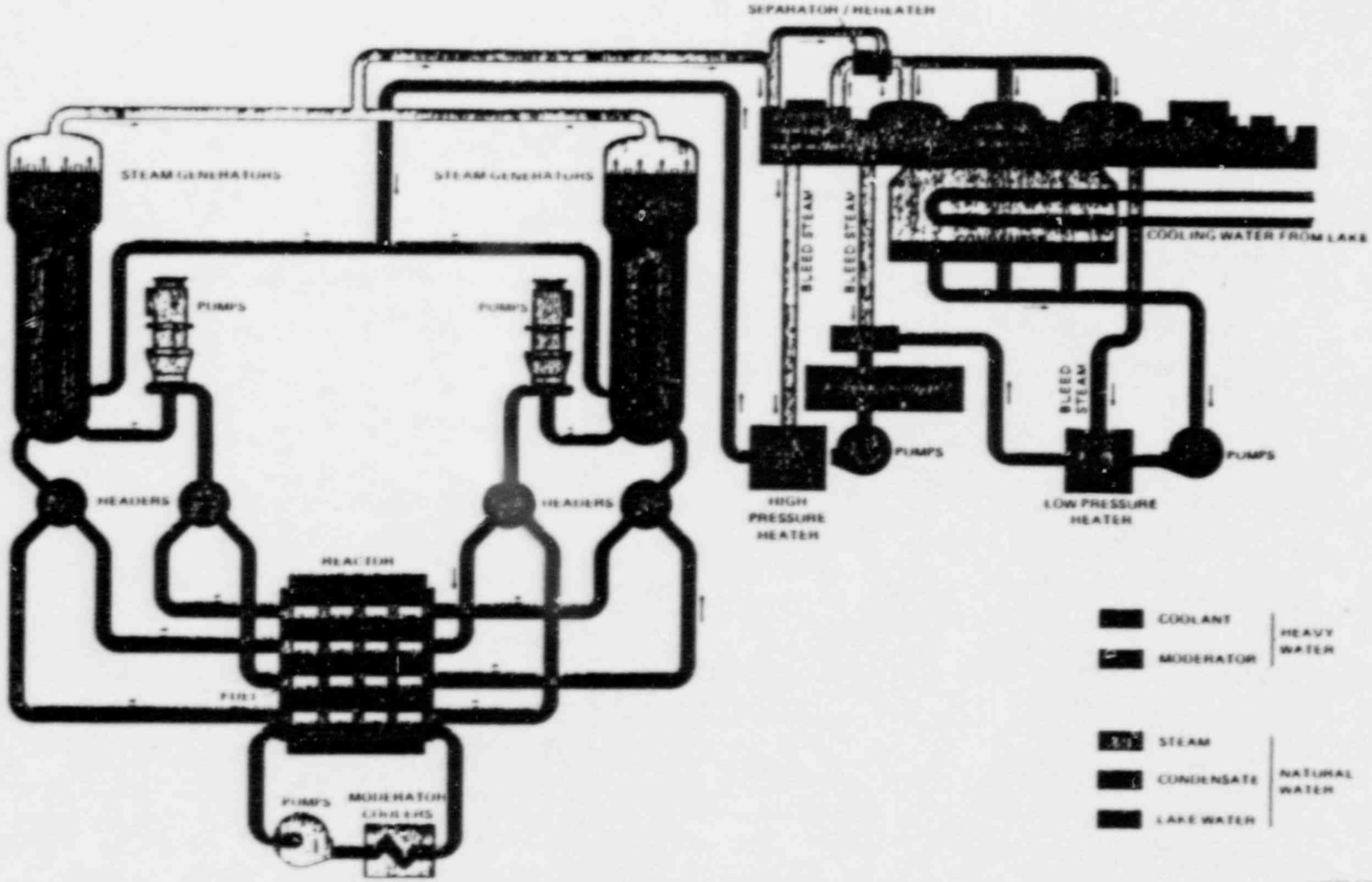


FIGURE 11.6
Simplified Unit Flow Diagram

Darlington – CANDU Design Features

1. SEPARATE – Moderator and Heat Transport Systems

2. Moderator System/Circuit – Low Temp.
   – Low Pressure
   – Austenitic Steel

3. Heat Transport System – Coolant Pressur 9.7 MPa
   – Temp. 265 C

4. On – Power Fuelling and Natural Uranium Fuel
   Chemical Shim Control (i.e. boric acid) unnecessary
   Given above, corrosion – related failure mechanisms not a concern

5. Elimination of Corrosion and Low Operating Pressure
   Allows Use of Low Alloy(Uncladded) Ferritic Steel for
   The Heat Transport System.

6. Presence of Tritium and Heavy Water Provide Significant
   Incentive for Leakage Monitoring and Control

DARLINGTON LBB APPROACH

APPLICATION LIMITED TO LARGE DIAMETER HEAT TRANSPORT SYSTEM PIPING

- 21 inch Diameter Heat Transport Pump Discharge;

- 22 inch Diameter Steam Generator Inlet

- 24 inch Diameter Heat Transport Pump Suction

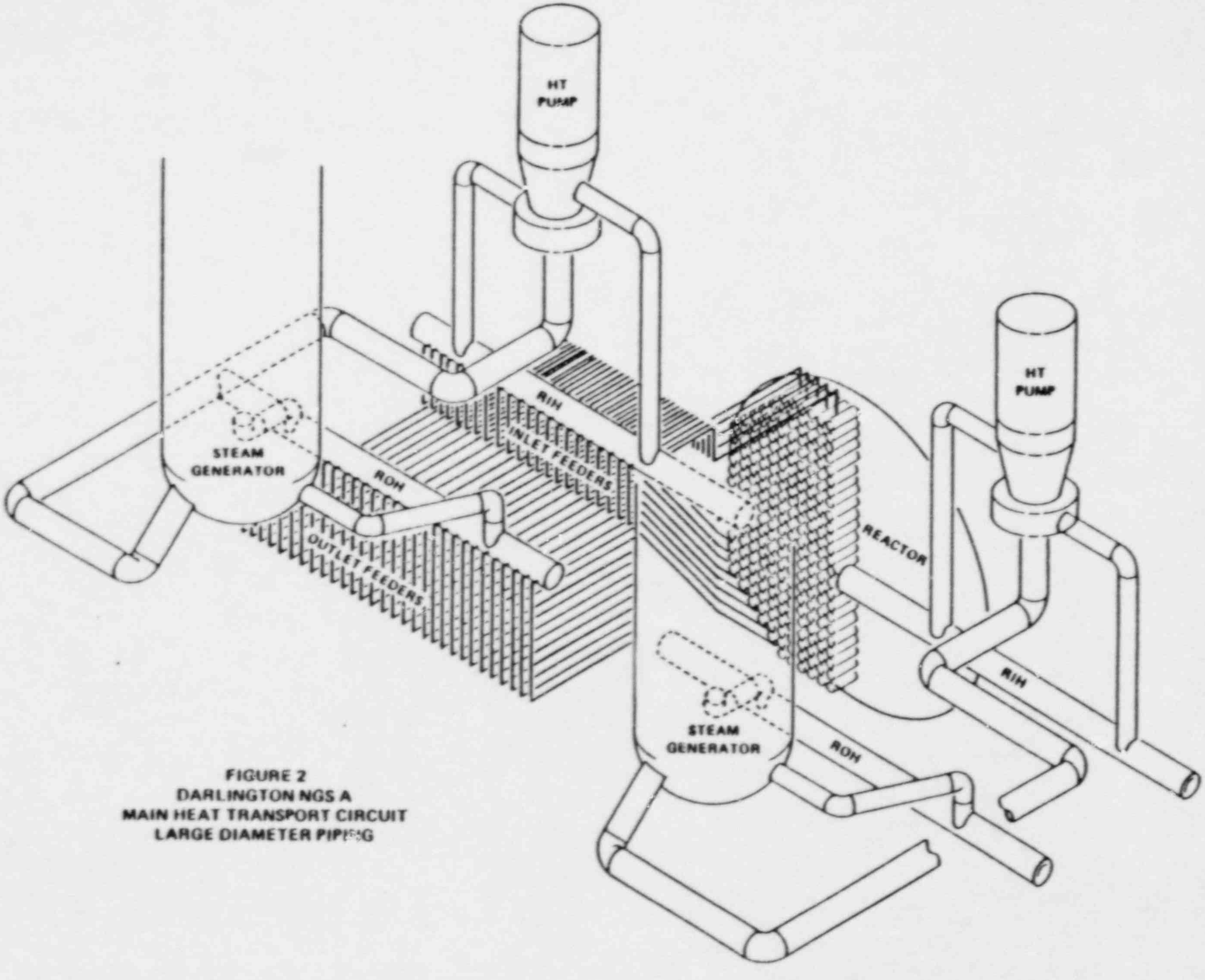


FIGURE 2
DARLINGTON NGS A
MAIN HEAT TRANSPORT CIRCUIT
LARGE DIAMETER PIPING

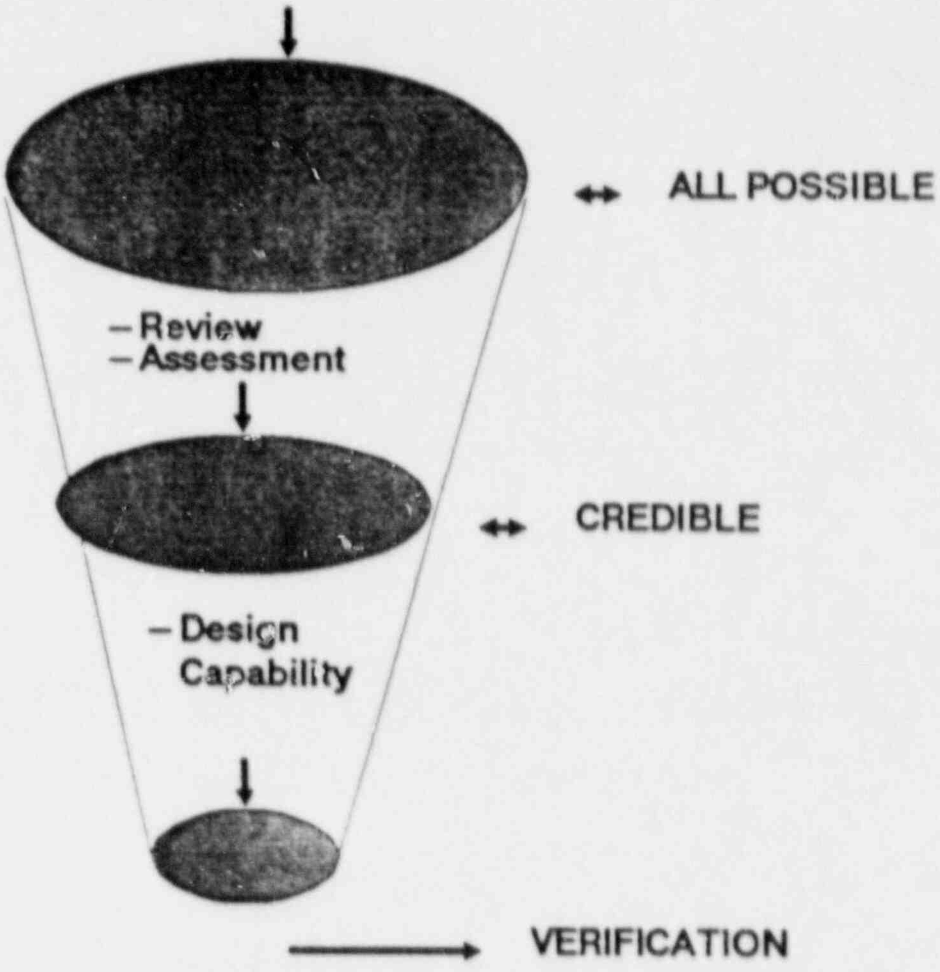
LICENCING

Maintain :

- Existing Features incorporated to preclude unacceptable consequences.
- Existing Commissioning, Operating and inspection practices
- Defence in Depth by Assuring Containment, Shut – down Systems, Moderator
- Large LOCA for sizing of Containment, ECI, Shut – down systems and safety analysis

Use of LBB for Large Piping in Lieu of Pipewhip Restraints

LBB For Darlington NGS A



Mechanisms and Causes of Pipe Failure

ALL POSSIBLE

- Review
- Assessment

CREDIBLE

- Design Capability

VERIFICATION

Assessment of Failure Mechanisms:

CONCLUSION

Given the Design, Fabrication and Installation Practices, Inspections, and Operational Checks, Catastrophic Rupture for large diameter piping is not a realistic concern.

\- - - - - - - - - - - - - - - -

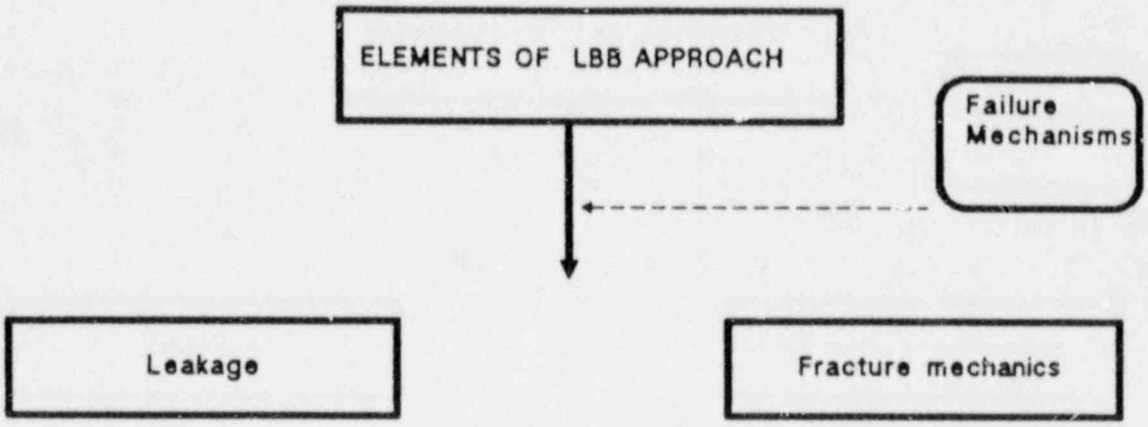
Added Monitoring/Assessment For:

1. EROSION (Steam – Water)
   In HT Outlet Piping (2 – 3% Quality(20% Steam)
   Thickness Measurements During Inaugural and Periodic Inspection at those locations considered susceptible. (CSA N285.4)

2. FRETTING
   Checks during Construction/Commissioning to Verify No Components in Close Proximity to Large HT Piping

3. CORROSION
   Checks of Autoclave Coupons Following Severe Abnormal Chemical Transients or CANDECON

4. FATIGUE
   Consequences of Failure Acceptable for Piping <21 in
   LBB For Large Diameter Piping >21 in



ELEMENTS OF LBB APPROACH

Failure Mechanisms

Leakage

- Operating Procedures and Checks
- Leakage Detection Capability
- Leak Rate Models and Validation

Fracture mechanics

- Material Properties
- Modelling (ABAQUS)

LBB and Fracture Evaluation

1. Based on EPFM Approach Developed in the U.S.

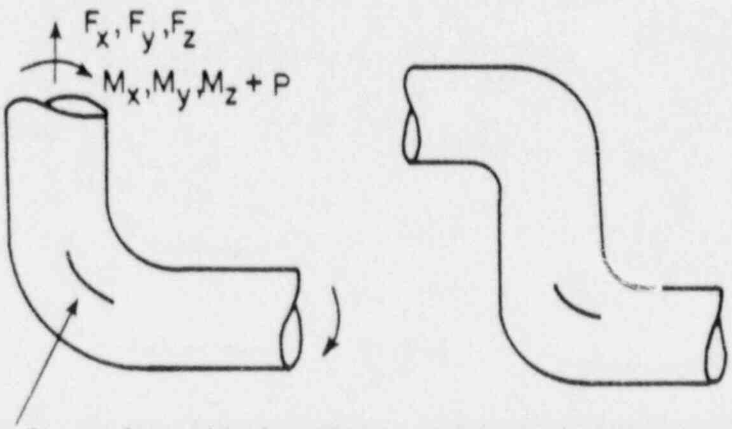
2. Through – Wall Crack Postulated in Piping

   - the crack size corresponds to that for which a conservative estimate of leakage corresponds to the immediate shutdown action limit

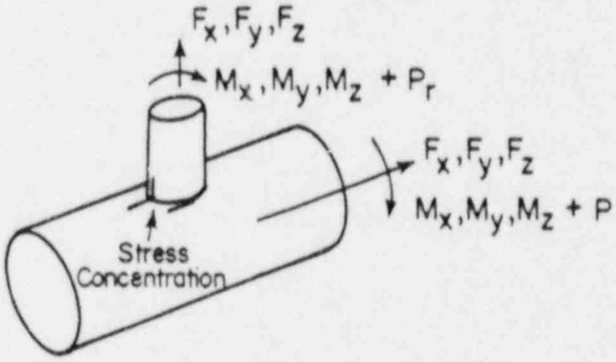
The System is Subjected to Largest Postulated Load

↓

Demonstrate Margins to Unstable Crack Propagation



Elbow Crown: Area with highest hoop stress/strain concentration under pressure and inplane bending moment initiation of Longitudinal crack probable.



Branch (sweepolet, vessolet) / Tee Connection

AIMS OF PAST/CURRENT FINITE ELEMENT ANALYSES

- VERIFY AND BENCHMARK FRACTURE MECHANICS CAPABILITIES OF ABAQUS
- DETERMINE ELEMENT TYPE TO USE
- DETERMINE CRACK-TIP MESHES TO USE
  - COLLAPSED NODES
  - PLACEMENT OF MID-SIDE NODES
- DETERMINE ADEQUATE MESH REFINEMENT
- DETERMINE SUITABLE VALUES PTOL/MTOL
- DEFORMATION PLASTICITY (USING R/O) VERSUS INCREMENTAL PLASTICITY
- PROTOTYPICAL ANALYSES

LEAKAGE

INTEGRAL ELEMENTS ARE:

1. Operating Procedures and Formal Checks
2. Leakage Detection Capability
3. Leak Rate Models and Validation

LEAKAGE

1. LBB – OPERATING PROCEDURES FOR SYSTEM LEAKS

OBJECTIVE:

– Demonstrate Adequacy of Margins to Support LBB

APPROACH:

– LBB Based on Proven Capabilities of Existing Systems and Operating Procedures and Practices

RESULT:

LBB is based on the Operating Requirement for Immediate Shutdown Action for an Heat Transport System Leak Rate of 0.5 KG/S (7.2 USGPM)

2. LBB – Leakage Detection Capability

Chronic Leakage into Containment (Normal Conditions "Unidentifiable")

0.003 KG/s
(0.043 USGPM)

Immediate Shutdown HT Leakage Limit (LBB Leak Rate)

0.5 KG/s
(7.2 USGPM)

Large Margins Exist Between Minimum Detectable Leak Rate and Leak Rate Upon Which LBB is Based

COMMITTED OPERATING PRACTICES TO SUPPORT LEAKAGE DETECTION CAPABILITIES

1. Assure Availability of Leakage Detection Capability
2. Daily Estimates of HT Leakage into Containment Using HT Heavy Water Storage Tank Level Trends
3. Daily Estimates of HT Leakage into Containment by estimating Vapour Recovery Drier Collection Rates
4. Daily Checks of the Contaminated Air Exhaust System Stack for Heavy Water Losses and Tritium Emissions
5. Level Deviation Alarm on Heat Transport Storage Tank to be Set at 2% of Normal Level (1650 KG)

Leak Rate Models and Validation

1. "LEAK RATE" Code Developed

2. Verified Against Available Experimental Data

- Predicts Leak Rates to Within 15%
- Predicts Pressure Profiles including exit pressures and location of flashing

3. Application of Leak Rate to Estimate Crack Size

- HT Pump Suction Line (24 in; 1.5in thick); 0.5 KG/s;
- Normal Internal Operating Pressure

4. Limited Data Available For Leakage Rates through Actual Cracks

- Future Experimental Program and Tests

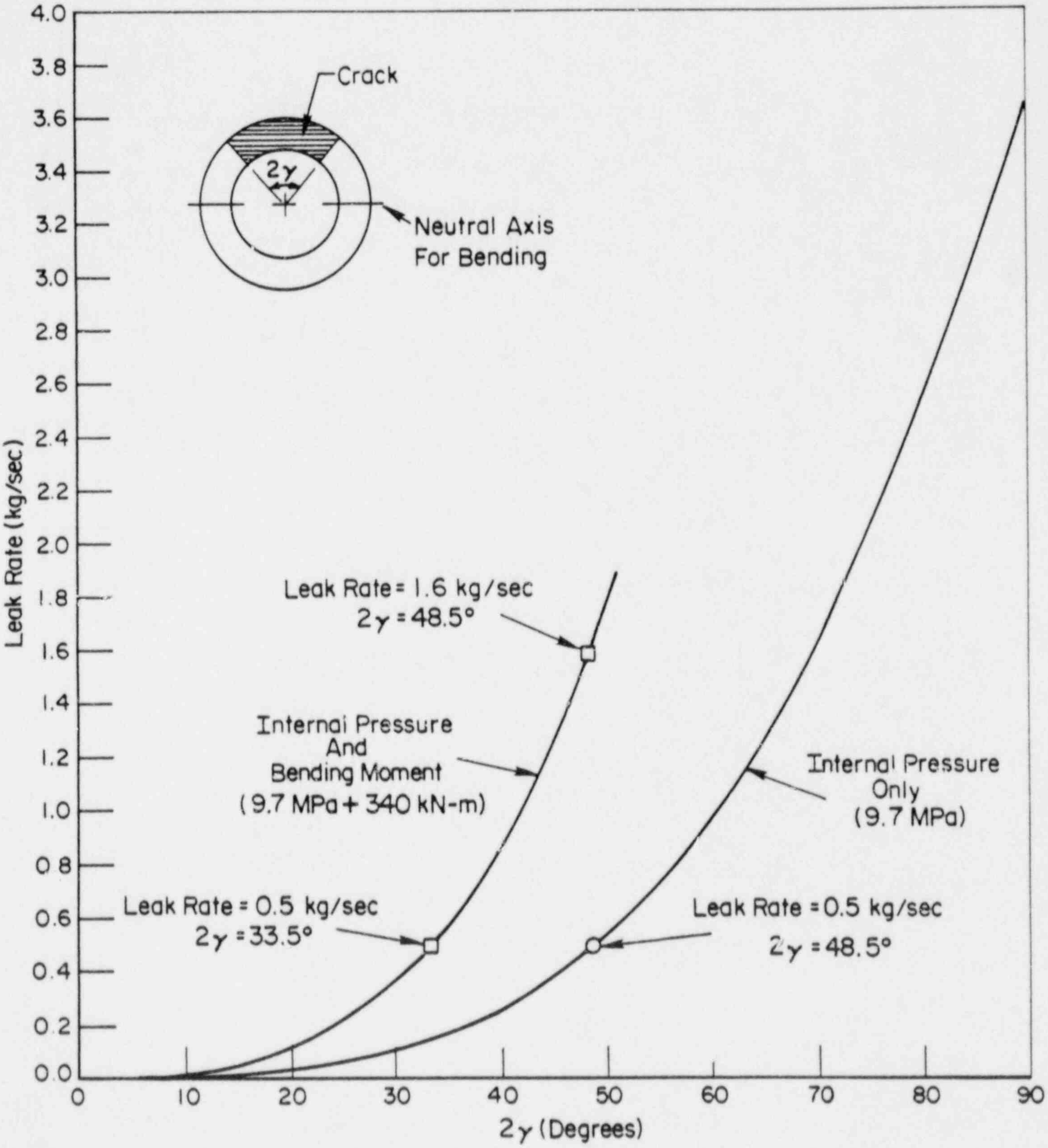

Crack
2γ
Neutral Axis
For Bending
Leak Rate (kg/sec)
4.0
3.8
3.6
3.4
3.2
3.0
2.8
2.6
2.4
2.2
2.0
1.8
1.6
1.4
1.2
1.0
0.8
0.6
0.4
0.2
0.0
Leak Rate = 1.6 kg/sec
2γ = 48.5°
Internal Pressure
And
Bending Moment
(9.7 MPa + 340 kN-m)
Internal Pressure
Only
(9.7 MPa)
Leak Rate = 0.5 kg/sec
2γ = 33.5°
Leak Rate = 0.5 kg/sec
2γ = 48.5°
0
10
20
30
40
50
60
70
80
90
2γ (Degrees)

FIGURE 3−1
SCHEMATIC OF HT SYSTEM LEAKAGE DETECTION SYSTEMS AT DARLINGTON NGS A.

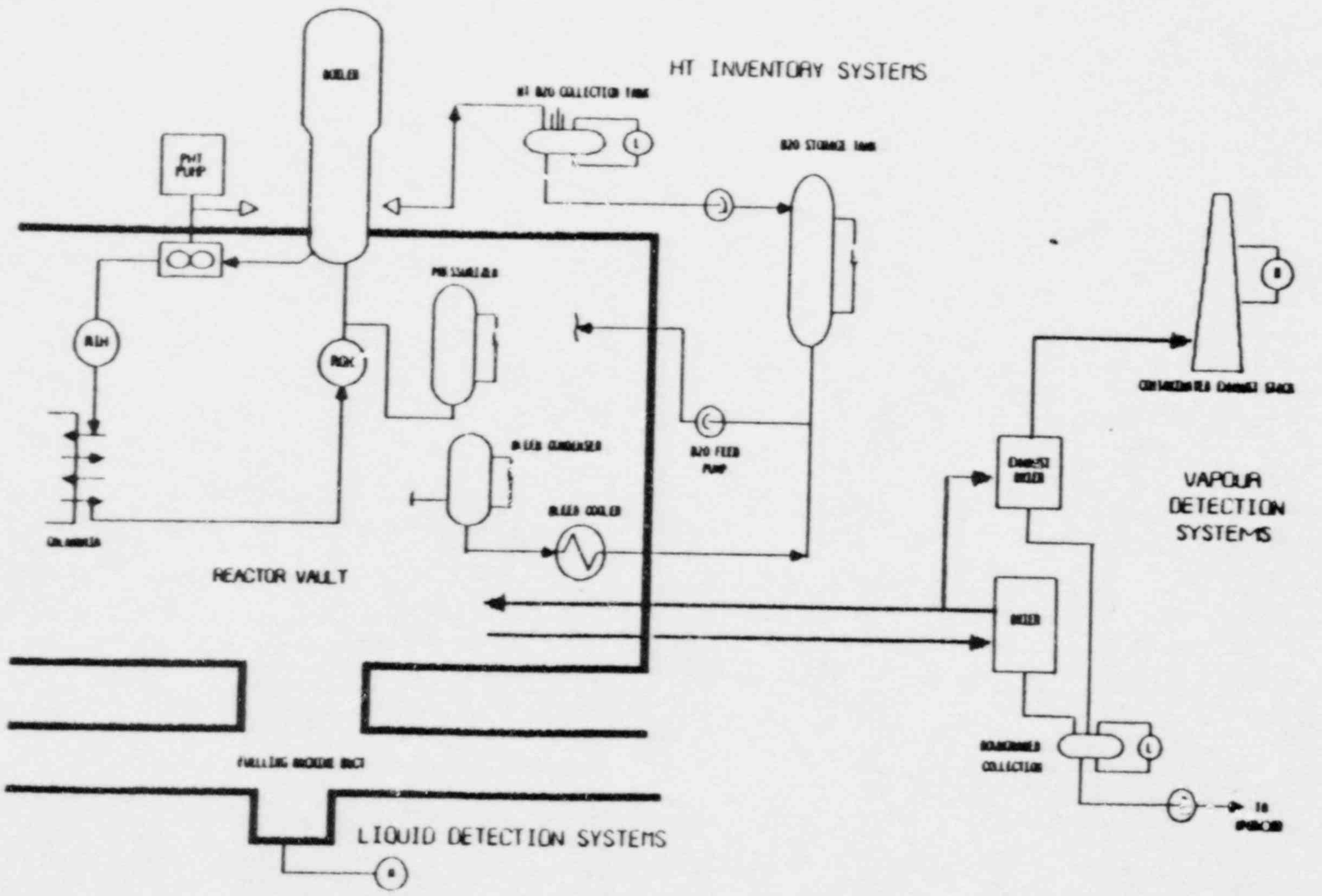


TABLE 2

Darlington NGS A HT Leak Detection Systems
Estimated Detection Times for Various HT
Leak Detection Systems and HT Leak Rates

Leak Detection System Indication/Alarm		Postulated Leak Rate Into Containment (kg/s) 0.005	0.05	0.2	0.5	7	35
HT Inventory Systems	HT D$_2$O Storage Tank - Level Trends(1) - Low Level Deviation Alarm(2)	(1) 48 to 72 hrs	(2) 6 1/2 hrs	(2) 3 hrs	(2) 1 1/2hrs	(2) 3.5 min	(2 1 min
(Detection times calculated assuming normal Type A leakage + postulated leak rate shown)							
Vapour Detection Systems (Type B leakage)	D$_2$O Vapour Recovery Drier Collection	72 hr(5)	24 hr(3)	24 hr(3)	24 hr(3)	-	-
	Powerhouse Exhaust D$_2$O in Air	72 hr(5)	24 hr(4)	24 hr(4)	24 hr(4)	-	-
Liquid Detection Systems (Type B leakage)	Beetles		Varies, depending on leak source, leak location, magnitude of leak				
	D$_2$O Recovery Trench Level(5)	-	> 24 hrs	8-19 hrs	3-8 hrs	<1 hr	-

Notes:

(1) Estimates of Type B leakage will be made daily using HT D$_2$O Storage Tank level trends. At least two monitoring periods are assumed required to detect and confirm small increases in Type B leakage. This monitoring will identify and confirm small changes in Type B leakage over a few days.

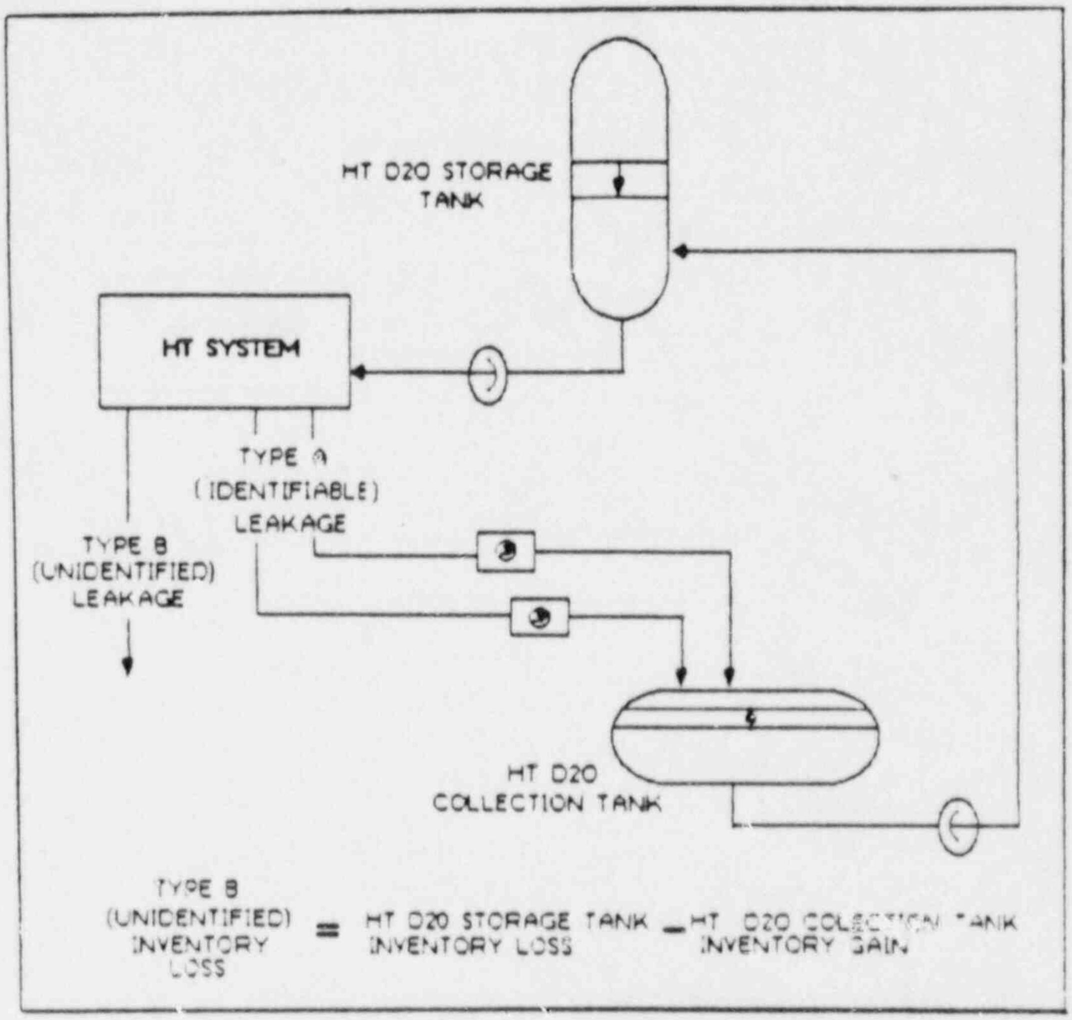


FIGURE B-1

SCHEMATIC OF INVENTORY MONITORING SYSTEMS EXTERNAL TO THE HT SYSTEM

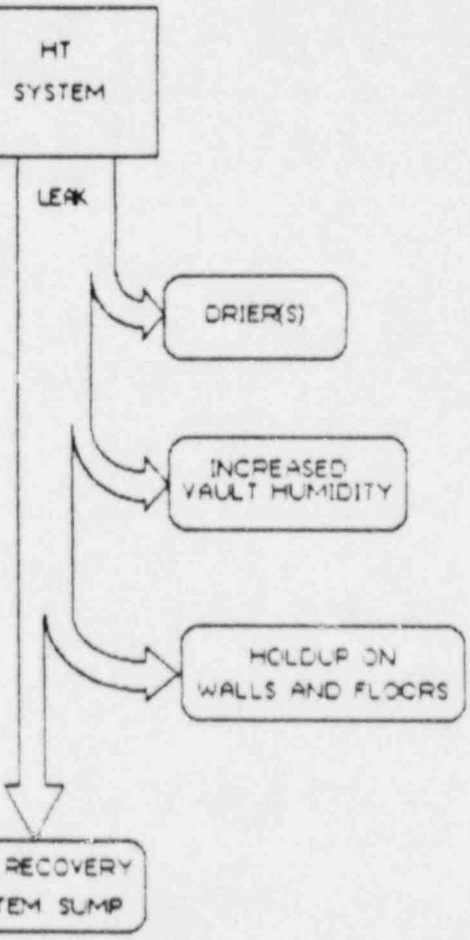


FIGURE B-2

FLOW PATHS FOR CHRONIC
OR TYPE B (UNIDENTIFIED) LEAKAGE
FROM THE HT SYSTEM INTO CONTAINMENT

ONTARIO HYDRO - LBB PIPING STUDIES

HT PIPING LEAK RATE TESTS

A. PURPOSE: TO PROVIDE RELEVANT EXPERIMENTAL DATA TO SUBSTANTIATE AND TO IMPROVE CRITICAL ASSUMPTIONS IN PRESENT LEAK RATE ANALYSIS PROCEDURES.

B. DESCRIPTION OF TESTS: VARIOUS LEAK RATES ARE TO BE MEASURED IN STRAIGHT SECTIONS OF HT PIPING CONTAINING CRACKS OF VARIOUS SIZES FOR DIFFERENT THERMAL HYDRAULIC OPERATING CONDITIONS.

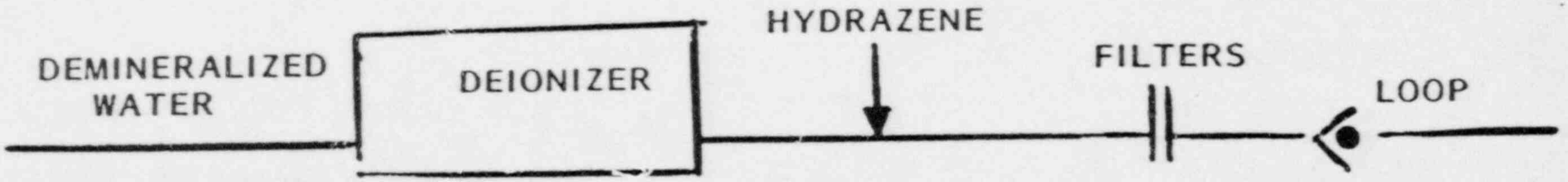
C. ADDITIONAL CAPABILITIES: STRUCTURAL MECHANICS VALIDATION TESTING PARTICULARLY IN RELATION TO FRACTURE MECHANICS PREDICTIONS FOR SPECIFIC HT PIPING COMPONENTS.

PUMP TEST COMPLEX

DESCRIPTION :

- DARLINGTON NGS PHT PUMP
- 7.2 MW
- 24 INCH SCH 100 A106 Gr B PIPE

WATER QUALITY



DEMINERALIZED WATER

DEIONIZER

HYDRAZENE

FILTERS

LOOP

STORAGE:	2 TANKS	22,700 LITERS
PIPING LOSSES:		13,300 LITERS
	TOTAL	36,000 LITERS

EXAMPLE: LEAK RATE = 0.5 kg/sec $T \approx 20$ HOURS

IN ADDITION: WATER MAKE-UP $\approx$ 600 LITERS/HOUR

ONTARIO HYDRO - BURST TEST FACILITY (BTF)

OBJECTIVE: TO PROVIDE A FACILITY CAPABLE OF PERFORMING SUCH "HIGH-ENERGY" TESTING ON PRESSURE VESSELS AND PIPING COMPONENTS AS:

i) LEAK-RATE TESTING,
ii) HYDROSTATIC BURST/PROOF TESTING, AND
iii) CYCLIC PRESSURE TESTING

BTF DIMENSIONS: INSIDE 12 m(l) x 4 m(W) x 4 m(H)

BTF FEATURES:

1) "CONTINUOUS" WATER SUPPLY (T = 320°C AND p = 15 MPa)
2) HEATING CAPABILITY (700 kW)
3) REACTION FLOOR/20,000 kN TENSILE TEST FRAME
4) FLOW MEASUREMENT (0.001 kg/sec - 10 kg/sec)
5) CONTROL ROOM/MECHANICAL ROOM
6) CYCLIC MECHANICAL LOADING/PRESSURE CAPABILITY
7) DESIGN - "IDEAL" FOR PIPING

IN-SERVICE DATE: DECEMBER 1987

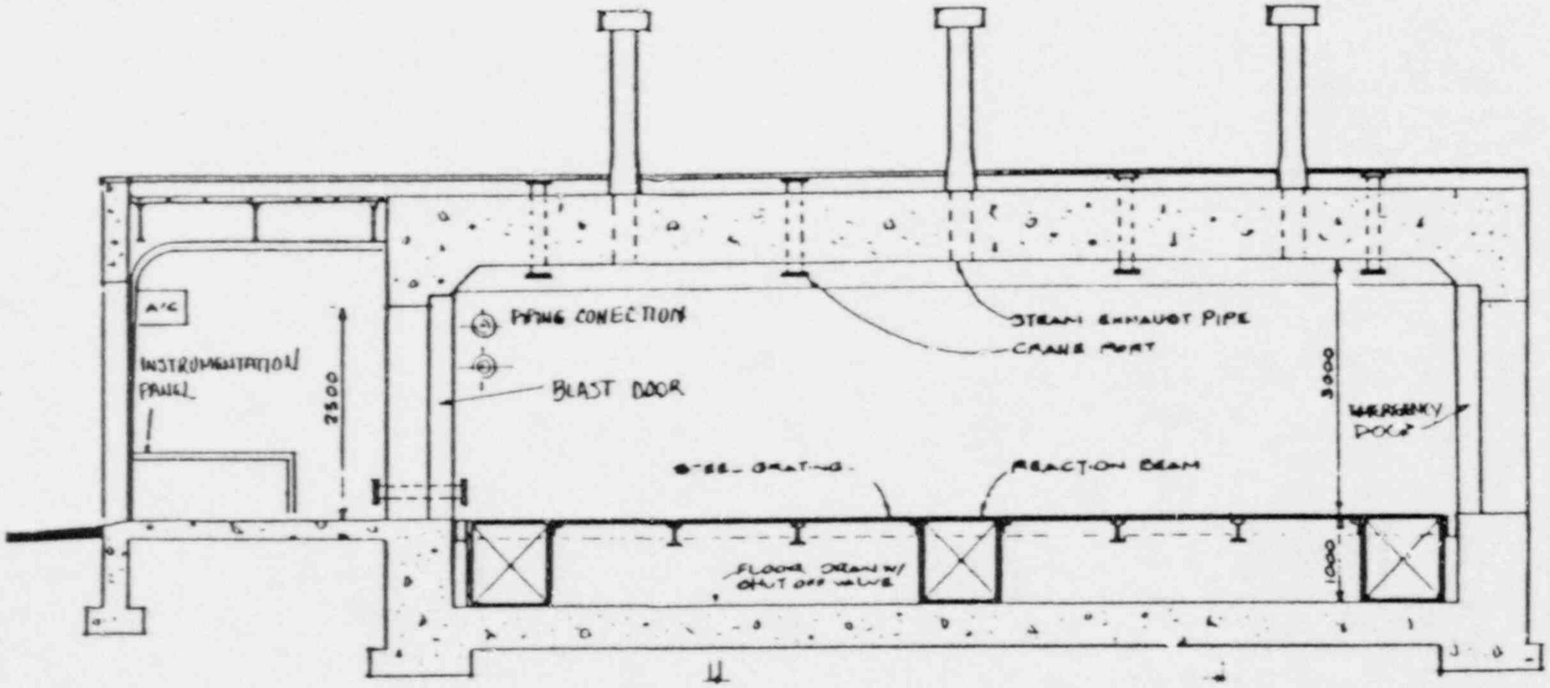


FIGURE 1

ELEVATION CROSS-SECTIONAL VIEW OF BURST TEST FACILITY

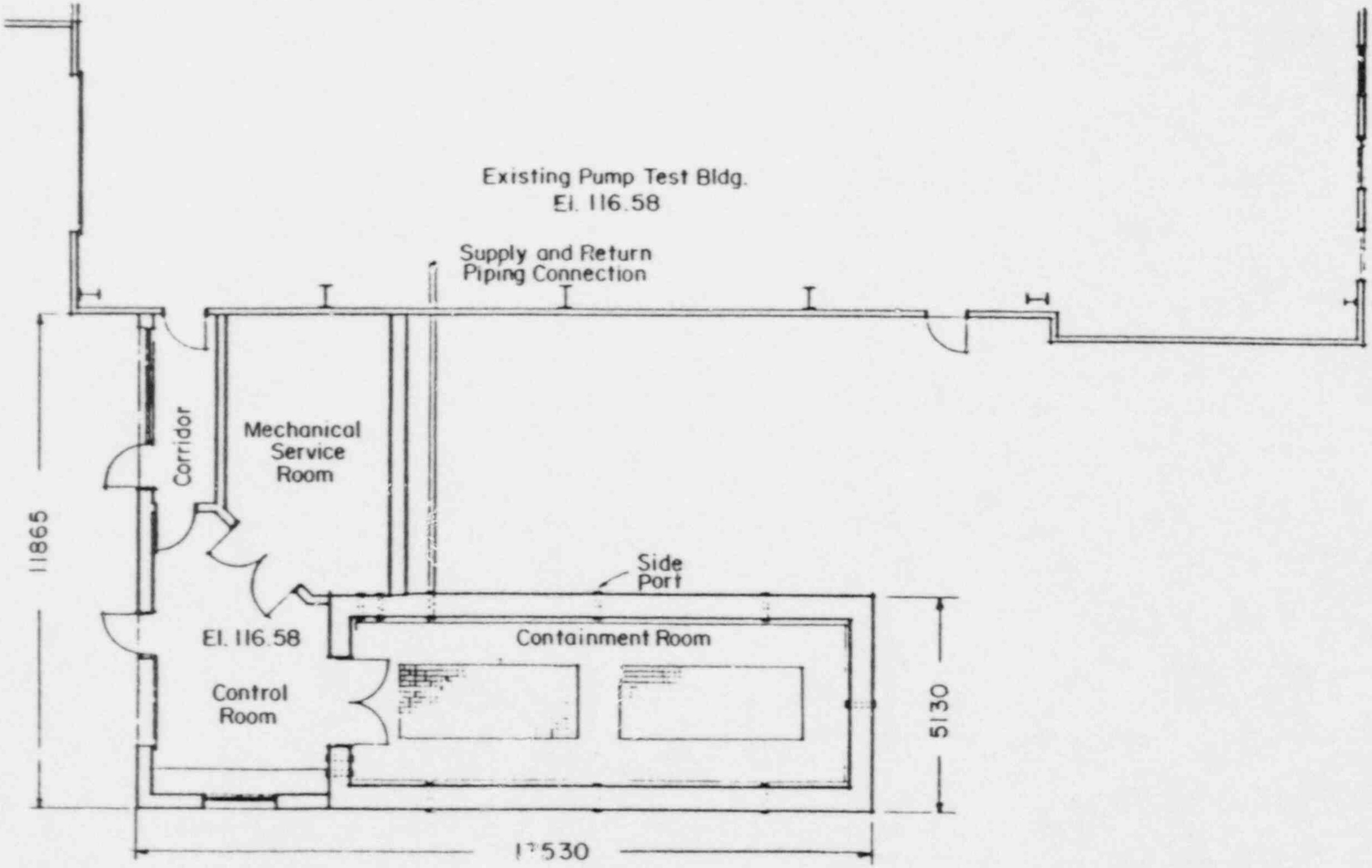


FIGURE 2

PLAN VIEW OF BURST TEST FACILITY

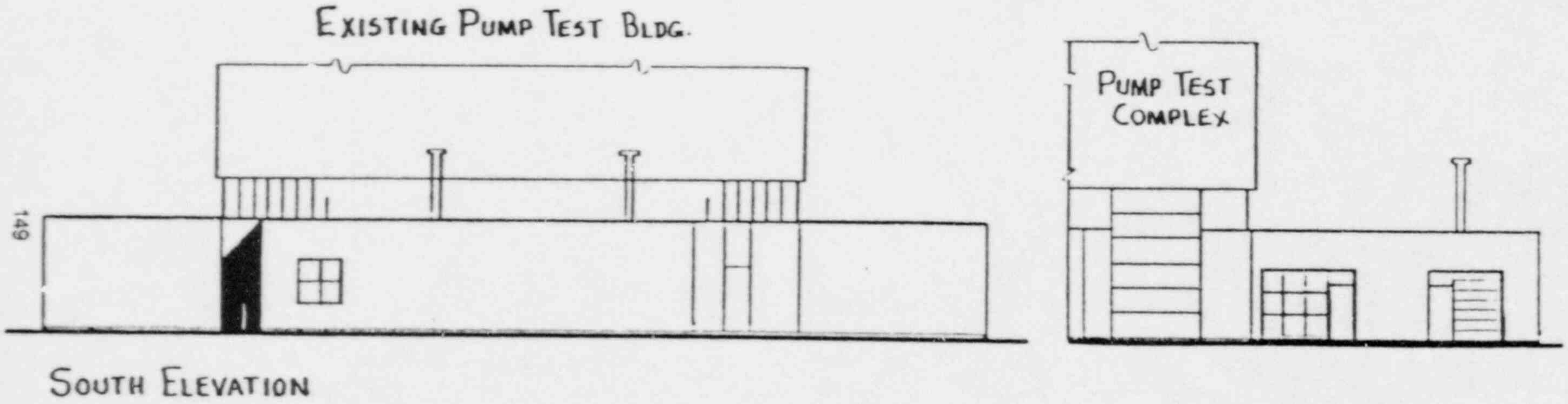


FIGURE 3
EXTERIOR ELEVATION VIEWS OF BURST TEST FACILITY

SUMMARY

1. A MAJOR BURST TEST FACILITY FOR THE "HIGH-ENERGY" TESTING OF PIPING AND PIPING COMPONENTS HAS BEEN DESIGNED AND WILL BE CONSTRUCTED BEFORE THE END OF 1987.

2. HT PIPING LEAK RATE TESTS WILL BE CONDUCTED IN THE BURST TEST FACILITY UNDER VARIOUS THERMAL/ HYDRAULIC CONDITIONS ON STRAIGHT SECTIONS OF PIPE CONTAINING DIFFERENT CRACK CONFIGURATIONS. (TESTING TO BEGIN MARCH 1988).

DARLINGTON LEAK BEFORE BREAK MATERIAL TEST PROGRAM J-RESISTANCE CURVES

B. Mukherjee, Ontario Hydro Research, Canada

1.0 INTRODUCTION

Ontario Hydro has developed a leak-before-break (LBB) methodology for application to the large diameter heat transport piping for Darlington NGS A as a design alternative to the provision of pipewhip restraints, in recognition of the questionable benefits of providing such devices. This approach has been developed for pipe sizes which are equal to or greater than 21" nominal diameter (21" NPS). However, the basic approach and underlying theories are applicable to smaller pipe sizes equally well.

Piping steels are expected to exhibit a stable crack extension behaviour with rising load up to the point of instability in the ductile uppershelf regime. Under these conditions, the application of structural integrity concepts that are based on crack initiation alone will be overly conservative. Thus the margin against failure must be determined under elastic plastic condition by taking into account both crack initiation and stable crack growth. Several methods are currently available to analyse and evaluate leak-before-break conditions in ductile piping with postulated flaws under elastic plastic conditions. These methods include but are not limited to limit load analysis (net section collapse), the J integral/tearing modulus (J/T) approach, the R-6 approach and its derivative the failure assessment diagram (FAD).

The J/T method has been selected for the Ontario Hydro leak-before-break approach/1/ because it is a general procedure that incorporates a rational crack tip parameter, can discriminate between materials of different toughness and tensile properties, and can incorporate various boundary conditions (e.g., load vs displacement control) and pipe system characteristics (e.g., system configuration and support characteristics)/2/. There are two critical steps in a leak-before-break analysis, which are initiation or first extension of a hypothetical flaw and stability of a growing flaw subsequent to initiation. The material value of J associated with initiation is J_{Ic}. If the applied value of J is less than J_{Ic} then initiation or significant extension of a crack will not occur and the stability of an existing crack is ensured automatically. When crack extension is predicted, the amount of extension must be evaluated to determine if this extension would occur in a stable manner. The material value of J associated with crack extension which is also known as material resistance curve or a J-R curve is required for stability assessment. The nondimensionalised slope of a J-R curve at a given value of J is defined as tearing modulus (T). The margin against instability is determined from a J-T plot where J is plotted against T for the material and the structure.

In this Darlington leak-before-break material test program J-R and J-T curves were determined from actual Darlington NGS A large diameter heat transport piping, forgings, associated welds and heat affected zones. Sixty-three tensile tests were performed to determine their stress-strain behaviour accurately. A total of 91 J-resistance curve tests were conducted. This paper is a summary of the J-R curve test results and major conclusions.

2.0 EXPERIMENTAL PROGRAM

A comprehensive description of the test program is given in /3/. The test program was designed to take into account the effect of various factors which influence fracture properties. The objective was to identify those factors which tend to lower resistance curves and apply the appropriate lower bound resistance curves that were obtained from this program, for structural analyses. Factors that were included in this project were:

Product Forms;
Heat of Piping Material;
Crack Plane Orientation;
Temperature;
Welding Effects.

Three different pipe sizes of SA-106 Grade B were used (12, 22 and 24 inch NPS). Two SA-105 vesselet forgings were also tested. Vesselet is a trade name for a weld-in contour fitting. The test program utilized actual piping material from actual piping heats that were used in the construction of the Darlington heat transport system (Units 1 and 2). Table I lists all heats of large diameter piping employed in Darlington NGS A Units 1 and 2.

In the stability assessment of through circumferential and longitudinal flaws, J-Resistance curves in L-C and C-L crack plane orientations, respectively, are required. Figure 1 shows the orientation of the specimens with respect to the product geometry. Specimens from SA106B and SA105 forgings were machined from both L-C and C-L orientation. Since the welds are oriented in circumferential direction, the weld samples and the heat affected zone samples can only be machined in the L-C orientation.

It has been recommended in NUREG-1061, Volume 3, "Evaluation of Potential Pipe Breaks"/2/ that J-resistance tests should be conducted at a temperature near the upper range of normal plant operation. The maximum normal operating temperature of the heat transport system occurs in the reactor outlet header and is 310°C. It is known that ductility of SA106B steel, as measured by the lateral contraction during a tensile test, shows a minimum value between 200°C and 300°C. Tensile tests were conducted in this temperature range and it was established that the lateral expansion of this material is minimum in the neighbourhood of 250°C/4/. Single specimen J_{Ic} tests were conducted at 200°C, 250°C and 300°C to confirm that the toughness of the material, when characterized by J_{Ic} and R-curve, also goes through a minimum near 250°C. Figure 2 shows the J-resistance curves from 6 specimens from 24" NPS Sch 100 Parent Material. Two specimens were tested at each temperature. Although, specimens from either C-L or L-C orientation could have been used to confirm the temperature effect, C-L orientation was selected for this program since pipes exhibit lower toughness in the C-L direction. The J_{Ic} values for these specimens are shown in Figure 3. These figures show that temperature effects on J_{Ic} and R curves are not very strong between 200°C to 300°C. Nevertheless, the lowest J_{Ic} and R curves were obtained near 250°C. Therefore, 250°C was selected as the appropriate test temperature for measuring, conservatively, the material response at 310°C. In order to determine if there is any significant dependence of toughness on temperature over the temperature range, a temperature of 20°C was selected arbitrarily, as a lower bound of the lowest service temperature for the heat transport system.

Ontario Hydro has developed various welding procedures to satisfy ASME Code requirements based on pipe size, type of welding, shop or field weld, class of pipe, wall thickness, P Number etc. Two welding procedures (PN-107 and PN-229) were developed for girth-butt welds on 12" and 22" NPS Schedule 100 pipes. Two different welding procedures (PN-108 and PN-232) were developed for 24" NPS Sch 100 pipe. Each of these welding procedures, which are listed in Table II, were included in the test program. However, only two Ontario Hydro welding procedures (PN-108 and PN-232) were within the scope of the Darlington LBB program and are addressed in this report. Results from PN-107 and PN-229 welds will be published in this report. Results from PN-107 and PN-229 welds will be published in the proceedings of the 1987 Tokyo LBB Workshop. Tests were conducted on the weld material and the heat affected zone (HAZ) material. The orientation of the crack in the weld and HAZ was in the L-C direction. Due to the V-shape of the weld preparation surface, the crack front in a HAZ specimen straddled the HAZ, parent and weld material. The measured HAZ toughness is therefore an average toughness. The entire test matrix is shown in Table III.

3.0 TEST METHOD

This section describes briefly, a single specimen compliance procedure that was used to determine the J-Resistance curves.

Three different sizes of compact tension (CT) specimens, 1-1/4T, 1T and 3/4T, conforming to the geometry of the ASTM test procedure E813-81, Test for J_{Ic}, a Measure of Fracture Toughness were used. The sizes of the specimens were determined by the largest CT specimens that could be machined from the 24", 22" and 12" NPS Schedule 100 pipes, respectively. All specimens were precracked and sidegrooved after precracking to ensure a straight crack front. The R-curves produced with a straight crack front extension exhibit slopes (dJ/da) lower than those produced with a non sidegrooved specimen. Sidegrooving also appears to produce a slightly lower (more conservative) value of crack initiation toughness.

Regarding specimen geometry, existing data indicate that fracture toughness specimens having approximately the same thickness as the pipe wall and without sidegrooves tend to model actual pipe behaviour most accurately. However, sidegrooves specimens will provide an acceptable lower bound J-resistance curve/2/. Also note that C-T specimens are bend type specimens. J-R curves from bend type specimens defines the lower bound estimate of J-capacity as a function of crack extension, and has been observed to be conservative in comparison with those obtained with tensile loading specimen configuration/5/. In this test program sidegrooved CT specimens were utilized. Average thickness reduction due to side grooving was approximately 20%. The specimen thicknesses were approximately 80% of the respective pipe wall thicknesses from which they were machined. Therefore, this test program will provide an acceptable lower bound J-resistance curve.

A single specimen technique documented in /5/ was utilized to determine the J-R curves. In this test method, crack length and crack extension were determined from elastic compliance measurements. These measurements were taken on a series of unloading/reloading segments spaced along the load versus load-line displacement record. The unload/reload segments are conducted under elastic conditions, even when the specimen has undergone extensive plastic deformation. Therefore, the change in the crack length from one unloading to the next can be inferred through a change in compliance. J values are calculated at the end of the test from the area under the load versus load-line deflection curves using equations given in /5/. The extent of the slow stable crack extension at the end of the test was marked by heat tinting. The initial crack size after precracking and the final physical crack size were measured on the fracture surface using a nine point average method as per reference /5/.

A personal computer was used for test control and data acquisition. A computer program was developed to control the loading and unloading sequence. A separate computer program was used to analyze the acquired data and plot final graphs.

A typical R-curve produced with the single specimen compliance technique is shown in Figure 4. The proposed ASTM test method indicates that the R curves should be restricted to a small crack extension given by:

$$a_{max} = 0.1 \text{ x remaining ligament (b)}$$

to maintain a region of J controlled growth at the crack tip. The requirement of J controlled growth has been formulated by Hutchinson and Paris/6/ as

$$\omega >> 1$$

where

$$\omega = (b/J)(dJ/da)$$

The limits of applicability of J-R curves are associated with the assumptions and conditions of J-integral computation. Within the limits of the restriction and the plane strain condition

$$J_{max} \leq (b \text{ X Flow Stress})/20$$

J-integral computations are rigorous.

However, R-curves associated with longer crack extension, which would violate the ω-criteria may be necessary for application to a given structural analysis. The proposed ASTM validity criteria, including that of ω are still being researched and may be altered in the future. Therefore, in this program R-curves have been developed which exceed the current ASTM crack extension and (ω) and plane strain J_{max} limitation. When the J-R curve exceeds the plane strain limitation, it simply means that the crack extends under non-plane strain condition. Since sidegrooved and approximately full thickness specimens were utilized, this data will still be applicable for piping analysis beyond the plane strain limitation on J-integral.

Where valid data cannot be generated for large crack extensions, some method of estimating the ductile fracture resistance at large crack extension is necessary. A procedure for making such an extrapolation is recommended in /2/. In this method J-resistance curve is extrapolated up to a J level twice the highest J where valid data are available using a straight line tangent to the specimen J-resistance curve at its point of maximum valid J. A modified version of J called J_M has also been formulated where calculated J values exceed the ASTM validation limits/7/. J_M values have been calculated for all tests in this program.

The calculation of J_{Ic} in Figure 4 is according to ASTM E813-81. J_{Ic} is defined by the intersection of a linear regression fit to the data, that fall within the 0.15 and 1.5 r [illegible] clusion lines, with the blunting line

$$J = 2 \sigma_y \Delta a$$

A J-R curve is established by fitting a curve through the experimental data points using a power law equation in the form

$$J = C_1 \ (\Delta a)^{C_2}$$

This equation has been used extensively to establish J-R curves for ferritic steels/8/. This equation provides an excellent fit through the experimental J- a data points for each experiment in this test program. The J-R curves shown in this paper, for comparison between groups of tests, are the fitted curves. Symbols have been used in these figures to differentiate between groups of tests. They do not represent experimental points.

4.0 RESULTS

In this section test results are grouped and summarized to show the effect of various factors which have an influence on the fracture properties of SA106B piping steel, associated welds and SA105 forgings.

4.1 Pipe Size

Initiation toughness values J_{Ic} are plotted for the 24", 22" and 12" NPS Sch 100 pipes and SA105 forging (vesselet) at 250°C in Figure 5. The results are grouped by orientations, L-C and C-L. This figure shows that the J_{Ic} values within each orientation are comparable except for one 24" NPS Sch 100 test sample which showed a high value. Average J_{Ic} values after eliminating the outliers (maximum and minimum value in each orientation) are 148.3 kN/m and 99.6 kN/m in the (L-C) and (C-L) orientation, respectively.

J-resistance curves at 250°C and (L-C) orientation are plotted in Figure 6 for three pipe sizes. A 24" NPS Sch 100 specimen (J3) which exhibited the highest J_{Ic} in Figure 5 also gives the highest R curve in Figure 6. The R-curves for the remaining specimens from all three product forms lie within a narrow scatterband. The lower bound of the scatterband is delineated by a 12" NPS Sch 100 test result.

The J-T curves corresponding to the J-R curves of Figure 6 are plotted in Figure 7. These curves start at J values approximately equal to J_{Ic} and extend to the maximum measured J values. Unlike the J-R curves, the J-T curves show a layering effect. All 22" and 24" NPS Sch 100 specimens, except J3, in a narrow band. The 12" NPS Sch 100 specimen results lie below this band. Closely clustered J-R curves will show up as layers in the J-T domain since the slope change of J-R curves between J_{Ic} and J_{max} is much larger than the actual change of J values.

J-resistance curves at 250°C and (C-L) orientation are plotted in Figure 8 for three pipe sizes. In this orientation the R-curves from 12" NPS Sch 100 specimens are higher than those obtained from 22" and 24" NPS Sch 100 specimens.

4.2 Crack Plane Orientation

In order to determine stability assessment of longitudinal (C-L) and circumferential (L-C) cracks, J-R and J-T curves are required for both orientations. Specimens were machined from both orientations as shown in Figure 1. The effect of orientation on J-R curves is shown in Figure 9 for 22" NPS Sch 100 pipes. Crack initiation toughness, J_{Ic}, values for all product forms are shown in Figure 5. All product forms have higher J_{Ic} and higher J-resistance curves in (L-C) direction compared to those of (C-L) orientation. At 250°C, average J_{Ic} value for all product forms is 148.3 kN/m in (L-C) orientation and 99.6 kN/m in (C-L) orientation. The variation of toughness with orientation is related to the principal direction of mechanical working or grain flow in pipes and forgings. Similar material behaviour in pipes has been observed by other investigators through conventional Charpy tests/9/.

4.3 Effect of Temperature

In order to determine the effect of temperature on the J-resistance curves of SA106B piping, SA105 forging and associated welds, tests were conducted at 250°C and 20°C. In principle J-resistance curves are needed for the operating temperature of 310°C. However, tests were conducted at 250°C as these provide a slightly conservative J-resistance curve, see Figures 2 and 3. A lower bound of the service temperature was selected arbitrarily as 20°C. Toughness behaviour of high toughness - low strength C-Mn steels such as A106B shows a transition with temperature. An examination of the load displacement curves indicated that at 20°C all SA106B pipes, SA105 forging and welds exhibited ductile, high toughness upper shelf behaviour.

The effect of test temperature on crack initiation toughness, J_{Ic}, values for a 22" NPS Sch 100 pipe and a vesselet is shown in Figure 10. Temperature effects are shown for L-C and C-L orientations. The average J_{Ic} values at 250°C were lower than those at 20°C but the degree of reduction is dependent on product form and specimen orientation. For 22" NPS Sch 100 pipe the average J_{Ic} value in the L-C orientation was reduced by 39%, but the reduction in the C-L orientation was only 22%. The average J_{Ic} values for forged vesselet material were not influenced by temperature to the same degree. Its average J_{Ic} value was reduced by only 6% in the L-C orientation and by 15% the C-L orientation.

The J-resistance curves at two test temperatures for a 22" NPS Sch 100 parent material are shown in Figures 11 and 12. These figures show that the J-resistance curves for parent materials at 250°C are lower than those at 20°C. A comparison of Figures 11 and 12 will show that the lowering of the J-resistance curves in the C-L orientation with temperature is not as large as that observed for L-C orientation.

The effect of test temperature on J_{Ic} and J-resistance curves of weld materials and heat affected zones is similar to that observed for parent material. Specifically, J_{Ic} values and J-resistance curves for PN 232 weld at 250°, are lower than those at 20°C. J_{Ic} values and J-resistance curves of PN 232 weld are shown in Figures 10 and 13, respectively. All weld specimens were machined from C-L orientation.

The observation that J_{Ic} and J-R curves are lowered with temperature in the upper shelf temperature range, is characteristics of low-strength high toughness structural steels/10,11/. Reduction of material tensile stress with temperature will promote this behaviour. Other factors such as strain aging will also reduce J_{Ic} and J-resistance curves.

4.4 Welding Effects

Two Ontario Hydro welding procedures (PN 108 and PN 232) were used for the 24" pipe. J_{Ic} values for the weld and HAZ material for these welds procedures are shown in Figure 14. Average parent metal J_{Ic} (average of 3 tests) are also shown in these figures. J-resistance curves for these welds and HAZ are shown in Figures 15 and 16.

4.5 Validation of Test Results

Considerable care was exercised in checking transducer calibration and computer programs that were developed for data analysis of single specimen J_{Ic} tests. Since the material test program is pivotal to the Darlington LBB program, it was decided to validate the material test program by comparing three test results against tests conducted by an independent laboratory. Materials Engineering Associate of Lanham, Maryland was selected for conducting the validating tests because of their experience in conducting single specimen J-resistance curve tests for U.S. Nuclear Regulatory Commission/12/ and Electric Power Research Institute/8/.

A section of a 22" NPS Sch 100 parent material was shipped to MEA. Three 2.5 cm thick compact tension specimens were machined by MEA and were tested at 250°C. All specimens were machined in the L-C orientation. Yield and UTS values were provided to MEA by Ontario Hydro.

J-resistance curves are compared in Figure 17. With the exception of one MEA test, which produced a high J_{Ic}, all OH and MEA J_{Ic} values of 22" NPS Sch 100 parent material were comparable. The MEA J-resistance curves for the parent material (Figure 17) were slightly higher than the OH J-resistance curves. A comparison of Figure 17 with all parent material tests in Figure 6 suggests that both OH and MEA data lie within the observed scatter band of all parent material tests.

In summary, the J_{Ic} and J-resistance curves obtained by OH and MEA on parent materials were comparable. There were no systematic variation or discrepancy between the tests conducted by two independent laboratories. This comparison provides an independent but indirect validation of transducer calibration, test method, crack length and J calculation procedures.

5.0 DISCUSSION OF RESULTS

The objective of this test program was to develop a comprehensive material data base for all product forms, heats of steels and weld procedures that have been used in the construction of large diameter Darlington heat transport circuit. At the end of the test program, it can be stated with confidence that this objective has been achieved.

Specimens from four product forms were tested. These were 24", 22" and 12" NPS Sch 100 pipes and SA105 forgings. Toughness values and J-resistance curves of these product forms depend on temperature and orientation. However, at a fixed temperature and at a fixed orientation toughness value (Figure 5) and J-resistance curves (Figures 6, 8) lie within a reasonable scatterband. Observed scatter in the data is due to usual experimental and material variability.

A general observation from all parent material results is that all precracked parent material specimens showed stable and ductile crack extension with rising load. At both test temperatures and specimen orientations all parent material specimens showed high J_{Ic} and rising resistance curves with crack extension.

Toughness values of pipes depend on the direction of crack extension relative to the pipe geometry. Tests using conventional Charpy specimens have shown that toughness of SA106B steel, as indicated by energy absorption, depends on specimen orientation/9/. A literature search was conducted to identify papers which had investigated the effect of specimen orientation on J_{Ic} and J-resistance curves but none was found in this particular search. Stability assessment of longitudinal and circumferential cracks requires that J_{Ic} and J-resistance curves are known for both orientations. Therefore, tests were conducted on all product forms to quantify the effect of orientation on toughness.

At 250°C, J_{Ic} and J-resistance curves in the longitudinal direction were lower than those in the axial direction for all product forms. Changes in J_{Ic} values and J-resistance curves were comparable for the 24" and 22" NPS Sch 100 pipes and SA105 forgings. The 12" NPS Sch 100 pipe specimens showed the least reduction of toughness with orientation.

Toughness behaviour of high toughness - low strength C-Mn steels such as SA106B and SA105 forgings show a transition with temperature. An examination of load displacement curves and Charpy energy values indicated that these steels show an upper shelf toughness behaviour at 20°C. Specifically, load versus displacement behaviour of all specimens were highly ductile. All specimens exhibited high crack initiation toughness, stable crack extension and steeply rising resistance curves beyond J_{Ic}.

J_{Ic} and J-resistance curves at 250°C were lower than those at 20°C, which is characteristic of lower strength, high toughness structural steels. Reduction of material tensile stress with temperature will promote this behaviour.

Ontario Hydro has developed various welding procedures for the construction of Darlington PHT system. These procedures satisfy ASME Code requirements. The PN 108 and PN 232 weld procedures were used for all welds within the scope of the Darlington LBB project. Post weld heat treatment is required for these weld procedures. All tests on PN 108 and PN 232 weld and HAZ material at both test temperatures showed high J_{Ic}, stable crack extension and rising resistance curves, that is, general uppershelf toughness behaviour.

J_{Ic} values (Figure 14) and J-resistance curves (Figures 15, 16) for the PN 108 and PN 232 welds were higher than the corresponding parent material properties. Due to the geometry of weld preparation and the given thickness of pipes, the crack front in a HAZ specimen straddled the weld, HAZ and parent material. The measured HAZ J-resistance curves, therefore, lies between the weld and parent material J-resistance curves. Effect of test temperature on the toughness of PN 232 weld was similar to that observed in parent material tests in that J_{Ic} and J-resistance curves at 250°C were lower than those at 20°C. An uppershelf toughness behaviour was observed at both test temperatures.

6.0 CONCLUSIONS

1. Toughness (J_{Ic} and J-Resistance curve) of parent materials depends on specimen orientations. Toughness of a pipe in the circumferential orientation was higher than the corresponding toughness in the axial direction. The effect of orientation on toughness was quantified.

2. For a given temperature and specimen orientation, toughness also depends on various product forms, though to a lesser extent.

3. Toughness (J_{Ic} and J-Resistance curve) of parent and weld materials at 250° were lower than those at 20°.

4. The material test program was validated by comparing three test results against tests conducted by Materials Engineering Associates of Lanham, Maryland. The J_{Ic} and J-resistance curves obtained by Ontario Hydro and MEA were comparable.

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Their help with this project is greatly appreciated.

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TABLE I

Large Diameter Piping Heats Darlington NGS A Heat Transport Piping Units 1 and 2

Pipe Size (NPS)	Heat Numbers
21	J1136
	J1157
	J9980
22	J9968
	J9969
	L7204
24	J1136
	J1157
	J9980
	J9932
	L7204
	L7205

TABLE II

Ontario Hydro Nuclear Weld Procedures

NPS (Inches)	Ontario Hydro Weld Procedure	Weld Process
12 22	PN 107	- GTAW root pass - SMAW fill up - field and shop welding - no PWHT
	PN 229	- GTAW root pass - SMAW 2nd pass - SAW fill up - for shop welding only - no PWHT
24	PN 108	- GTAW root pass - SMAW fill up - for field and shop welding - PHWT performed
	PN 232	- GTAW root pass - SMAW 2nd pass - SAW fill up - for shop welding only - PWHT performed

Notes: (1) PN 108 weld procedure applied to 22" NPS pipe also for comparison of effect of PHWT.

TABLE III DARLINGTON LBB J-RESISTANCE CURVES

DARLINGTON LBB A106B MATERIAL DATA SUMMARY TABLE

	TEST NO	MATERIAL	DIRECTION OF TEST	COUPON NO	DESCRIPTION	TEST TEMP C
J	1	PARENT	L-C	M1	24" NPS Sch 100	250
J	2	PARENT	L-C	M1	24" NPS Sch 100	250
J	3	PARENT	L-C	S1	24" NPS Sch 100	250
J	4	WELD PN-108	L-C	A1	24" NPS Sch 100	250
J	5	WELD PN-108	L-C	A1	24" NPS Sch 100	250
J	6	WELD PN-108	L-C	A3	24" NPS Sch 100	250
J	7	HAZ PN-108	L-C	A2	24" NPS Sch 100	250
J	8	HAZ PN-108	L-C	A2	24" NPS Sch 100	250
J	9	WELD PN-232	L-C	B1	24" NPS Sch 100	250
J	10	WELD PN-232	L-C	B1	24" NPS Sch 100	250
J	11	WELD PN-232	L-C	B3	24" NPS Sch 100	250
J	12	HAZ PN-232	L-C	B2	24" NPS Sch 100	250
J	13	HAZ PN-232	L-C	B2	24" NPS Sch 100	250
J	14	PARENT	L-C	N1	22" NPS Sch 100	20
J	15	PARENT	L-C	N1	22" NPS Sch 100	20
J	16	PARENT	L-C	N2	22" NPS Sch 100	20
J	17	PARENT	L-C	N1	22" NPS Sch 100	250
J	18	PARENT	L-C	N2	22" NPS Sch 100	250
J	19	PARENT	L-C	N2	22" NPS Sch 100	250
J	20	WELD PN-107	L-C	C1	22" NPS Sch 100	20
J	21	WELD PN-107	L-C	C1	22" NPS Sch 100	20
J	22	WELD PN-107	L-C	C2	22" NPS Sch 100	20
J	23	WELD PN-107	L-C	C4	22" NPS Sch 100	250
J	24	WELD PN-107	L-C	C2	22" NPS Sch 100	250
J	25	WELD PN-107	L-C	C4	22" NPS Sch 100	250
J	26	HAZ PN-107	L-C	C3	22" NPS Sch 100	20
J	27	HAZ PN-107	L-C	C3	22" NPS Sch 100	20
J	28	HAZ PN-107	L-C	C6	22" NPS Sch 100	250
J	29	HAZ PN-107	L-C	C6	22" NPS Sch 100	250
J	30	WELD PN-229	L-C	D1	22" NPS Sch 100	20
J	31	WELD PN-229	L-C	D1	22" NPS Sch 100	20
J	32	WELD PN-229	L-C	D2	22" NPS Sch 100	20
J	33	WELD PN-229	L-C	D4	22" NPS Sch 100	250
J	34	WELD PN-229	L-C	D2	22" NPS Sch 100	250
J	35	WELD PN-229	L-C	D4	22" NPS Sch 100	250
J	36	HAZ PN-229	L-C	D3	22" NPS Sch 100	20
J	37	HAZ PN-229	L-C	D3	22" NPS Sch 100	20
J	38	HAZ PN-229	L-C	D6	22" NPS Sch 100	250
J	39	HAZ PN-229	L-C	D6	22" NPS Sch 100	250
J	40	WELD PN-108	L-C	E1	22" NPS Sch 100	250
J	41	WELD PN-108	L-C	E1	22" NPS Sch 100	250
J	42	WELD PN-108	L-C	E2	22" NPS Sch 100	250
J	43	HAZ PN-108	L-C	E2	22" NPS Sch 100	250
J	44	HAZ PN-108	L-C	E2	22" NPS Sch 100	250
J	45	PARENT	L-C	P1	12" NPS Sch100	250
J	46	PARENT	L-C	P1	12" NPS Sch100	250

TABLE III DARLINGTON LBB J-RESISTANCE CURVES

DARLINGTON LBB A106B MATERIAL DATA SUMMARY TABLE

TEST NO	MATERIAL	DIRECTION OF TEST	COUPON NO	DESCRIPTION	TEST TEMP C
J 47	PARENT	L-C	P2	12" NPS Sch100	250
J 48	WELD PN-107	L-C	F1	12" NPS Sch100	250
J 49	WELD PN-107	L-C	F1	12" NPS Sch100	250
J 50	WELD PN-107	L-C	F2	12" NPS Sch100	250
J 51	HAZ PN-107	L-C	F3	12" NPS Sch100	250
J 52	HAZ PN-107	L-C	F3	12" NPS Sch100	250
J 53	PARENT	L-C	R1	22"*12" VESSELET	20
J 54	PARENT	L-C	R1	22"*12" VESSELET	20
J 55	PARENT	L-C	R2	22"*12" VESSELET	20
J 56	PARENT	L-C	R1	22"*12" VESSELET	250
J 57	PARENT	L-C	R2	22"*12" VESSELET	250
J 58	PARENT	L-C	R2	22"*12" VESSELET	250
J 59	PARENT	C-L	M2	24" NPS Sch 100	250
J 60	PARENT	C-L	M2	24" NPS Sch 100	250
J 61	PARENT	C-L	S1	24" NPS Sch 100	250
J 62	PARENT	C-L	N3	22" NPS Sch 100	20
J 63	PARENT	C-L	N3	22" NPS Sch 100	20
J 64	PARENT	C-L	N4	22" NPS Sch 100	20
J 65	PARENT	C-L	N3	22" NPS Sch 100	250
J 66	PARENT	C-L	N4	22" NPS Sch 100	250
J 67	PARENT	C-L	N4	22" NPS Sch 100	250
J 68	PARENT	C-L	P2	12" NPS Sch100	250
J 69	PARENT	C-L	P3	12" NPS Sch100	250
J 70	PARENT	C-L	P3	12" NPS Sch100	250
J 71	PARENT	C-L	R3	22"*12" VESSELET	20
J 72	PARENT	C-L	R3	22"*12" VESSELET	20
J 73	PARENT	C-L	R4	22"*12" VESSELET	20
J 74	PARENT	C-L	R3	22"*12" VESSELET	20
J 75	PARENT	C-L	R4	22"*12" VESSELET	20
J 76	PARENT	C-L	R4	22"*12" VESSELET	20
J 77	PARENT	C-L	R5	22"*12" VESSELET	250
J 78	PARENT	C-L	R5	22"*12" VESSELET	250
J 79	PARENT	C-L	F6	22"*12" VESSELET	250
J 80	WELD PN-232	L-C	B3	24" NPS Sch 100	20
J 81	WELD PN-232	L-C	B8	24" NPS Sch 100	20
J 82	WELD PN-232	L-C	B8	24" NPS Sch 100	20
J 83	W PN229 SR	L-C	D5	22" NPS Sch 100	20
J 84	W PN229 SR	L-C	D5	22" NPS Sch 100	250
J 85	W PN229 SR	L-C	D8	22" NPS Sch 100	250
TD 13	PARENT	C-L	M5	24" NPS Sch 100	200
TD 14	PARENT	C-L	M5	24" NPS Sch 100	200
TD 15	PARENT	C-L	M5	24" NPS Sch 100	250
TD 16	PARENT	C-L	M2	24" NPS Sch 100	300
TD 17	PARENT	C-L	M6	24" NPS Sch 100	300
TD 18	PARENT	C-L	M6	24" NPS Sch 100	250

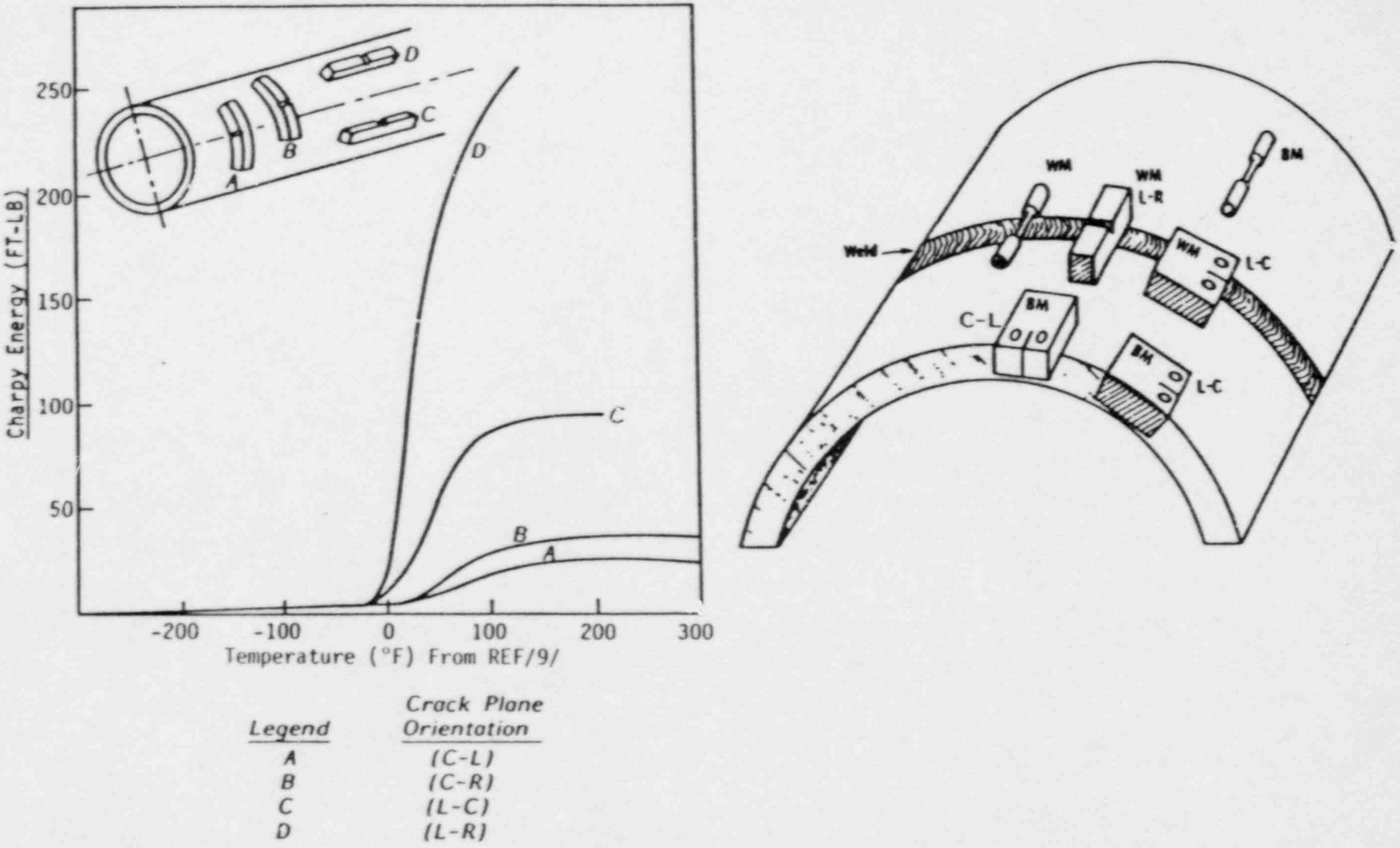


FIGURE 1

SPECIMEN ORIENTATION WITH RESPECT TO A PIPE GEOMETRY

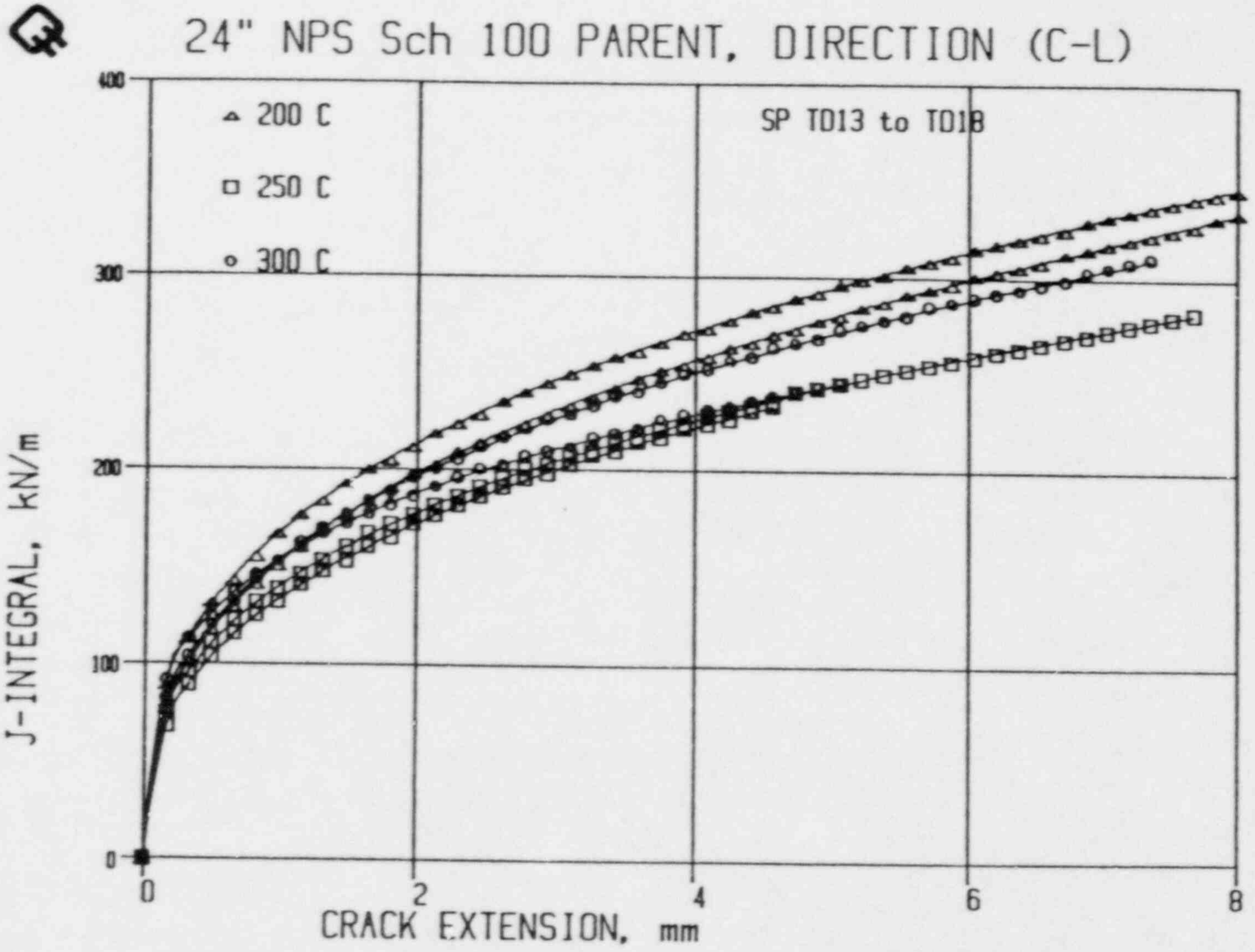


FIGURE 2

COMPARISON OF J-RESISTANCE CURVES AT 3 TEMPERATURES

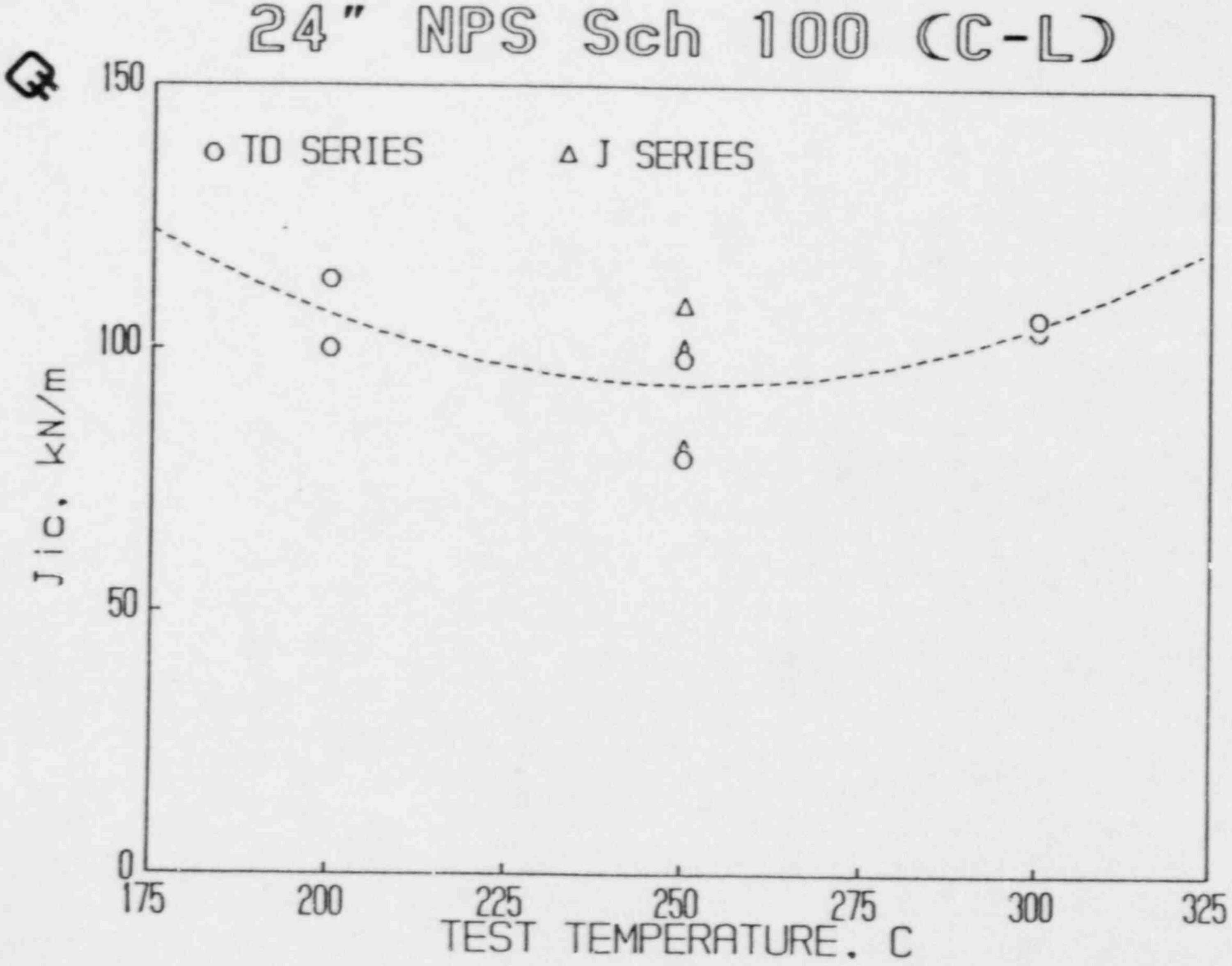


FIGURE 3

VARIATION OF J_{IC} BETWEEN 200°C AND 300°C

File JC 13-18 ENG

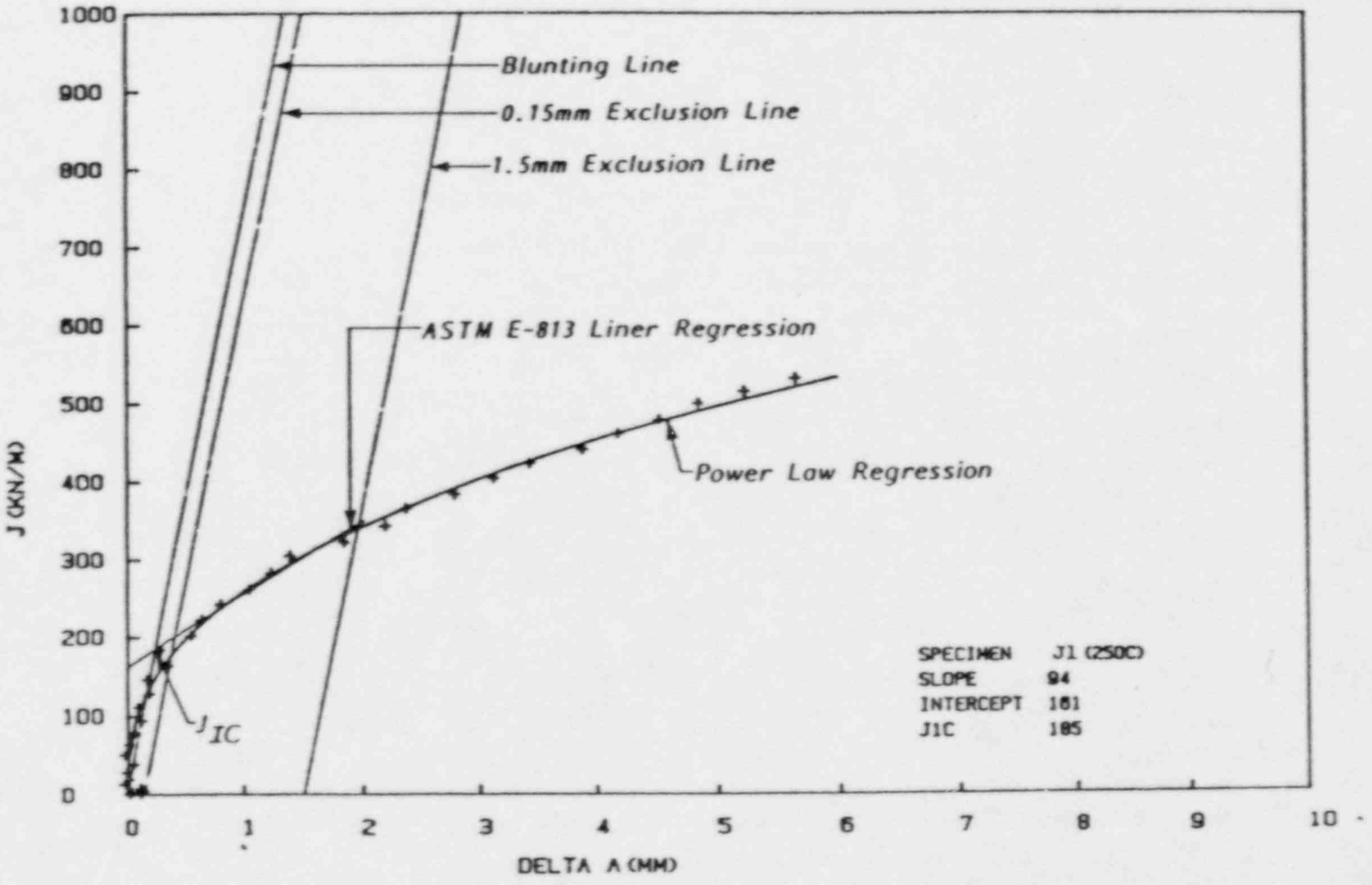


FIGURE 4
J-R CURVE

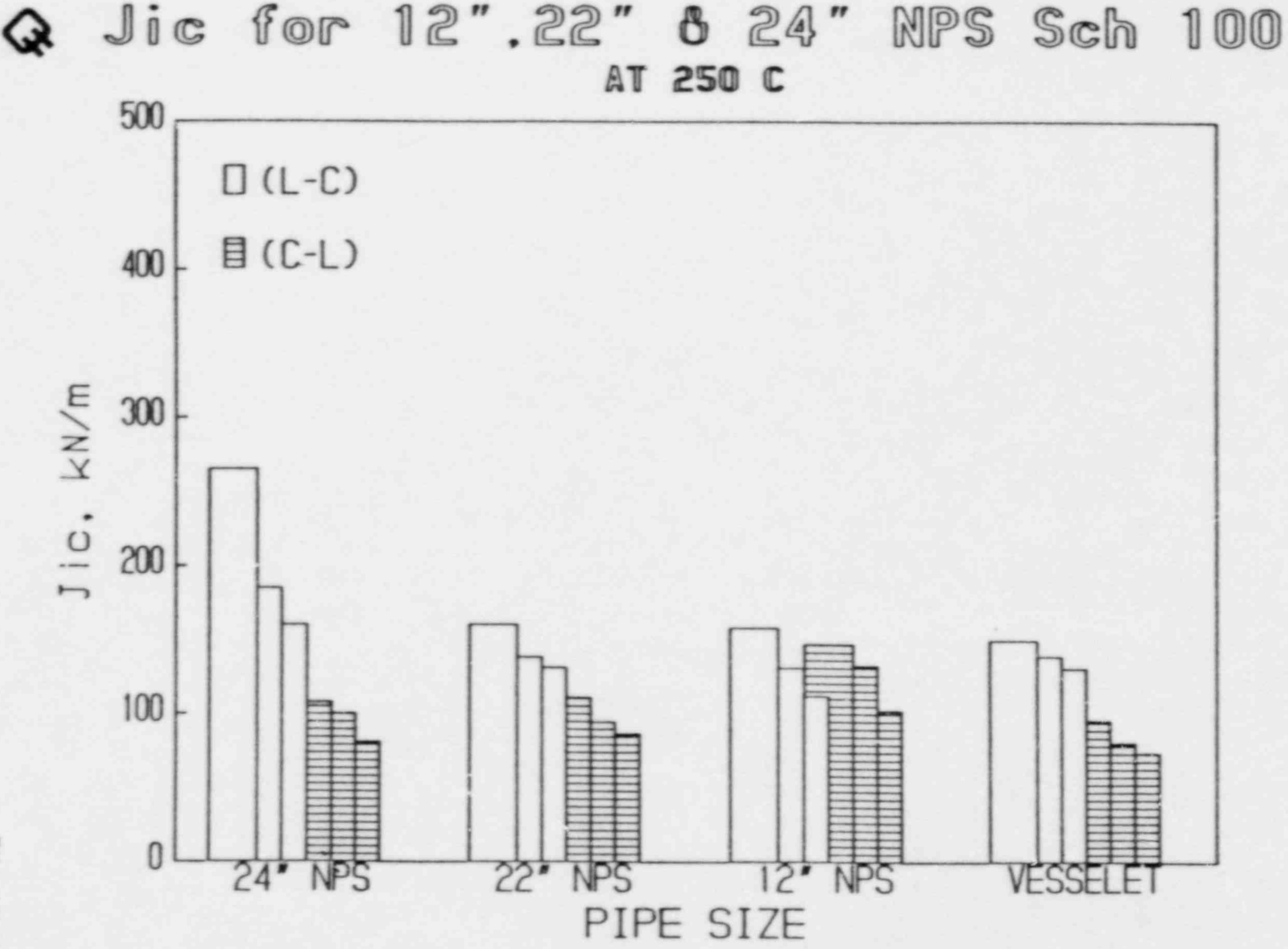


FIGURE 5

COMPARISON OF J_{IC} FOR VARIOUS PRODUCT FORMS

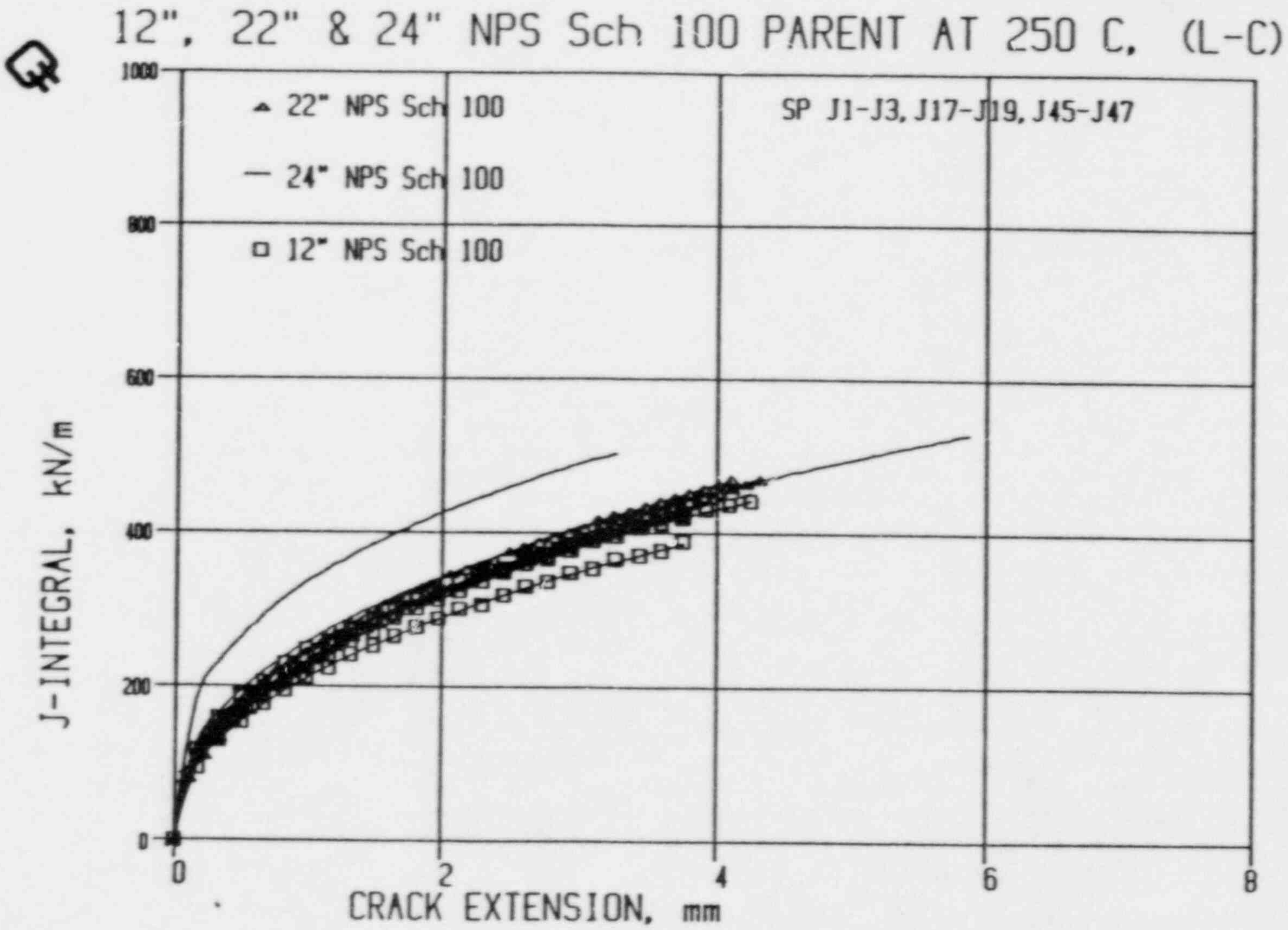


FIGURE 6

J-RESISTANCE CURVES FOR THREE PIPE SIZES

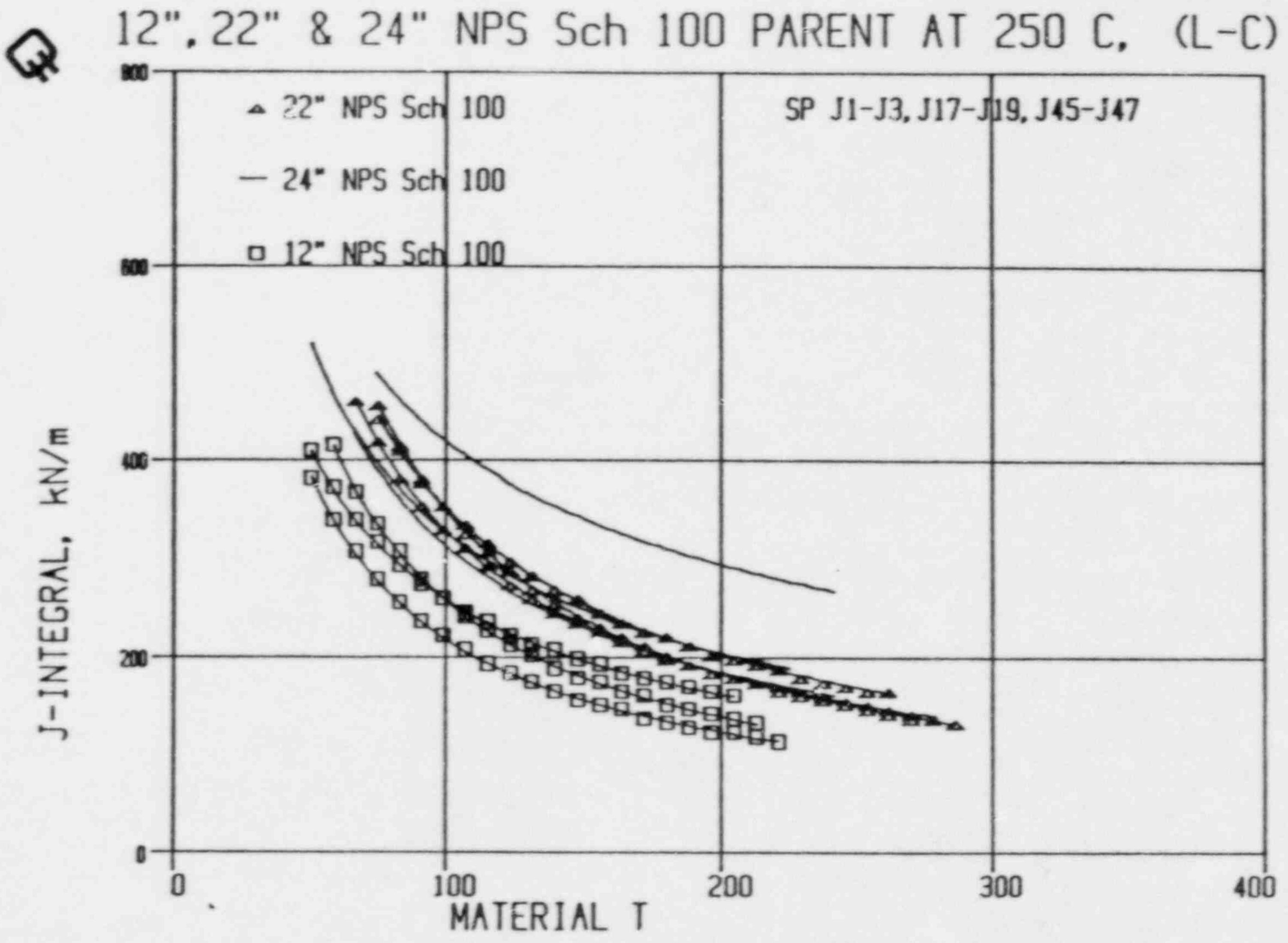


FIGURE 7

J-T CURVES FOR THREE PIPE SIZES

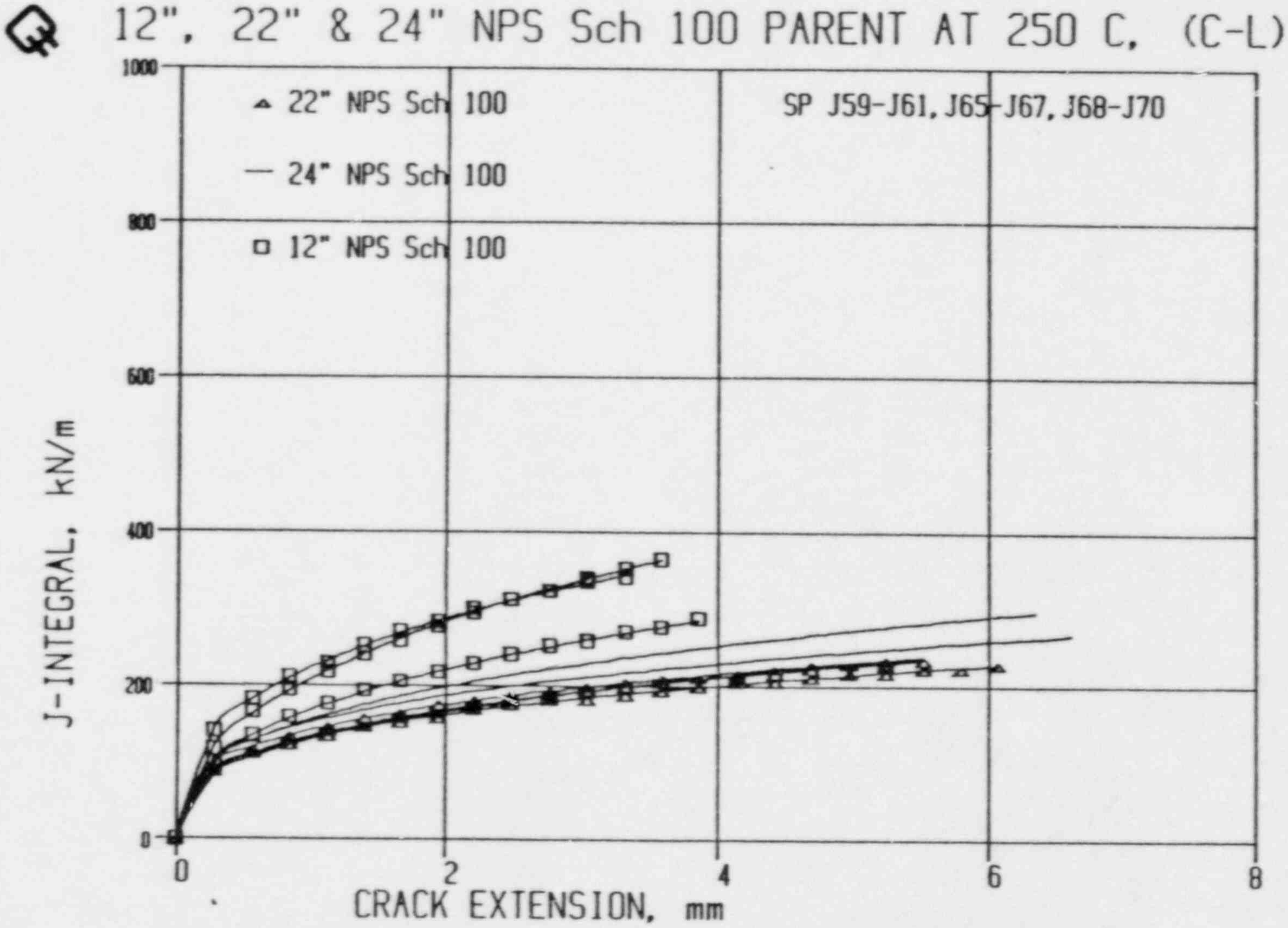


FIGURE 8

J-RESISTANCE CURVES FOR THREE PIPE SIZES

File J 59-70

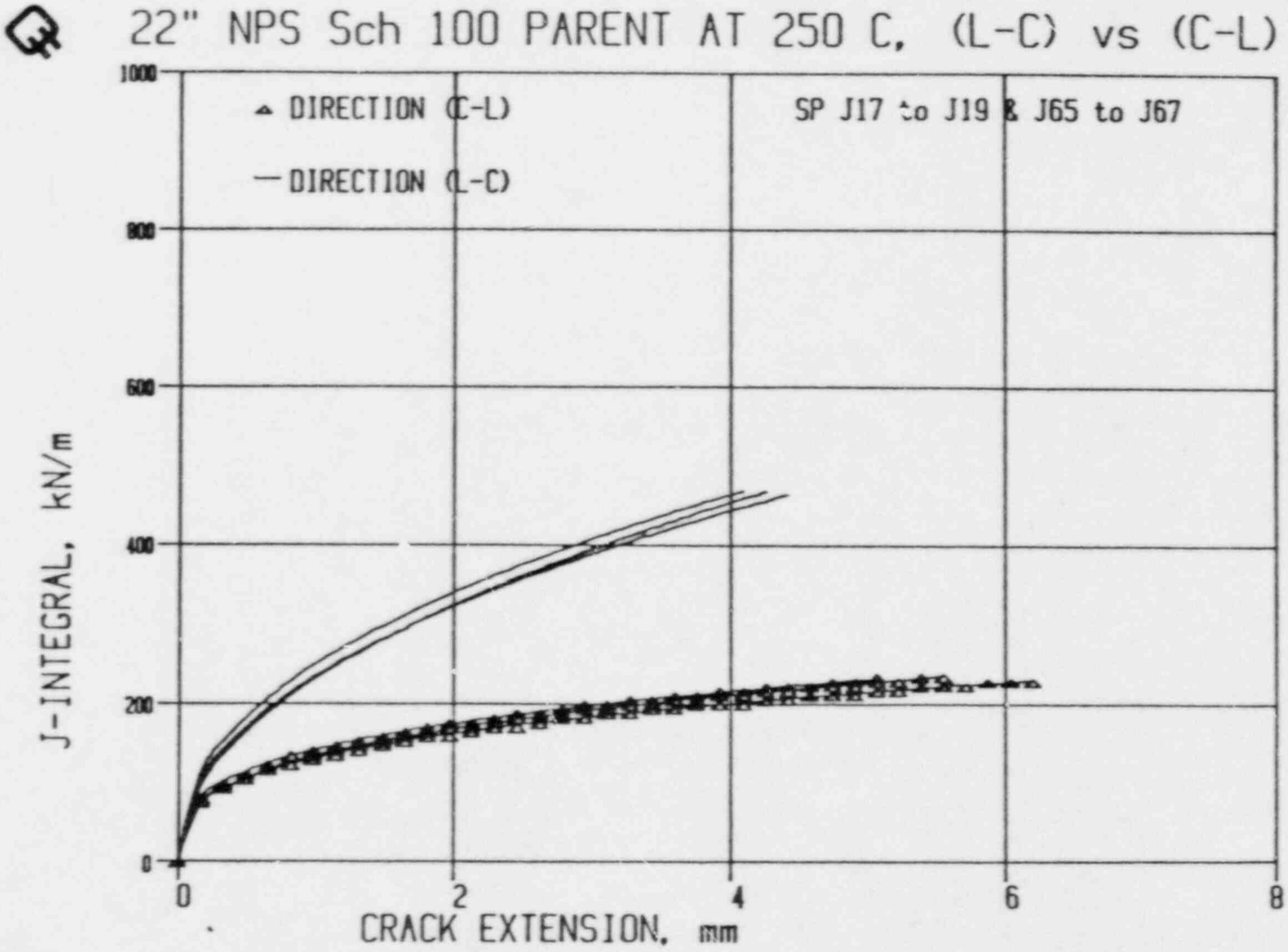


FIGURE 9

EFFECT OF ORIENTATION ON J-RESISTANCE CURVES

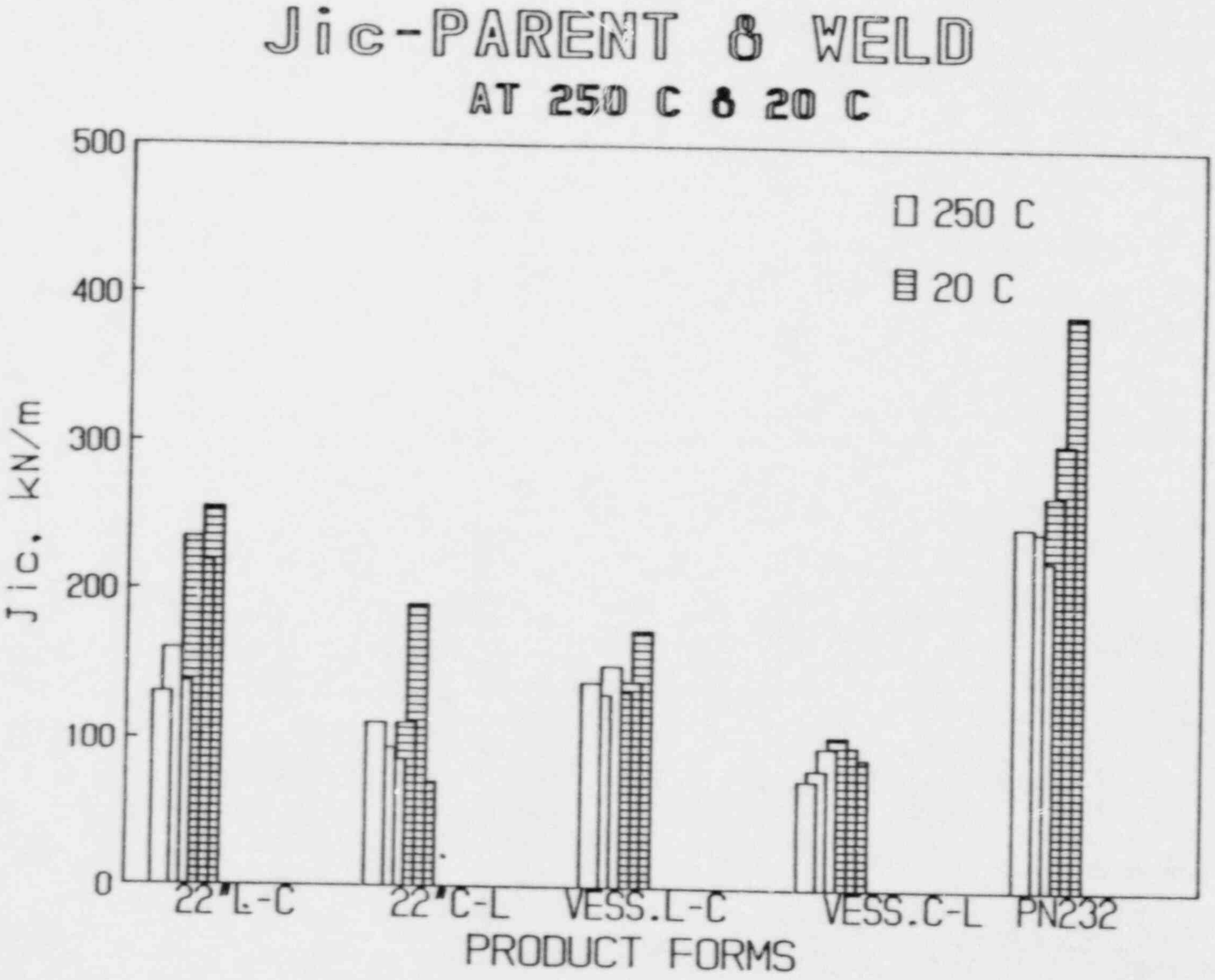


FIGURE 10

VARIATION OF J_{IC} WITH TEMPERATURE FOR TWO PRODUCT FORMS AND PN232 WELD

File JAPAN F10

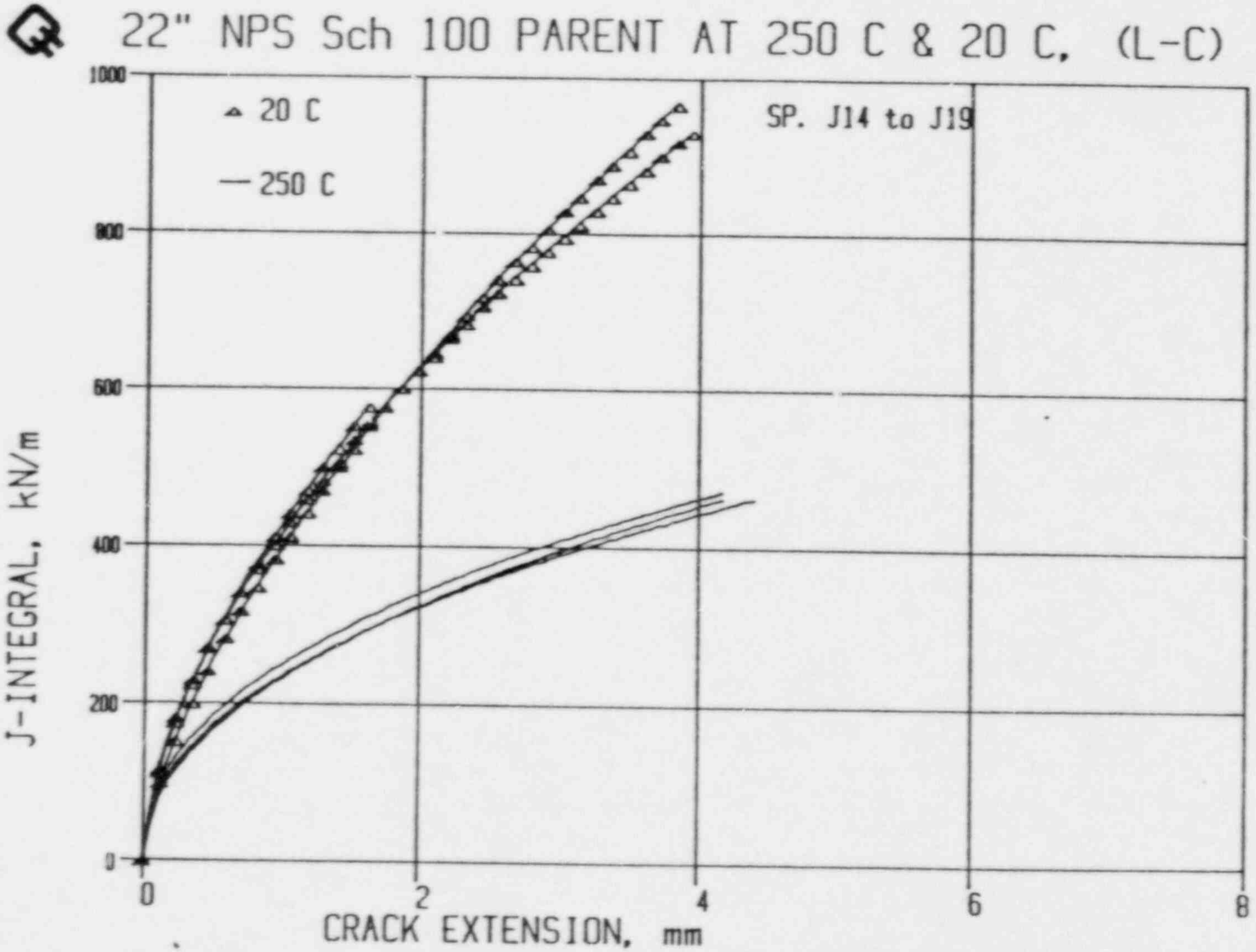


FIGURE 11
EFFECT OF TEST TEMPERATURE ON J-RESISTANCE CURVES

File J 14-19

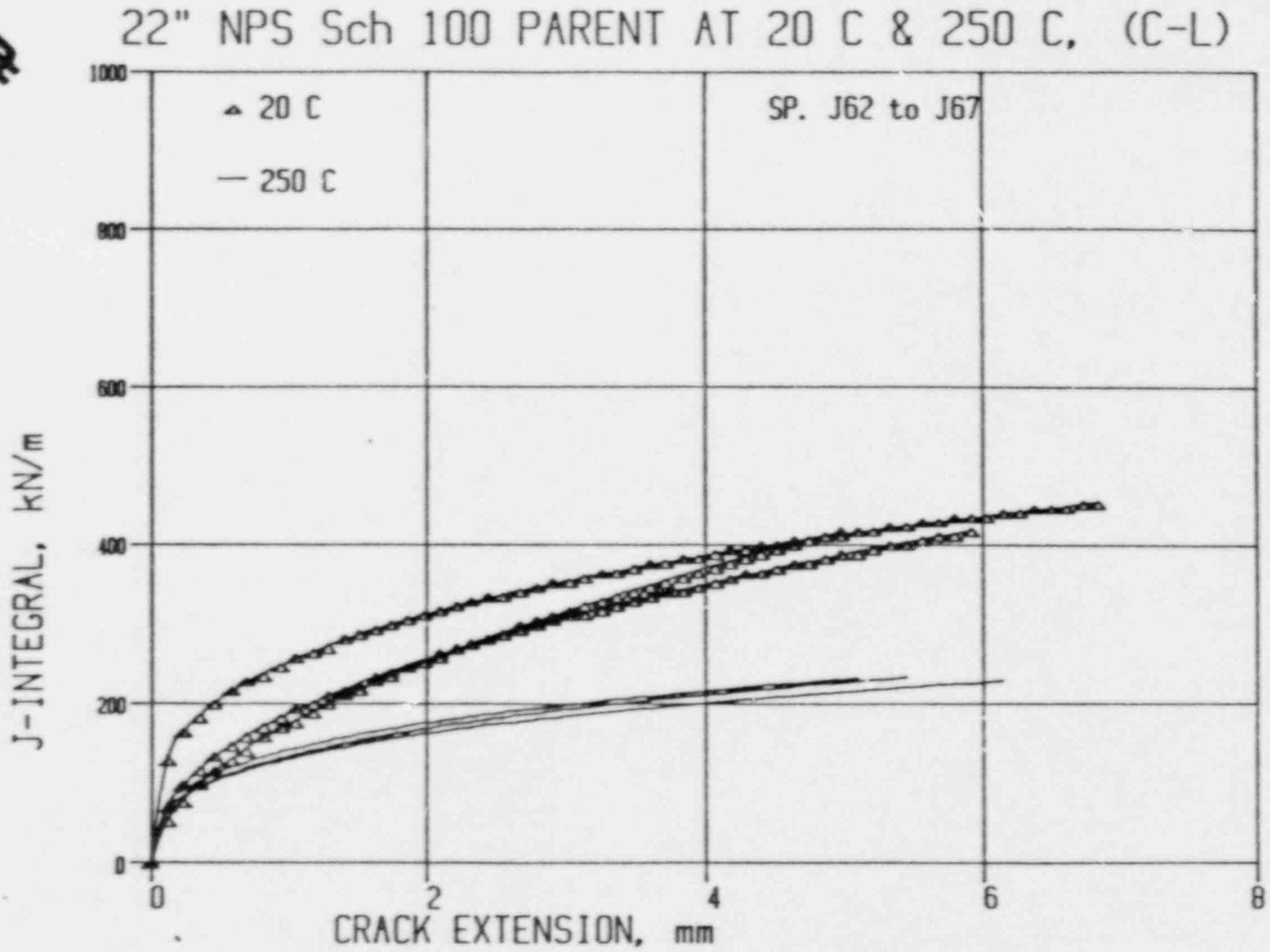


FIGURE 12

EFFECT OF TEST TEMPERATURE ON J-RESISTANCE CURVES

File J 62-67

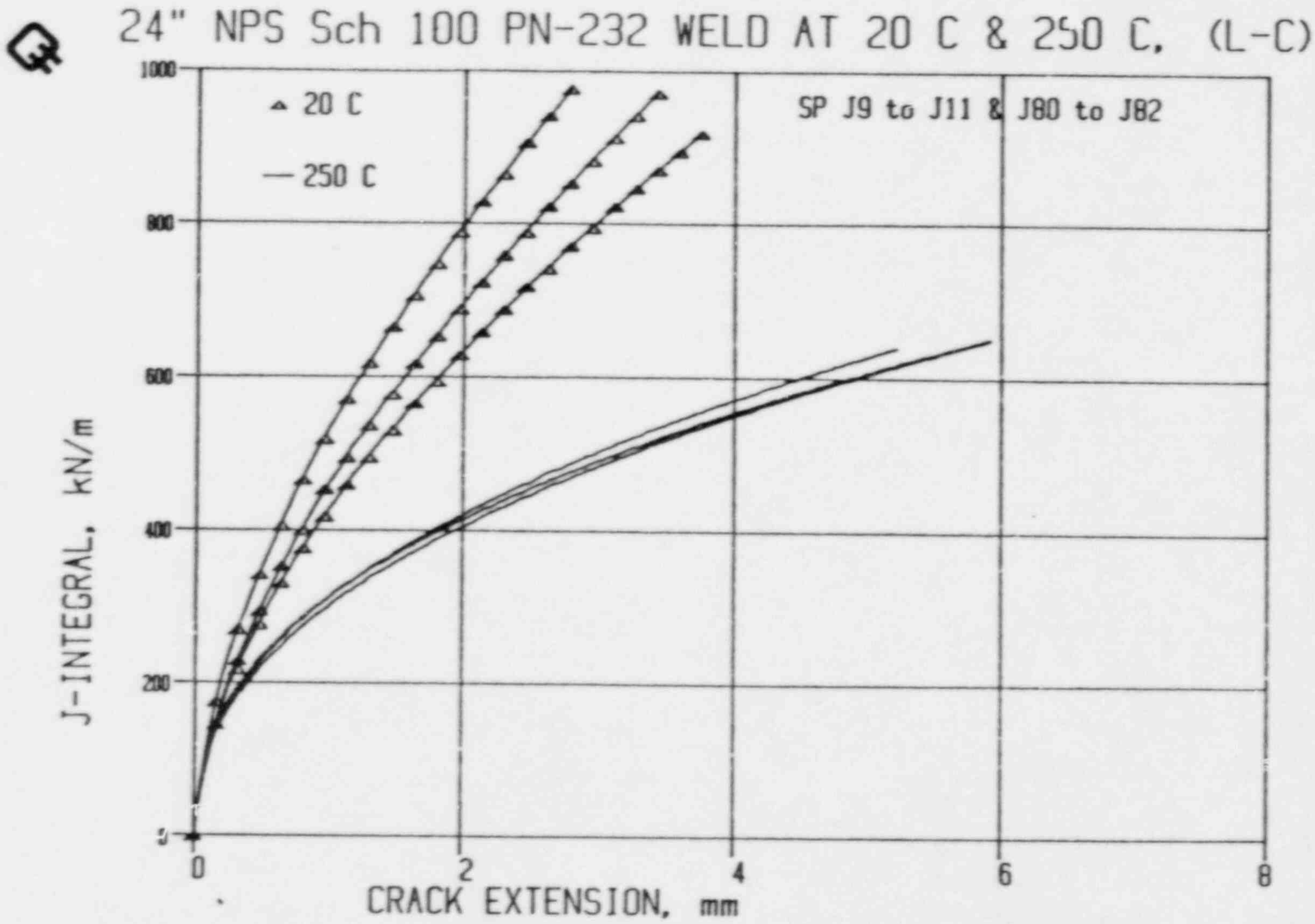


FIGURE 13

EFFECT OF TEST TEMPERATURE ON J-RESISTANCE CURVES

File J 80-82

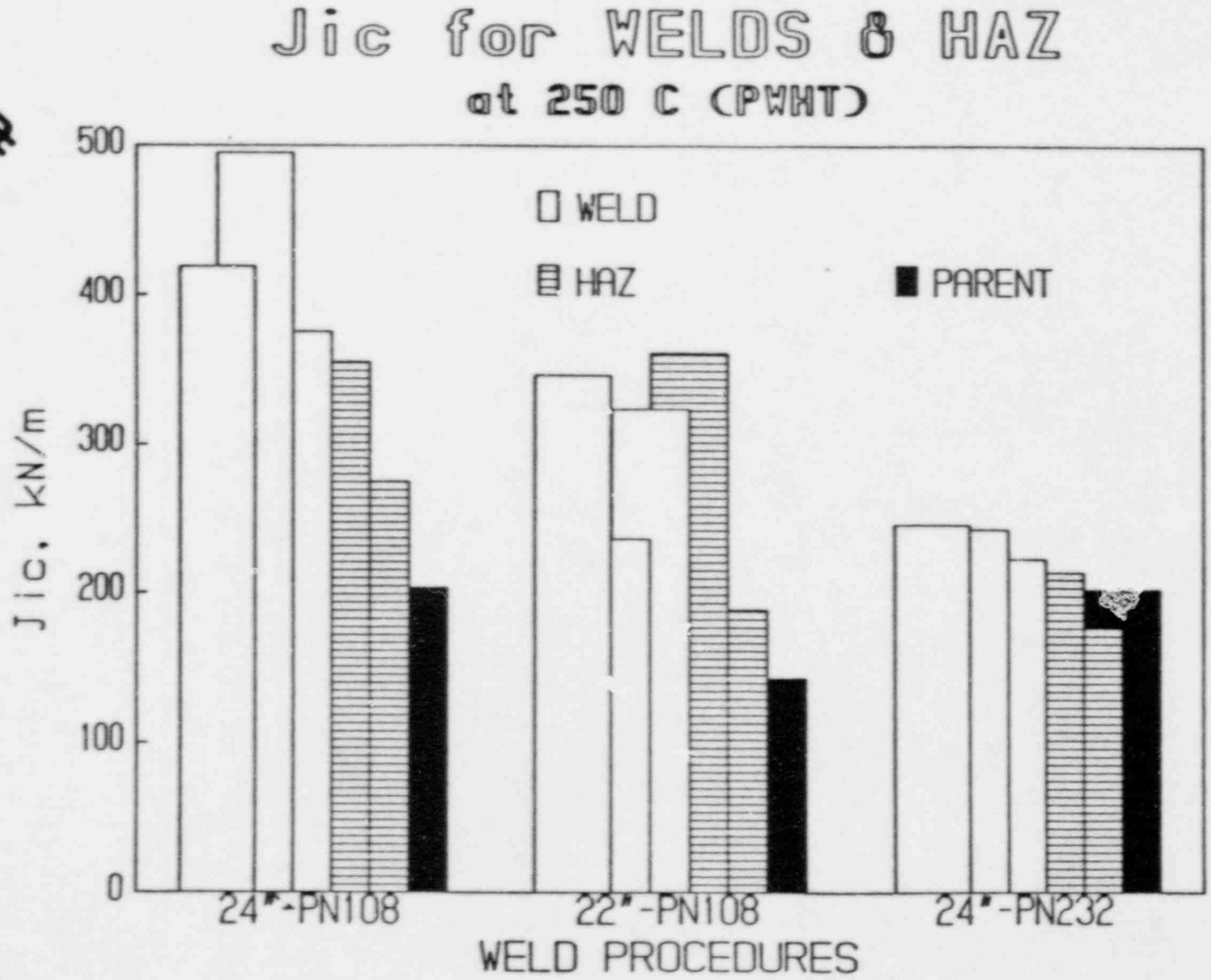


FIGURE 14
WELD AND HAZ J_{IC} VALUES

File JC HA21

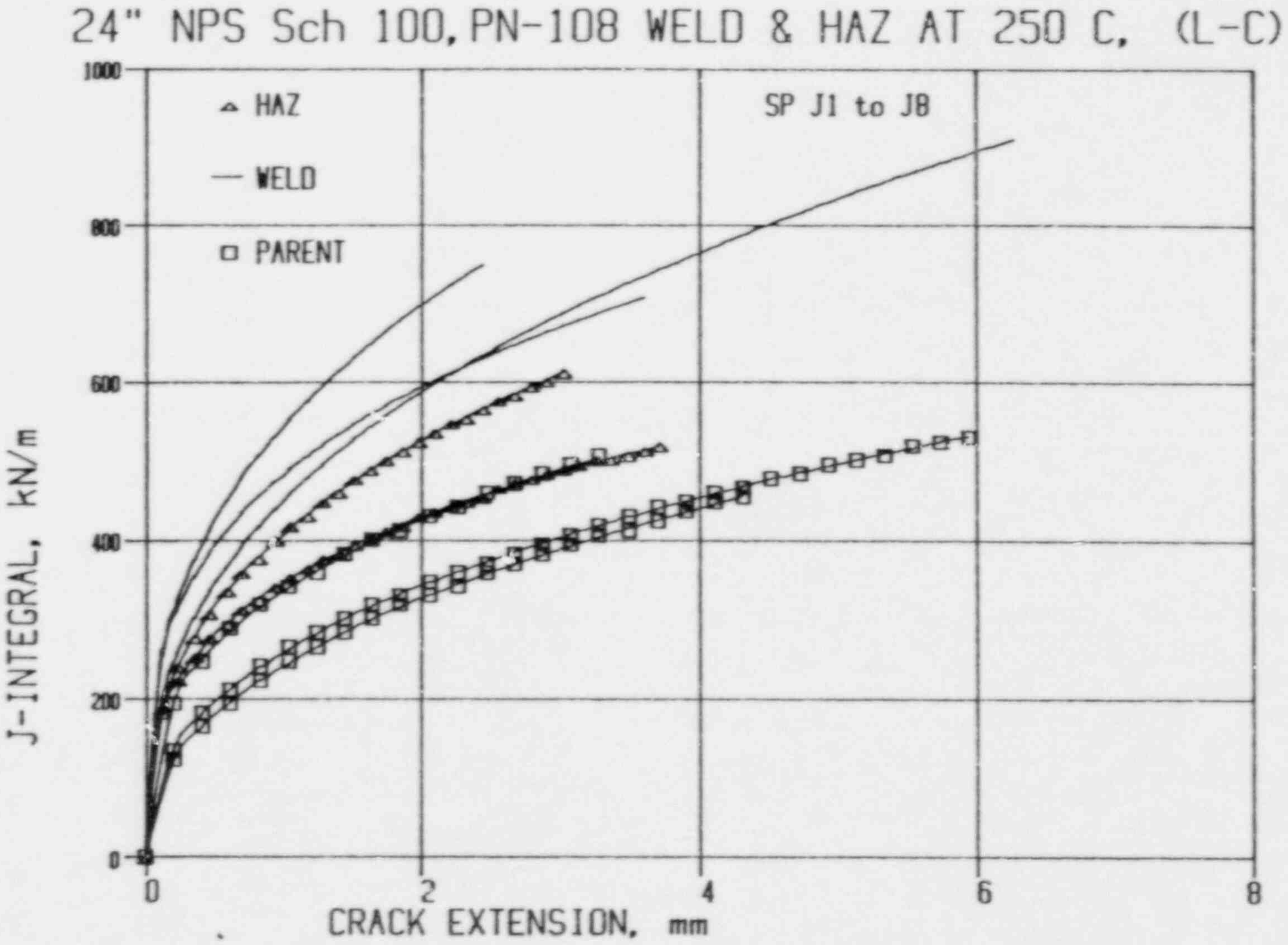


FIGURE 15

COMPARISON OF J-RESISTANCE CURVES FOR WELD HAZ AND PARENT MATERIAL

File SMIRT F5

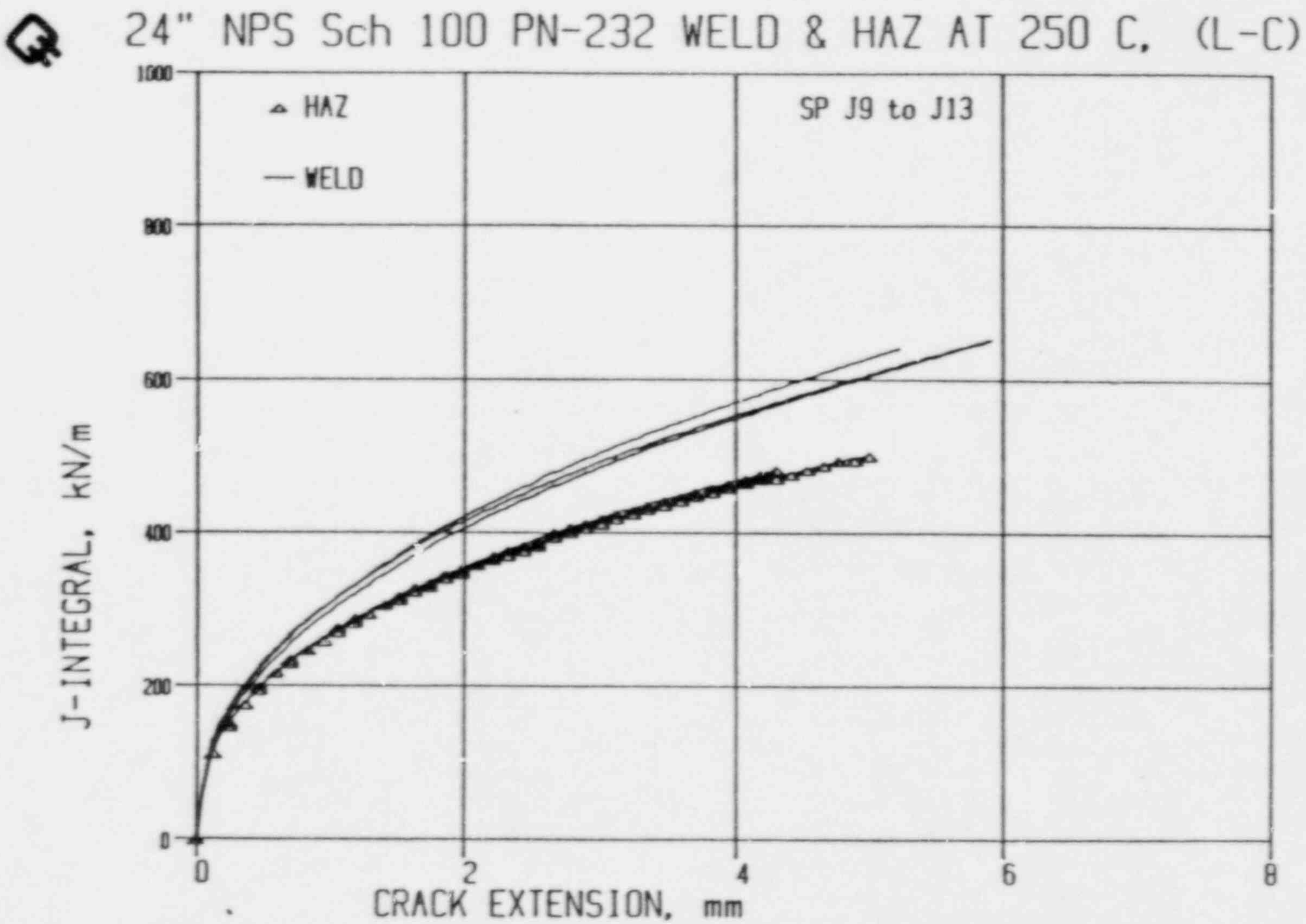


FIGURE 16

COMPARISON OF J-RESISTANCE CURVES FOR WELD AND HAZ

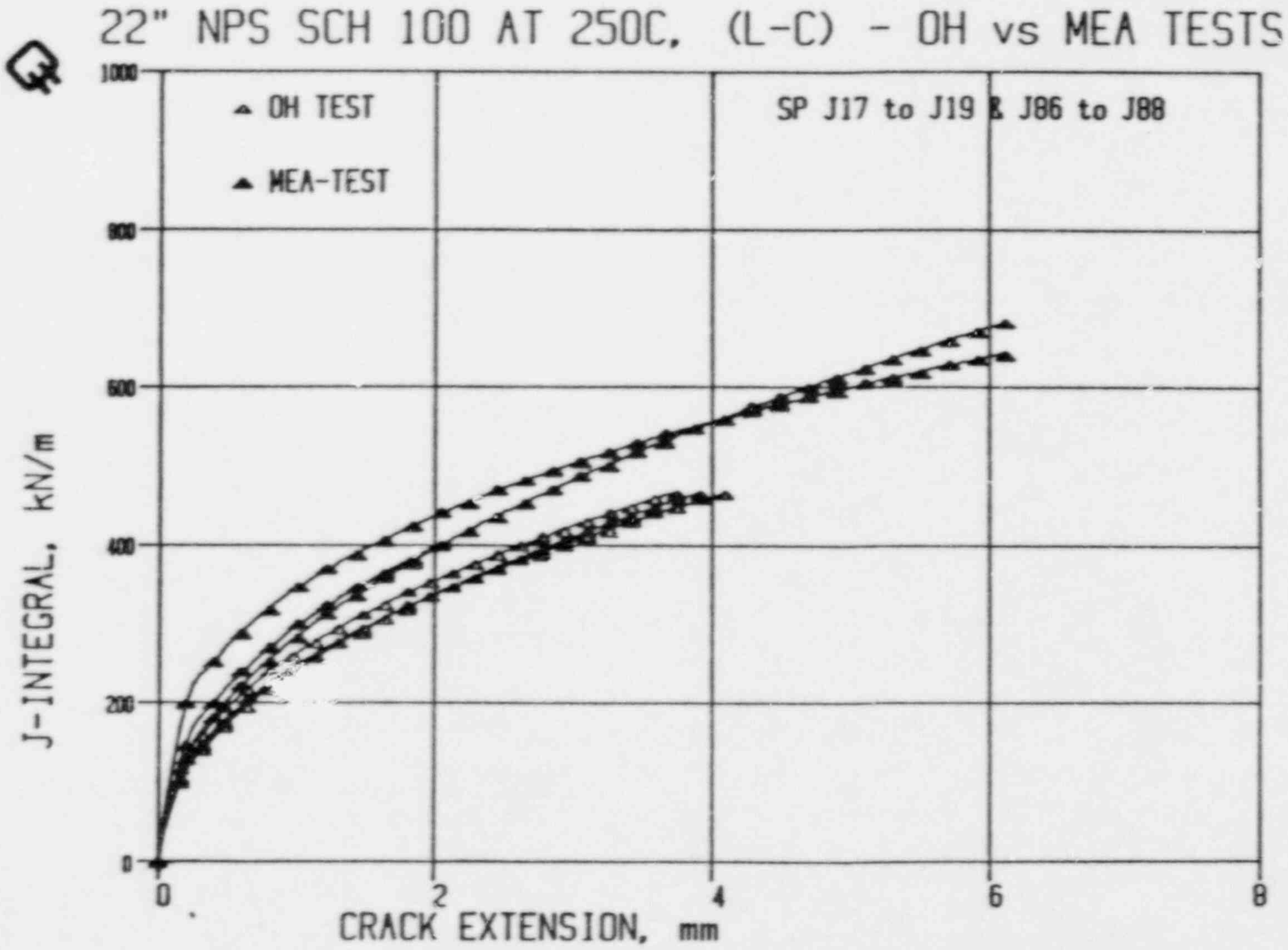


FIGURE 17

COMPARISON OF J-RESISTANCE CURVES BETWEEN OH AND MEA TESTS

SESSION 4: LEAK RATE STUDIES

Chairman: P. P. Milella, ENEA, Italy

LEAK RATE RESEARCH AT EPRI

A PRESENTATION TO THE
LBB TOKYO SEMINAR

D. M. NORRIS
K. KISHIDA
V. CHEXAL

MAY 14, 1987

TOKYO, JAPAN

PICEP: PIPE CRACK EVALUATION PROGRAM

COMPUTES:

- CRITICAL CRACK LENGTH
- CRACK OPENING AND DISPLACEMENT (AREA)
- LEAKAGE RATE

NEW MODELS IN PICEP

FLUID:

- TWO-PHASE MIXTURE
- SATURATED STEAM
- SUPERHEATED STEAM

CRITICAL CRACK LENGTH:

- Z FACTORS FOR EPFM

CRACK-OPENING AREA:

- COMBINED TENSION AND BENDING

LEAK RATE VALIDATION DATA BASE

- BATTELLE COLUMBUS LABORATORY
- DUANE ARNOLD
- UNIVERSITY OF CALIFORNIA AT BERKELEY
- CANADA (AECL AND ONTARIO HYDRO)
- WYLE LABORATORY
- ARGONNE NATIONAL LABORATORY
- ITALY (CREC)
- JAPAN (IHI)

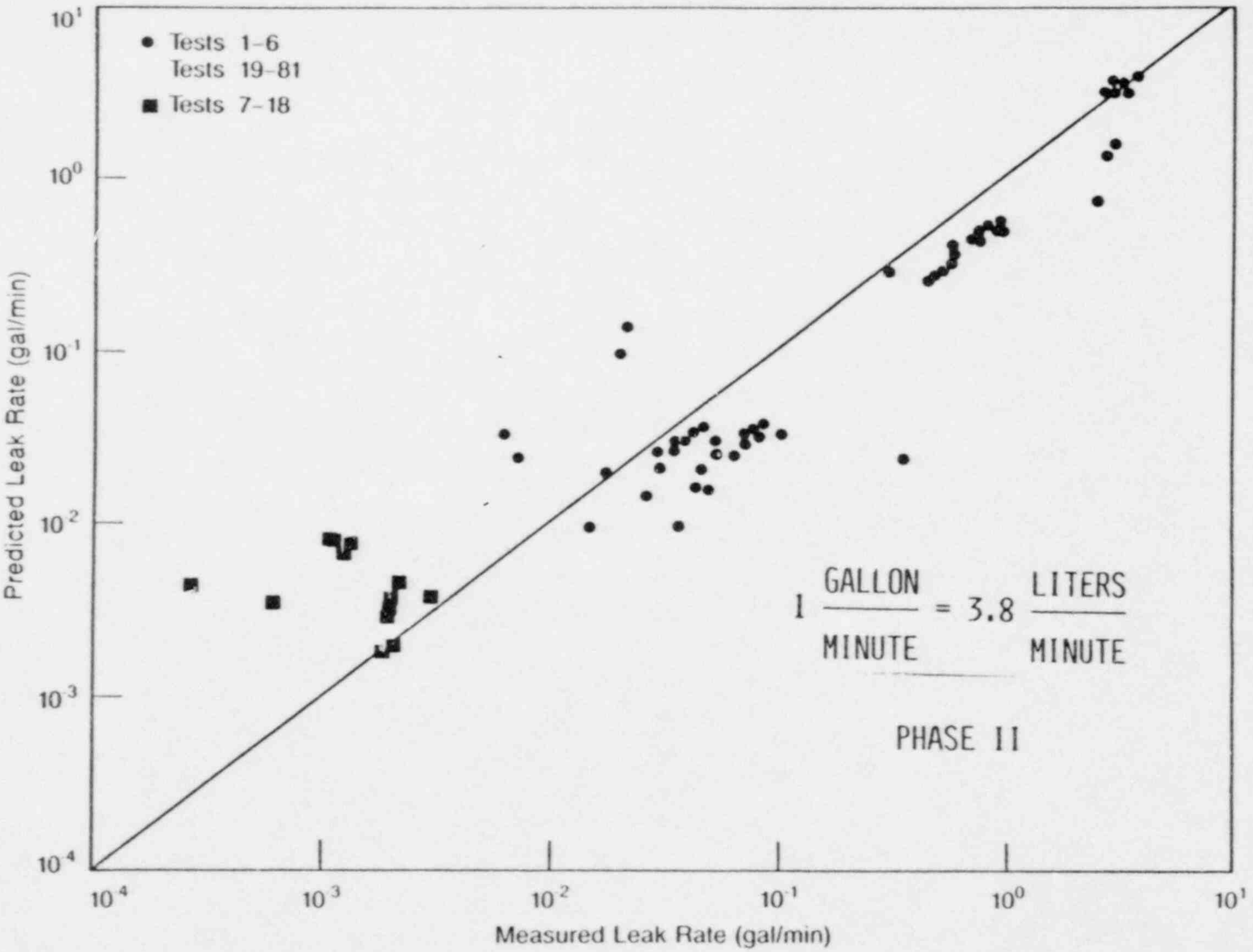
SUBCOOLED OR SATURATED LIQUID

- MOMENTUM EQUATION
- PRESSURE DROPS
- FRICTION FACTOR
- CRITICAL MASS FLOW EQUATION
- MASS TRANSFER RATE

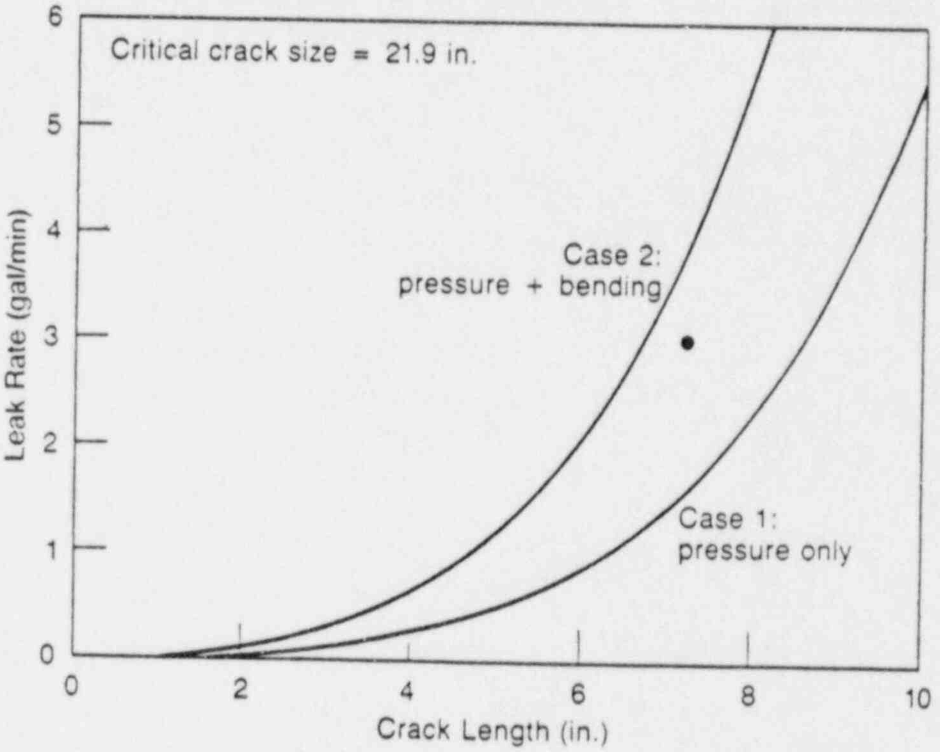
TWO-PHASE MIXTURE OR SATURATED STEAM

- BLEND INERTIA MODEL AND CRITICAL FLOW MODEL
- DISCHARGE BASED ON SUBCOOLED PROCEDURE ASSUMING FLASHING AT ENTRANCE

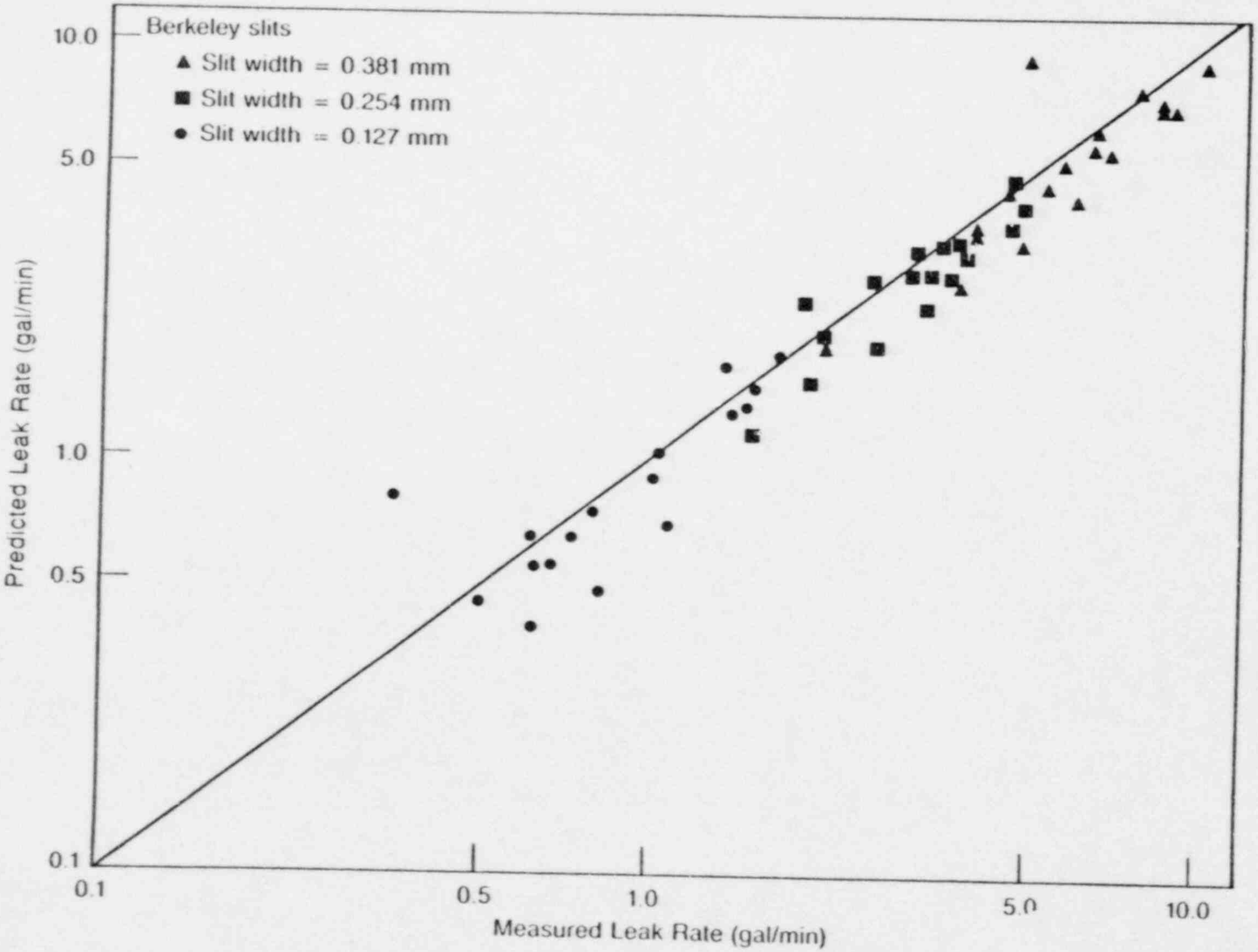
BATTELLE COLUMBUS LAB IGSCC



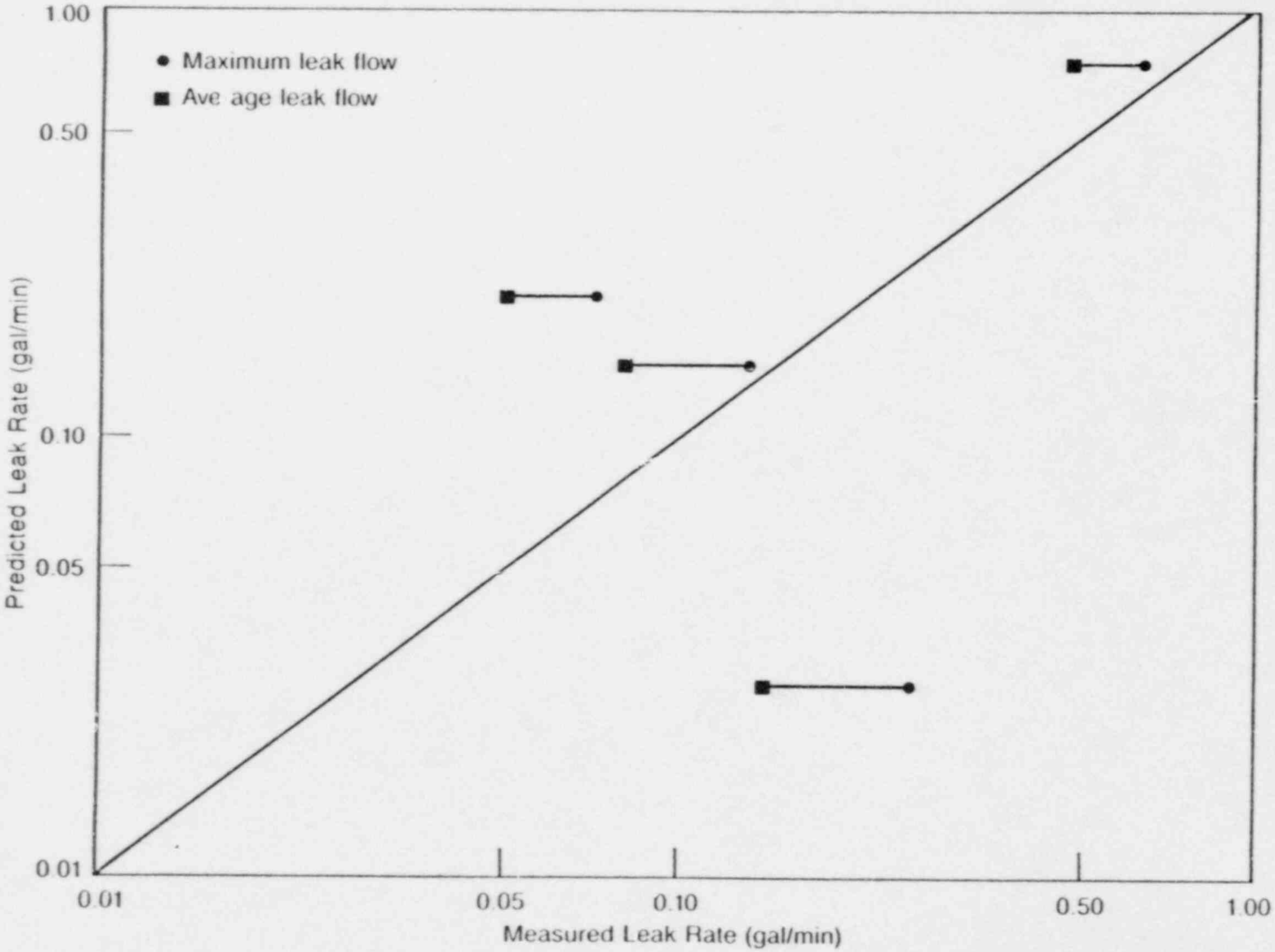
DUANE ARNOLD PLANT SAFE END CRACK



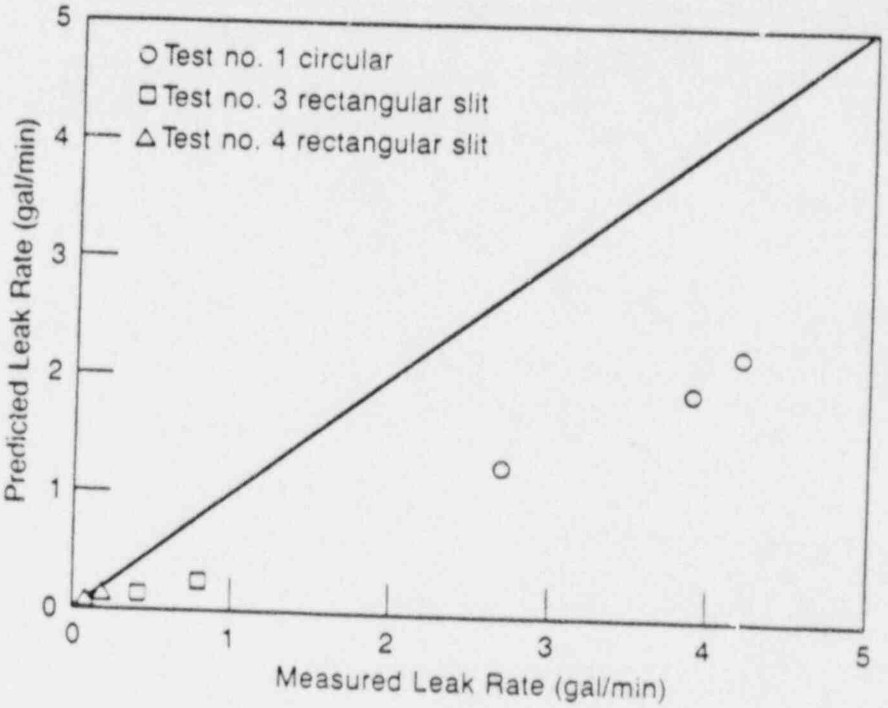
U.C. BERKELEY SLITS



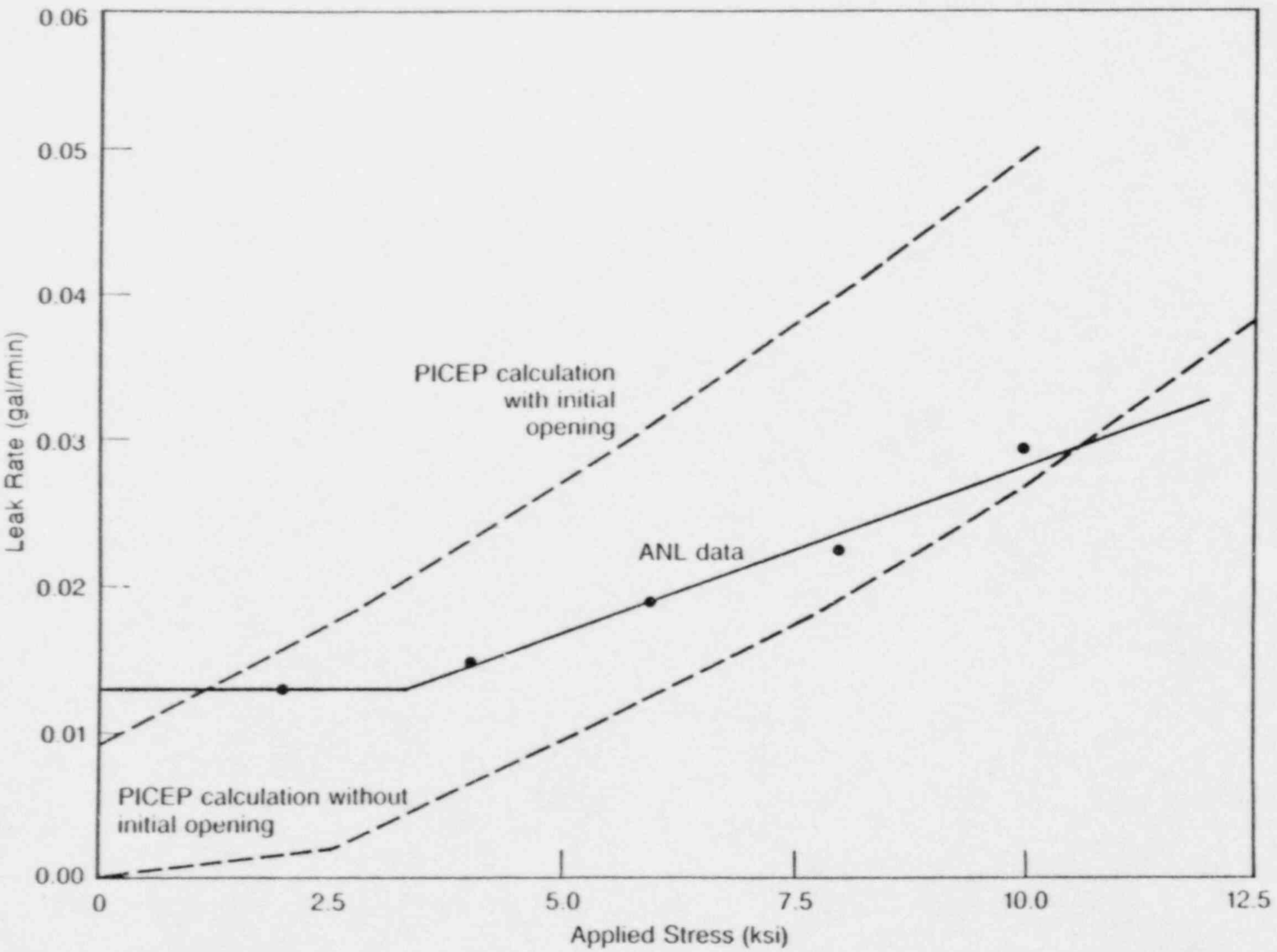
CANADIAN FATIGUE CRACK



WYLE SLITS

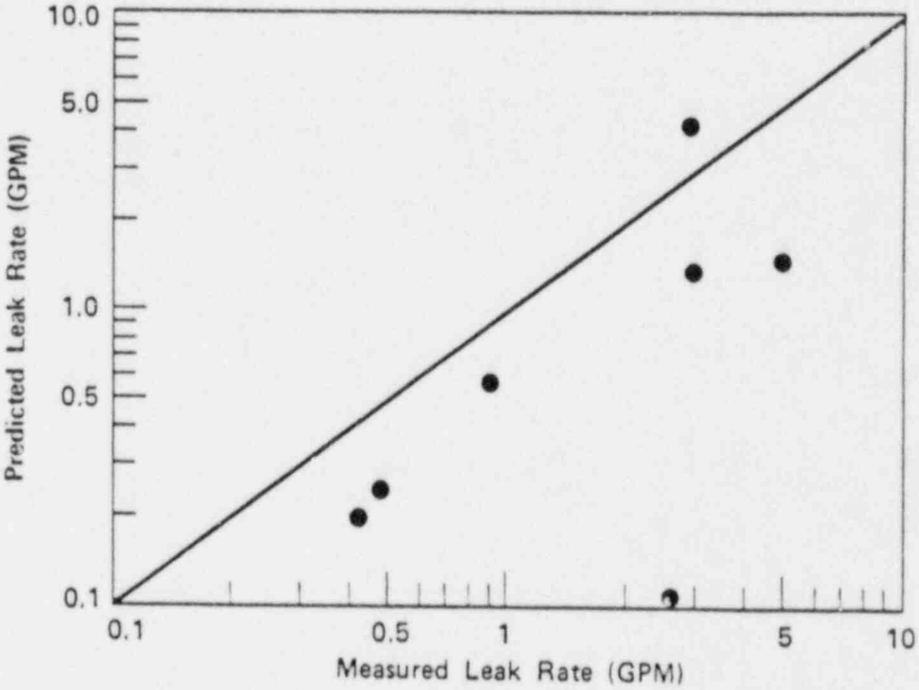


○ Test no. 1 circular
□ Test no. 3 rectangular slit
△ Test no. 4 rectangular slit
Predicted Leak Rate (gal/min)
Measured Leak Rate (gal/min)
0
1
2
3
4
5
0
1
2
3
4
5

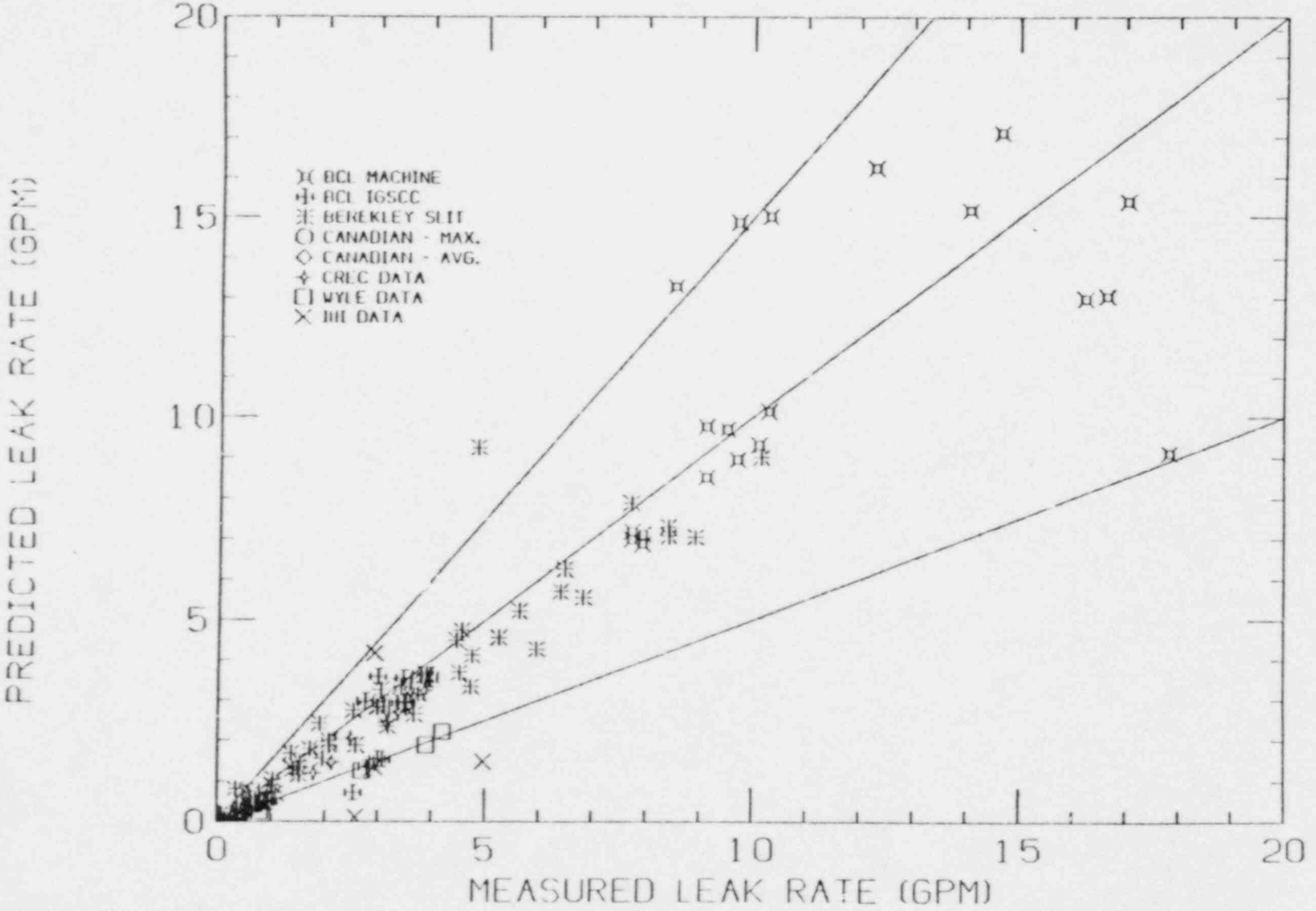


ARGONNE NATIONAL LAB SLITS

JAPANESE (IHI) SLITS



ALL DATA



PREDICTED LEAK RATE (GPM)
MEASURED LEAK RATE (GPM)
0
5
10
15
20
BCL MACHINE
BCL IGSCC
BEREKLEY SLIT
CANADIAN - MAX.
CANADIAN - AVG.
CREC DATA
WYLE DATA
IHI DATA

CONCLUSIONS

- ACCURACY INCREASES AT HIGHER LEAKAGES
- ACCURACY - ±50%, 1 TO 20 GPM
- SUMMARIZED IN EPRI NP-3596-SR REV. 1 (IN PRESS)

LEAK BEFORE BREAK

SEMINAR

organized by CRIEPI

TOKYO, May 13 - 16, 1987

***

RECENT RESULTS OF LEAK RATE STUDIES AT PWR CONDITIONS IN FRANCE

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SUMMARY

The results presented in this paper are based upon experimental works performed at the Nuclear Research Center of Cadarache and aimed at estimating the mass fluxes in throughwall cracks.

The previous studies about leak rate estimation had generally been carried out within the scope of intergranular stress corrosion problems regarding Boiling Water Reactors piping system, it was therefore necessary to investigate in the domain of Pressurized Water Reactors primary conditions, so that to ensure the related leak before break assessment.

The test program was dedicated to simulated tight cracks with different crack wall surface conditions, either smooth or rough surfaces. When compared to the calculations performed with the help of existing two phase flow models, the test results actually showed the necessity to introduce some modifications in the modelization, specifically to better take in account the wall roughness.

The accuracy of the mass flux predictions resulting of these modifications applied to the model already adopted by the Battelle Columbus Laboratories, appears very satisfactory while remaining slightly conservative from the safety point of view.

⁂

1 - INTRODUCTION

The leak before break concept requires that we know how to estimate the leak flow rate in a throughwall crack under level 2 service conditions*, wich relies on the dual knowlegde of the mass flux on the one hand, and of the crack breach area on the other hand.

It is therefore obvious that the accuracy of the estimation depends on the accuracy of both terms. The following is only concerned with the first term, noting that in order to ensure the conservatism of the leak before break methodology we must be in a position to propose a calculation model which underestimates the flow rate and then the mass flux. It is however necessary that the underestimation degree should be reasonable in relation to the safety coefficient to introduce; this is a matter of leak detection system reliability and beyond that, of the power of the methodolofy for the design and analysis of the systems which participate in the reactor safety.

The results presented hereafter are based on experimental works resulting from a quadripartite co-operation between CEA, FRAMATOME, EDF and WESTINGHOUSE - the major part of these works having been carried out at the Nuclear Research Center of CADARACHE - in order to estimate the mass fluxes in throughwall cracks which might affect the PWR primary piping system.

2 - BACKGROUND

Major experimental programmes have been carried out a little before 1980 at Columbus by the BATTELLE Laboratory under EPRI (1), (2) and also a little after at the University of BERKELEY (3) in order to work out a data base on flow rates in throughwall cracks.

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* according to AFCEN/RCC-M (French standards)

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The BATTELLE's works had been carried out within the scope of intergranular stress corrosion problems faced in BWR reactors. They have made it possible to propose a flow model, basically satisfactory (HENRY's approach), and which, in spite of rather a significant dispersion, is situated around the experimental mean results obtained from simulated cracks. It can be noted however, that the dispersion is still greater in the case of tests on real IGSCCs where the surface roughness was not accurately evaluated.

In this context, **the purpose of the programme tackled at** CADARACHE at the time when some of these results were already known, **was also to study mass fluxes, but concentrating on the thermodynamic conditions in PWR primary systems** and aiming in addition at evidencing the role of the crack wall surface roughness, with a high or low roughness.

3 - EXPERIMENTAL RESULTS

The results given in table 1 are representative of tight cracks, i.e. of cracks whose depth/hydraulic ratio, $L/_{Dh}$, is >80.

These cracks have been made in plates of a thickness > 40 mm and whose dismantling capability made it possible to achieve and control the smooth surface conditions ($k < 10^{-3}$ mm) or rough conditions ($k > 10^{-1}$ mm, refer to Fig.1).

Leak flow rates have spread between 0.166 and 1.63 kg/sec., i.e. from about 4 to 40 GPM and, depending on the tests, the subcooled conditions were caused to vary: $10°C < T_{sub} = (T_{sat} - To) < 45°C$.

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4 - CHOOSING A MODEL

WESTINGHOUSE, a partner in the quadripartite co-operation, had selected two calculation models for comparison purposes againts the experimental results, the first one being but the one used by BATTELLE (isentropic flow without slipping), hereafter designated as BATTELLE-HENRY (BH), and the second one derived from the methods proposed by GRIFFITH after FAUSKE and HENRY (4) (with a slipping assumption) designated here as FAUSKE - HENRY - GRIFFITH (FHG).

For most situations related to PWR primary piping these two models were intended to be a best estimate, an upper (FHG) and lower bound (BH) for the leak rate through tight cracks.

A further model, with separate phases taking in account the mechanical and thermal non equilibrium, developed in common by CEA, EDF and FRAMATOME and introduced in the CATHARE code (5) has also been used by EDF in the calculation/experiment comparison.

The extreme and average deviations of the predicted mass fluxes, when the different models are applied directly without any special precaution, are shown in table 2 hereunder:

TABLE 2

G/G_{exp}	B.H.	F.H.G.	CATHARE
minimum	- 44 %	- 2%	- 11%
maximum	+ 3 %	+ 58 %	+ 43 %
average	- 26 %	+ 20 %	+ 31 %

./.

This demonstrates that it is indeed possible to bracket the test results with an approximation better than 60%.

We have however tried to improve the calculation/experiment correlation by introducing two types of modifications in the B.H. model which immediately appears as rather a conservative model from the safety point of view:

- a better account of the crack walls surface conditions,
- a correction of the parameter ruling the distance as from which friction through the crack (flashing inception) is considered.

As a matter of fact, on the basis of a study (CEGB (6)), it has appeared that the friction coefficient, according to the KARMAN - PRANDTL formula, where the asperity height is considered, is no longer satisfactory when the surfaces are smooth. It is then necessary to use a relation which is a function of the REYNOLDS number.

Regarding the flashing point, a visualization carried out in the WESTINGHOUSE laboratories has shown that the phenomenon is very unstable and has therefore a marked hazardous nature. For the flashing relative distance, the value proposed in the F.H.G model has been preferred because it better reflects the results obtained in the case where the water stagnation subcooling is moderate.

Table 3 reports these modifications and shows that the **correlation then becomes very satisfactory** while remaining conservative, except for the test with the highest subcooled condition.

In order to widen the validation of the model thus corrected, we have integrated the experimental data obtained at the University of BERKELEY for cracks with smooth wall surfaces, in the domain similar to the PWR primary thermodynamic conditions and which brackets the domain explored at CADARACHE. The findinds are the same.

./.

By considering both data groups together, Fig.2 shows that, for a total of 27 points, the correlation underestimates the mass flux by 8 % as an average. The trend is more pronounced in the case of low or moderate subcooling, $T_{sub} < 30°C$, while for $45°C < T_{sub} < 62°C$, the mass flux is slightly overestimated.

5 - CONCLUSION

The experimental works carried out at Cadarache and the corresponding analysis show that we are now in possession of the necessary means for a **very good prediction of mass fluxes in throughwall cracks of Pressurized Water Reactor primary piping** at nominal service conditions, provided that we can assess the hydraulic diameter, (i.e. practically the opening δ) and the crack surface roughness k.

Where the following domain only is considered:

$120 < P < 155$ bars for the primary water
$\Delta T_{sub} < 60°C$ thermodynamic characteristics,

and the ratio $L/D_H > 80$ for the crack geometry,
using the modified BATTELLE-HENRY correlation underestimates very little the mass flux as an average with a narrow scattering, $\Delta G/G_{exp} = -8 \pm 25\%$, and therefore constitutes an excellent method for approaching its value.

Besides, considering the difficulty to appreciate the geometrical characteristics of real cracks, the accuracy is very satisfactory and it **does not appear necessary to make provision for additional theoretical or experimental developments in this respect.**

REFERENCES

(1) R.P. COLLIER et Al — Study of critical two-phase flow through simulated cracks. BCL-EPRI 80-1 Nov.80.

(2) R.P. COLLIER et Al — Two-phase flow through IGSCC s and resulting acoustic emissions. BCL-EPRI NP 3540, final report April 1984.

(3) CN AMOS and V.E.SCHROCK — Two-phase critical flow in slits. Nuclear Science and Engineering 88 261-274 (1984).

(4) P. GRIFFITH — Choked two-phase flow. M.I.T. Summer course, 1973.

(5) G. HOUDAYER and J.C. ROUSSEAU — The CATHARE Code, qualification and analytical experiments. 10th Water Reactor Safety meeting, WASHINGTON 1982.

(6) R.J. TYRRELL and G.C. GARDNER — Friction factor measurement for flow through model of cracks, CEGC Memorandum, March 1984.

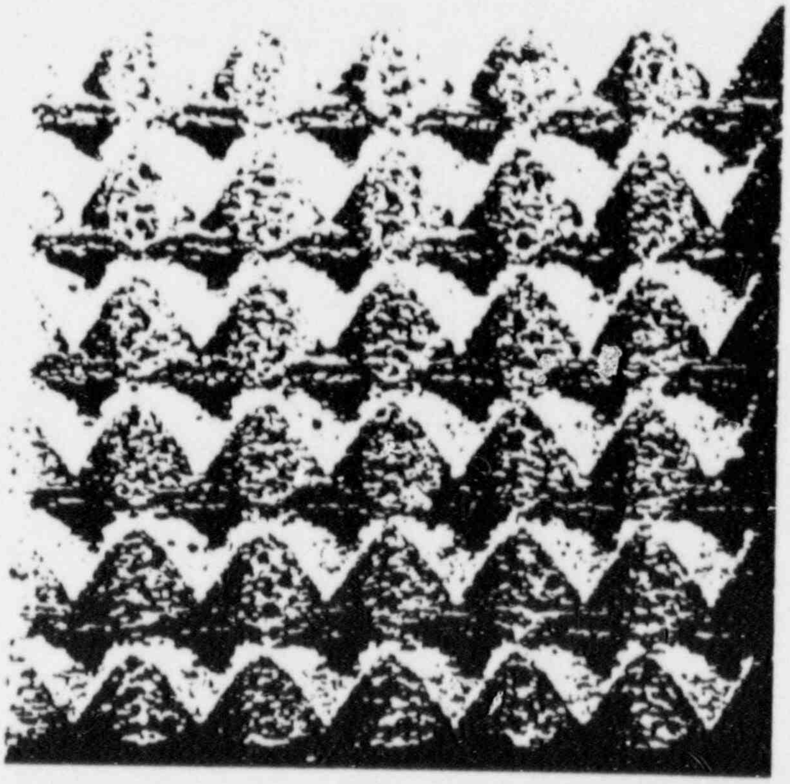
TABLE I

CADARACHE EXPERIMENTAL RESULTS

Wall surface Condition	$^{k}/_{DH}$	$^{L}/_{DH}$	Stagnation			G exp 10^5kg/m^{2}sec
			pressure (MPa)	température (°C)	Δ T subcooling (°C)	
smooth k < 10^{-6}m	< 5 10^{-3}	213	15.0	321.4	20.7	0.3143
	"	"	15.0	322.0	20.1	0.3050
	< 2 10^{-3}	178	15.0	317.0	25.1	0.3646
	"	"	14.0	317.0	19.6	0.3281
	"	"	13.0	316.5	15.5	0.3177
	< 1,2 10^{-3}	102	15.0	332.0	10.1	0.4077
	"	"	15.0	317.5	24.6	0.4851
	"	"	15.0	297.0	45.1	0.4643
	"	"	14.0	317.0	19.6	0.3690
	"	"	13.5	317.5	16.3	04345
	"	"	13.0	316.5	14.3	0.3330
	"	"	12.5	317.5	10.3	0.4196
rough k = 0.136 10^{-3}m	0.152	95	15.0	317.0	25.1	0.2790
	"	"	14.0	317.0	19.6	0.2193
	"	"	13.0	316.5	14.3	0.1984
rough k = 0.130 10^{-3}m	0.122	80	15.0	317.0	25.1	0.2707
	"	"	14.0	316.5	20.1	0.2448
	"	"	13.0	316.5	14.3	0.2241

ROUGH WALL CRACK

Crack opening



WALL SURFACE ASPECT, MAGNIFICATION ≃ 100

FIGURE 1

TABLE 2
MODEL/EXPERIMENT COMPARISON

Stagnation		G exp $10^5 kg/m^2 sec$	G1 cal initial B.H. $10^5 kg/m^2 sec$	Δ G1/G exp (%)	G2 cal modified B.H. $10^5 kg/m^2 sec$	Δ G2/G exp (%)
Pressure (MPa)	ΔT subcooling (°C)					
15.0	20.7	0.3143	0.2087	- 33.6	0.2657	- 15.5
15.0	20.1	0.3050	0.2074	- 32.0	0.2635	- 13.6
15.0	25.1	0.3646	0.2727	- 25.2	0.3349	- 8.1
14.0	19.6	0.3281	0.2467	- 24.8	0.2979	- 9.2
13.0	15.5	0.3177	0.2239	- 29.5	0.2646	- 16.7
15.0	10.1	0.4077	0.2904	- 28.8	0.3244	- 20.4
15.0	24.6	0.4851	0.3510	- 27.6	0.4196	- 13.5
15.0	45.1	0.4643	0.4754	+ 2.4	0.5446	+ 17.3
14.0	19.6	0.3690	0.3163	- 14.3	0.3571	- 3.2
13.5	16.3	0.4345	0.3012	- 30.7	0.3423	- 21.2
13.0	14.3	0.3330	0.2849	- 14.5	0.3271	- 1.9
12.5	10.3	0.4196	0.2659	- 36.6	0.3274	- 22.0
15.0	21.5	0.2790	0.1573	- 43.6	0.2051	- 26.5
14.0	19.6	0.2193	0.1443	- 34.2	0.1875	- 14.5
13.0	14.3	0.1984	0.1324	- 33.3	0.1705	- 14.1
15.0	25.1	0.2707	0.1943	- 28.2	0.2636	- 2.6
14.0	20.1	0.2448	0.1784	- 27.1	0.2401	- 1.9
13.0	14.3	0.2241	0.1623	- 27.6	0.2182	- 2.6

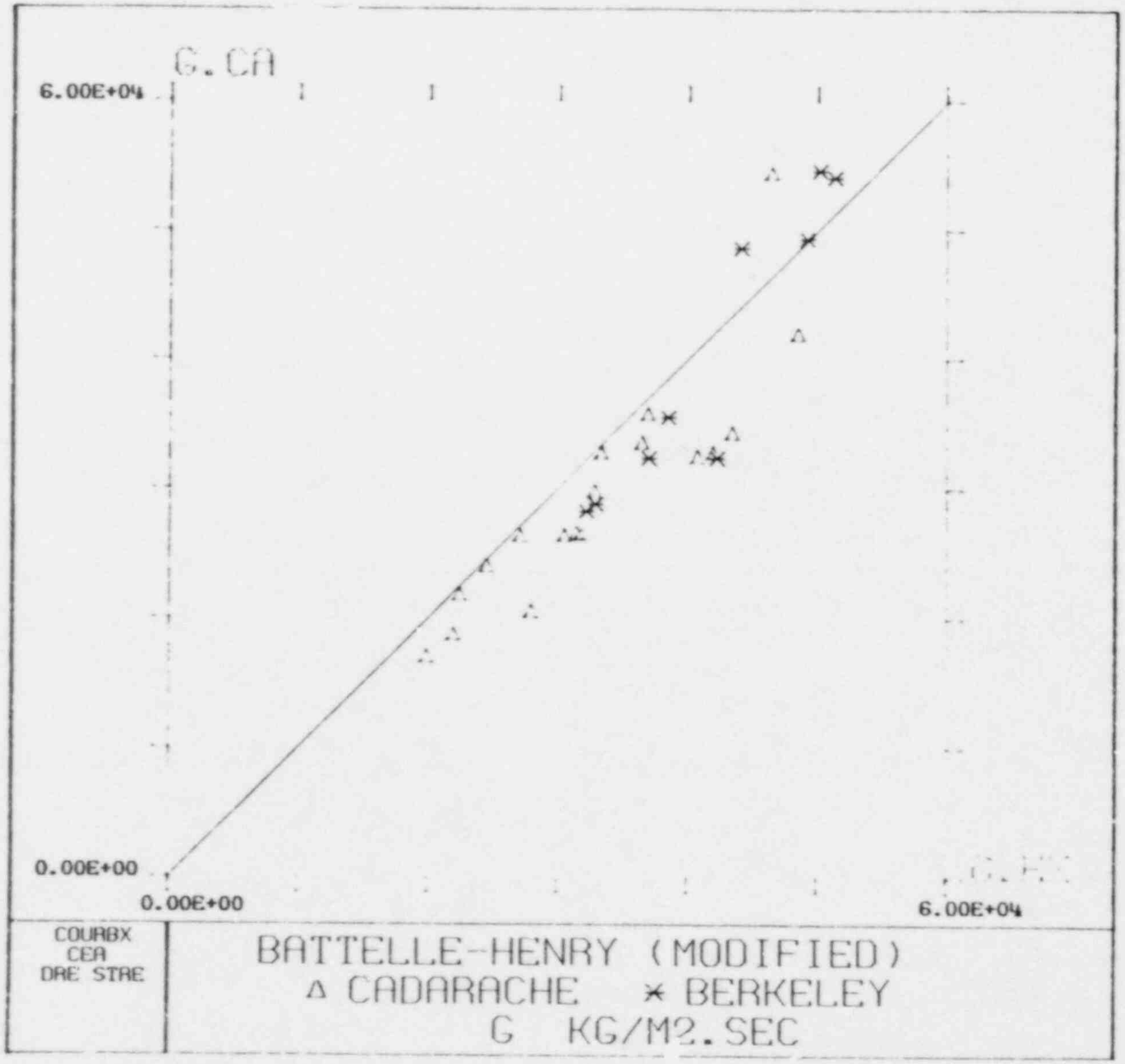

G.CA
6.00E+04
0.00E+00
0.00E+00
6.00E+04
G.HE
COURBX
CEA
DRE STRE
BATTELLE-HENRY (MODIFIED)
Δ CADARACHE
✶ BERKELEY
G KG/M2.SEC

Figure 2

SESSION 5: PIPE FRACTURE TESTS

Chairman: G. Yagawa, University of Tokyo, Japan

SIGNIFICANCE OF DEGRADED PIPING PROGRAM RESULTS ON LBB AND IN-SERVICE FLAW INSPECTION CRITERIA

by

Gery M. Wilkowski
Battelle Columbus Division

The objective of the Degraded Piping Program is to verify and improve fracture mechanics analysis methods for nuclear power plant piping. Results of this program, which is now in its final year, will provide bases for regulatory decisions regarding applications for leak-before-break (LBB) and in-service flaw assessments.

At this point in the program, it is possible to make an evaluation of the benefits this program will provide by the closure of the current program. Numerous tasks have been undertaken to satisfy regulatory needs. The remaining discussion in this summary briefly describes the many technical issues and their impact on current or future regulatory needs, that are anticipated to be sufficiently addressed by the end of this program. Technical concerns that may require further evaluation are also reviewed.

Technical Issues Sufficiently Addressed by the Completion of the Current Program

The ASME IWB-3610 (limit-load) analysis for cracks in austenitic piping has been verified. Experimental results have shown that the analysis procedure provides a better than average value for the advertised safety factor rather than a minimum safety margin. It has also been shown that a correction is needed for the pipe radius to thickness ratio for surface-cracked pipe. Larger R/t pipe fails at lower stresses, probably due to ovalization effects.

It was experimentally demonstrated that there is a large safety margin between the load at crack initiation and maximum load for low-toughness large-diameter pipe with a through-wall crack. Hence, current LBB analysis procedures could incorporate crack growth considerations to take advantage of this margin.

The ASME IWB-3640 flux weld analysis was evaluated by seven full-scale pipe fracture experiments with through-wall or surface cracks. Analytically, the analysis was found to have inherent safety margins. However, it was recently found that the material property data used in this analysis was higher than typical values for the flux welds. The experimental data indicate that these margins seemed to compensate for the use of higher than actual toughness values used in developing the analysis.

Fracture tests at LWR conditions showed that cracked pipe with weld-overlay repairs had large deformations in the unwelded pipe adjacent to the weld overlay. These large deformations occurred prior to the fracture of a crack in the overlay. In evaluating the design analysis procedures, it was found that well-defined guidelines do not exist. One could predict either very low loads relative to the experimental data or loads slightly higher than the experimental data depending on what radius, thickness or flaw depth was used. A final experiment and an analytical round robin, which are currently underway, should clarify the design analysis procedures.

A large data base has been developed from the pipe fracture experiments. This has been used to develop a screening criterion and a unified statistical criterion to predict maximum loads for carbon or stainless steel pipe. This could be applied to through-wall or surface-cracked pipe. This simplified procedure which could easily be incorporated into a code procedure, can be used for in-service flaw evaluations or LBB evaluations.

Correlations between Charpy data and fracture toughness have been verified for ferritic nuclear piping materials at LWR temperatures. This is useful for in-service flaw assessment criteria, and could be applied to mill quality control requirements for new plant construction. This was incorporated into the statistical pipe flaw analysis described above.

Technical Issues Requiring Further Evaluations

There are certain technical issues that will need to be addressed further to assess their impact on regulatory or industrial applications. Most of these evolved from investigations conducted during this program. The most significant ones are summarized below.

More data are needed for prototypical evaluations of cracks in carbon steel welds and thermal-aged pipe. These data are needed for verification of tentative ASME carbon steel pipe flaw evaluations and plant life extension evaluations.

For LBB analyses, the presence of allowable shallow surface flaws (such as those allowed by ASME Section X IWB-3514.3) could contribute to lowering the apparent toughness of the pipe. The current complex-cracked pipe results show that such a shallow flaw could lower the apparent fracture resistance by 25 to 50 percent. Further data are needed to assess this margin and evaluate its significance on LBB analyses.

Crack instabilities have been observed in many nuclear grade carbon steel piping materials at 550 F (288 C) during the course of this program. These unexpected instabilities have occurred in both laboratory and pipe experiments. It is believed to be related to dynamic strain aging that occurs in many carbon steels. The net result is a significant reduction in the fracture resistance. This metallurgical phenomenon needs further evaluation to: (1) understand why some of the carbon steels are susceptable to unstable cracking while others are not, (2) predict its effect on pipes using laboratory specimen data, (3) assess its magnitude at seismic as well as at normal operating condition strain rates, and (4) assure that it will not lower the initiation toughness for a surface-cracked pipe under a seismic loading to the extent that the current LBB approach is jeopardized.

The crack propagation toughness along a stainless steel SMAW fusion line was recently found to be lower than the fracture resistance in the SMAW. This is significant since the SMAW weld metal is currently believed to be one of the lower toughness austenitic materials. Moreover, stress corrosion cracks more frequently grow along the fusion line than into the weld metal. Further attention should be given to evaluation of crack initiation toughness and crack growth resistance along the fusion line of welds. This could impact both in-service flaw acceptance criteria, such as IWB-3640, as well as LBB acceptance criteria. Neither of these analyses address the question of fusion line toughness since this observation is a recent finding.

A theoretical finite-length surface-cracked pipe analysis is needed to verify the approximate Code criteria for conditions where there are no experimental data. Efforts are currently underway to develop such an analysis. In addition, data for shorter length surface-cracked pipe are needed under combined pressure and bending to evaluate tentative in-service flaw acceptance criteria.

For determination of crack growth resistance curves from laboratory specimens, it currently appears that two specimen sizes are needed to account for geometry effects using the Modified J-Integral approach. Such studies are underway using current data, but the applicability to different classes of nuclear piping materials should be verified.

It has been found that material anisotropy affects the fracture toughness and direction of crack propagation in full-scale pipe experiments. The possible beneficial or detrimental effects of the anisotropy should be examined more closely. For instance, most circumferential cracks in seamless carbon steel pipe propagate in a helical direction even under pure bending. What would happen if the pipe were subjected to torsional loads was well?

A large data base on nuclear material properties has been developed and will be incorporated in the NRC pipe material property data base. This will help to determine generic lower bound properties, but is not a statistically significant sample size by itself. Further data are needed, particularly for carbon steel welds, heat affected zones, and fusion lines.

SIGNIFICANCE OF DEGRADED PIPING PROGRAM RESULTS ON LBB AND IN-SERVICE FLAW INSPECTION CRITERIA

by

G. M. Wilkowski*

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* Compilation of results by J. Ahmad, C. Barnes, F. Brust, D. Guerrieri, G. Kramer, G. Kulhowvick, M. Landow, C. Marschall, M. Nakagaki, V. Papaspyropoulos, V. Pasupathi, P. Scott, and G. Wilkowski

Degraded Piping Program

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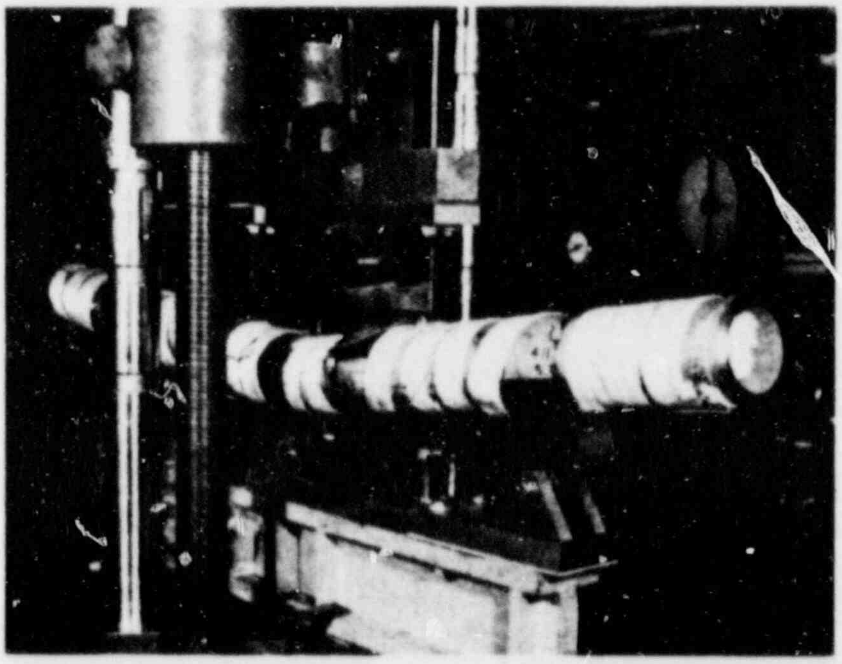
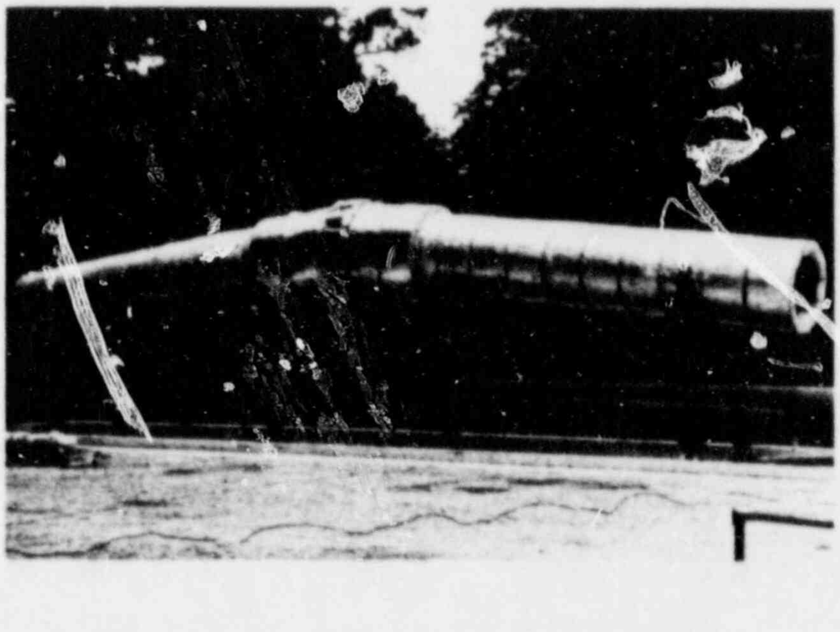
Objective: Verify and improve fracture mechanics analyses for nuclear piping

Scope:

- Assess various materials at 288 C (550 F)
- Circumferential cracked pipe
- Quasi-static bending and pressure loads
- Pipe sizes from 102 mm (4 inches) to 1,067 mm (42 inches)
- Verify/modify J-estimation schemes that can predict loads and displacements

Small and Large Diameter Pipe Experiments





Degraded Piping Program Technical Disciplines

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- Material Characterization and Metallurgy
  - Tensile and fracture toughness testing
  - Other metallurgical investigations
- Pipe Fracture Experimentation
  - Instrumentation and data acquisition
  - Testing at 550 F with pressurized water
- Analysis
  - Limit load
  - Checks on Code and NRC simplified procedures
  - Elastic-plastic fracture mechanics estimation schemes
  - Finite element detailed analysis

DP-GA4-3/87-GW

Degraded Piping Program Tasks

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- Over 60 technical subtasks in the following areas
    - Pipe Procurement
    - Material Characterization
    - Facility Modification
    - Pipe Fracture Experimentation
    - General Analytical Effort
    - Analysis of Pipe Fracture Experiment
    - Coordination With Other Programs
    - Program Management

Degraded Piping Program

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Applications:
- Leak-before-break analysis
- In-service flaw assessment analysis

Review of Results:
- Technical issue completely addressed
- Technical aspect arising from DP^3II research results

IWB-3640 Analysis for Stainless Steel

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- Base metal and weld metal (low toughness) criterion better than average, but not lower bound
- Inconsistencies in ASME Flux Weld Criteria
  - Used too high of toughness for SMAW and SAW
  - Used weld metal rather than base metal strength
  - Conservative GE/EPRI analysis procedure compensated
- Fusion line toughness half of SAW toughness.

Technical Issues Sufficiently Addressed

- Screening criteria developed to show limits of net-section-collapse analysis

- Margins between load at crack initiation and maximum load are large

Plastic-Zone Screening Criteria for TWC Pipe

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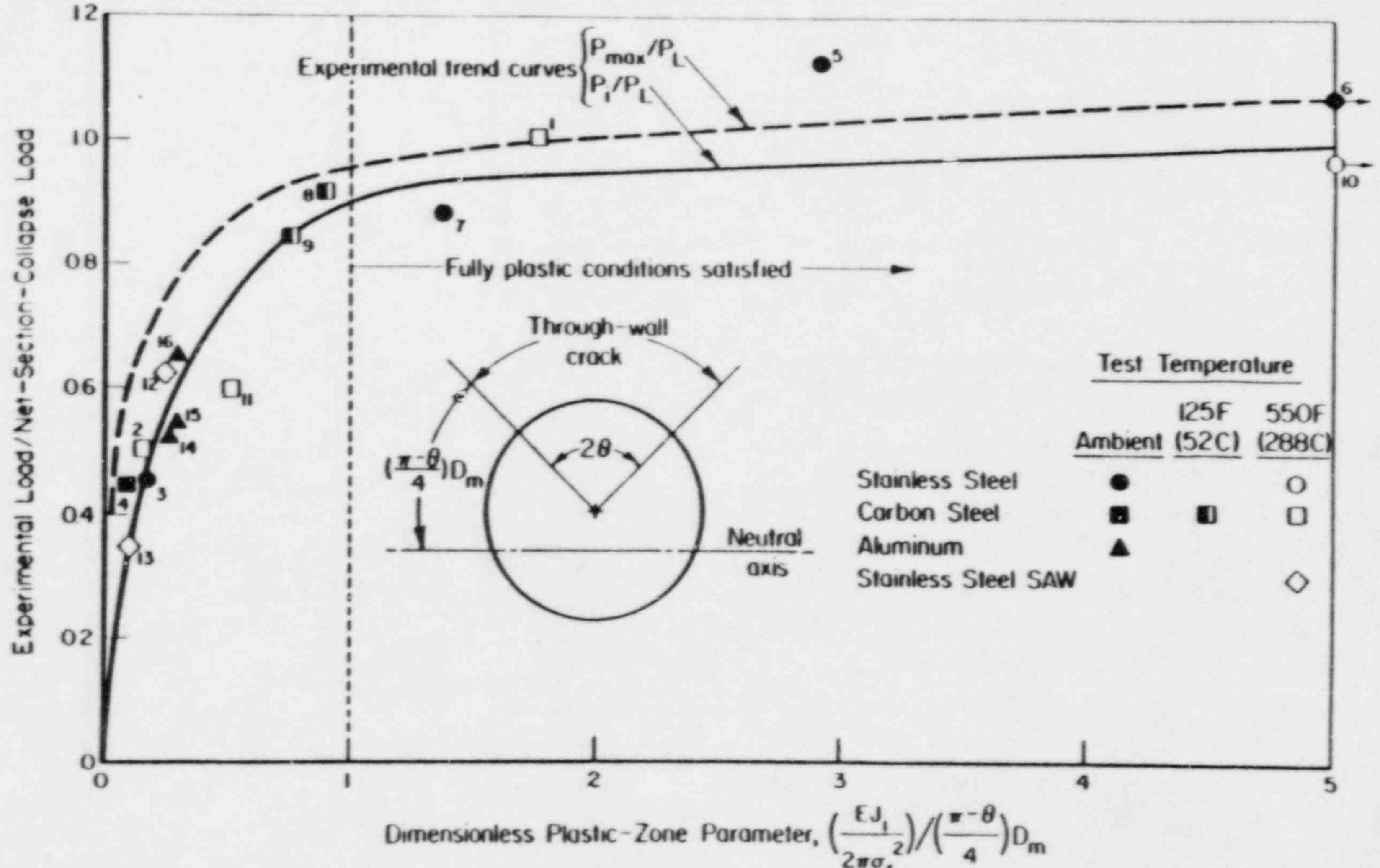

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Technical Issues Sufficiently Addressed

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- IBM-PC J-estimation scheme computer code, NRCPIPE, with improved TWC and SC analyses developed

- Pipe with weld overlay repairs

Estimation scheme methods currently in NRCPIPE

Structure	Analysis Method Available Within NRCPIPE	Loading[(a)] Type
Center-Cracked Panel	GE/EPRI	T
Compact-Tension Specimen	GE/EPRI	T
Single-Edge Notch Specimen	GE/EPRI	T
Bend Specimen	GE/EPRI	B
Circumferential Through-Wall Cracked Pipe	GE/EPRI	T,B,T+B
	Paris	T,B,T+B
	LBB.NRC	T,B,T+B
	LBB.BCL1	T,B,T+B
	LBB.BCL2	T,B,T+B
	Modified GE/EPRI	T,B,T+B
	CEGB.Rev. 3	T,B,T+B
Complex-Cracked Pipe	GE/EPRI	T,B,T+B
	Paris	T,B,T+B
	LBB.NRC	T,B,T+B
	LBB.BCL1	T,B,T+B
	LBB.BCL2	T,B,T+B
	Modified GE/EPRI	T,B,T+B
	CEGB.Rev. 3	T,B,T+B
Surface-Cracked Pipe	SC.SEN	B
	SC.Thin	B
	SC.Thick	B
	CEGB. Rev. 3	T,B,T+B

(a) T = Tension, B = Bending, T+B = Tension + Bending.

Ramberg-Osgood Fit of Tensile Data

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Original Ramberg-Osgood Relation

$$\varepsilon = \frac{\sigma}{E} + \frac{\sigma^n}{F}$$

Normalized Ramberg-Osgood Relation

$$\frac{\varepsilon}{\varepsilon_o} = \frac{\sigma}{\sigma_o} + \alpha \left(\frac{\sigma}{\sigma_o}\right)^n$$

Fitting the Normalized Ramberg-Osgood Relation Requires

$$\alpha = \sigma_o^n/(\varepsilon_o F)$$

$$E = \sigma_o/\varepsilon_o$$

Uniqueness of J-Estimation Schemes

Battelle

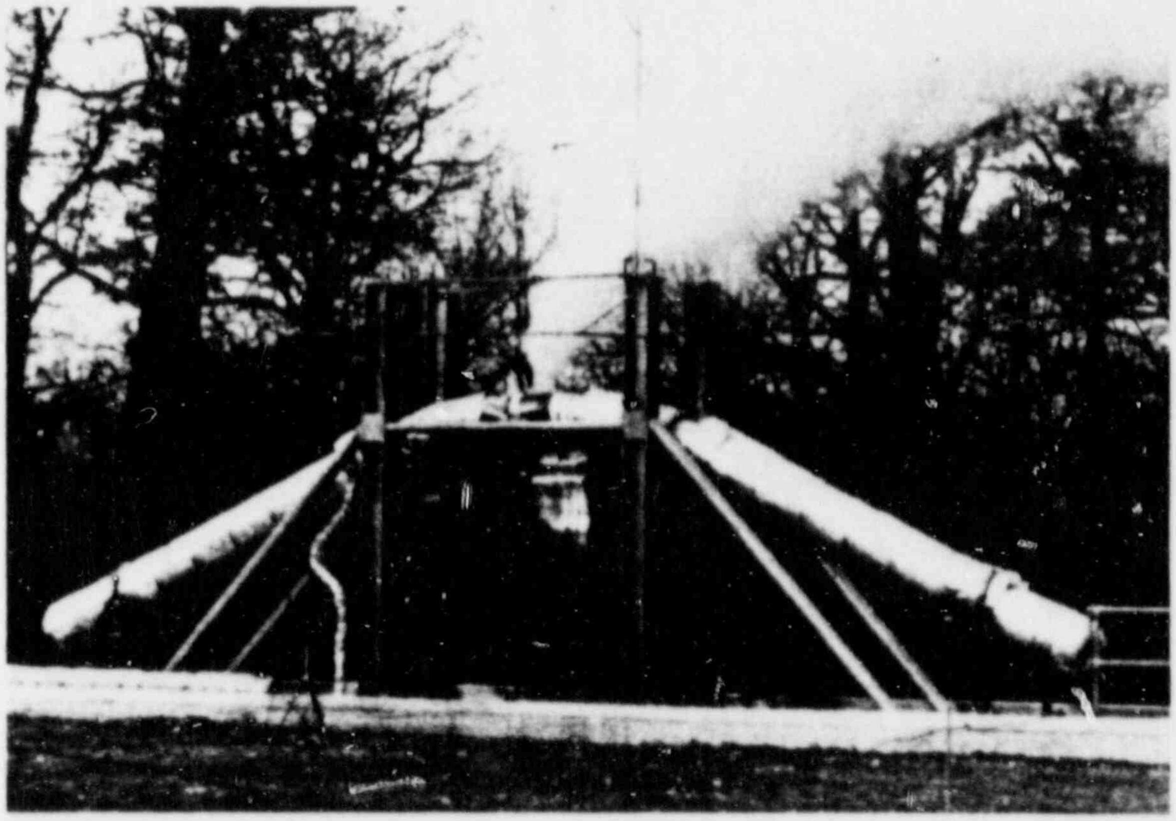
Method	Calculated Initiation Load, lb (kN)$^{(a)}$		Uniqueness ?
	Case 1$^{(b)}$	Case 2$^{(c)}$	
GE/EPRI	34,540 (153.7)	34,540 (153.7)	Yes
LBB.NRC	44,410 (197.6)	44,410 (197.6)	Yes
LBB.BCL1	40,446 (180.0)	40,446 (180.0)	Yes
LBB.BCL2	39,451 (175.6)	39,451 (175.6)	Yes
Modified GE/EPRI	38,902 (172.2)	53,750 (239.2)	No

(a) Circumferential-cracked pipe $2c/\pi D = 0.37$, diameter = 16 inches (406 mm) thickness = 1.03 inch (26.2 mm), J_{Ic} = 11.900 in-lb/in^2 (2.08 MJ/m^2).

(b) $F = 73.740 \times 10^6$, $n = 3.58$, (σ_o, ϵ_o, and α based on yield stress).

(c) $F = 73.740 \times 10^6$, $n = 3.58$, (σ_o, ϵ_o, and α based on flow stress).

16-Inch-Diameter Weld Overlay Repair Test



Comparison of WOR Experiments to IWB-3640

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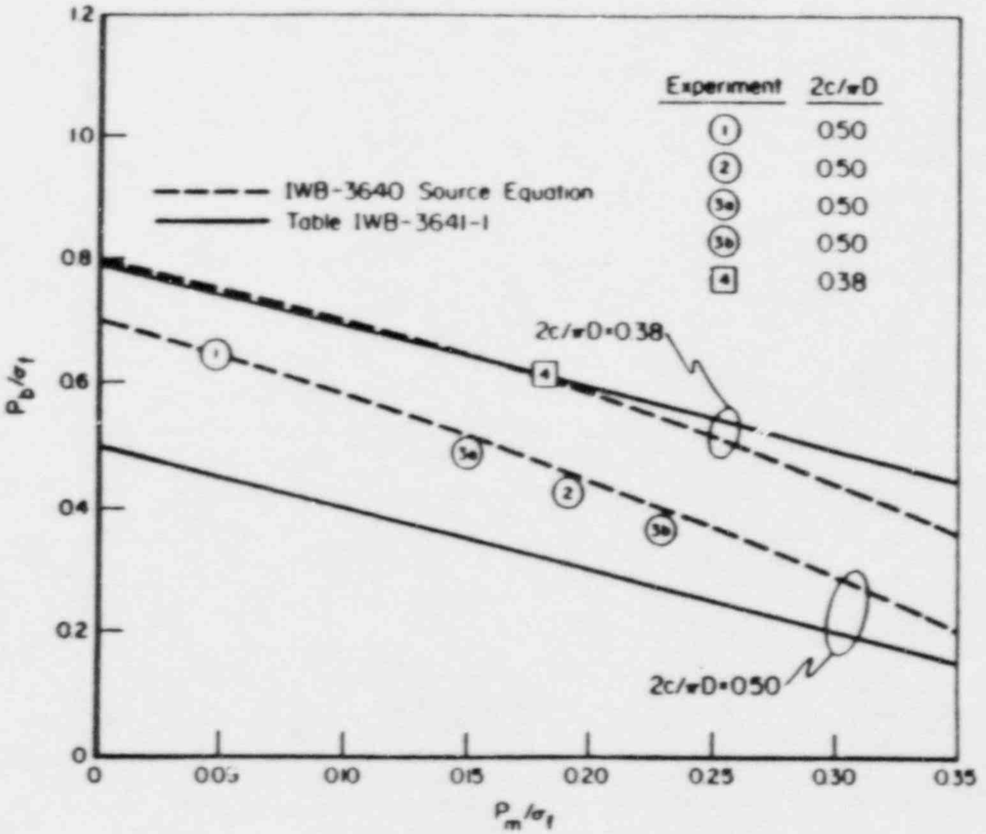

DP-GA13-6/87-GW

Technical Issues Sufficiently Addressed

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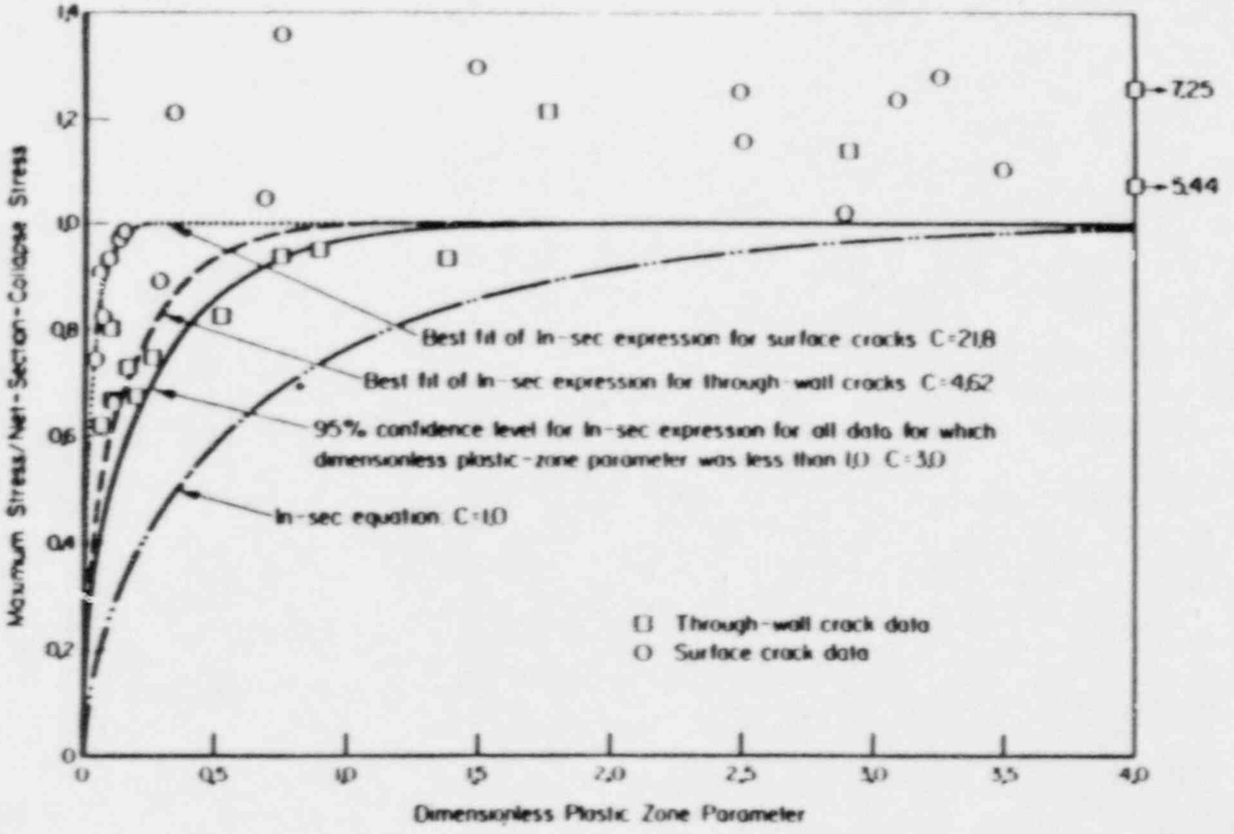
- Statistical analysis to predict maximum load
- Charpy energy and J_{Ic} correlations evaluated
- Round robins on held to verify
  - Tensile testing
  - Calculating J-R curve
  - Finite element analyses
  - J-estimation schemes for SC pipe
  - Design analyses for weld overlay repairs

Statistical Analysis of Pipe Tests

Battelle



DP-GA15-6/87-GW

95-Percent Confidence Analysis

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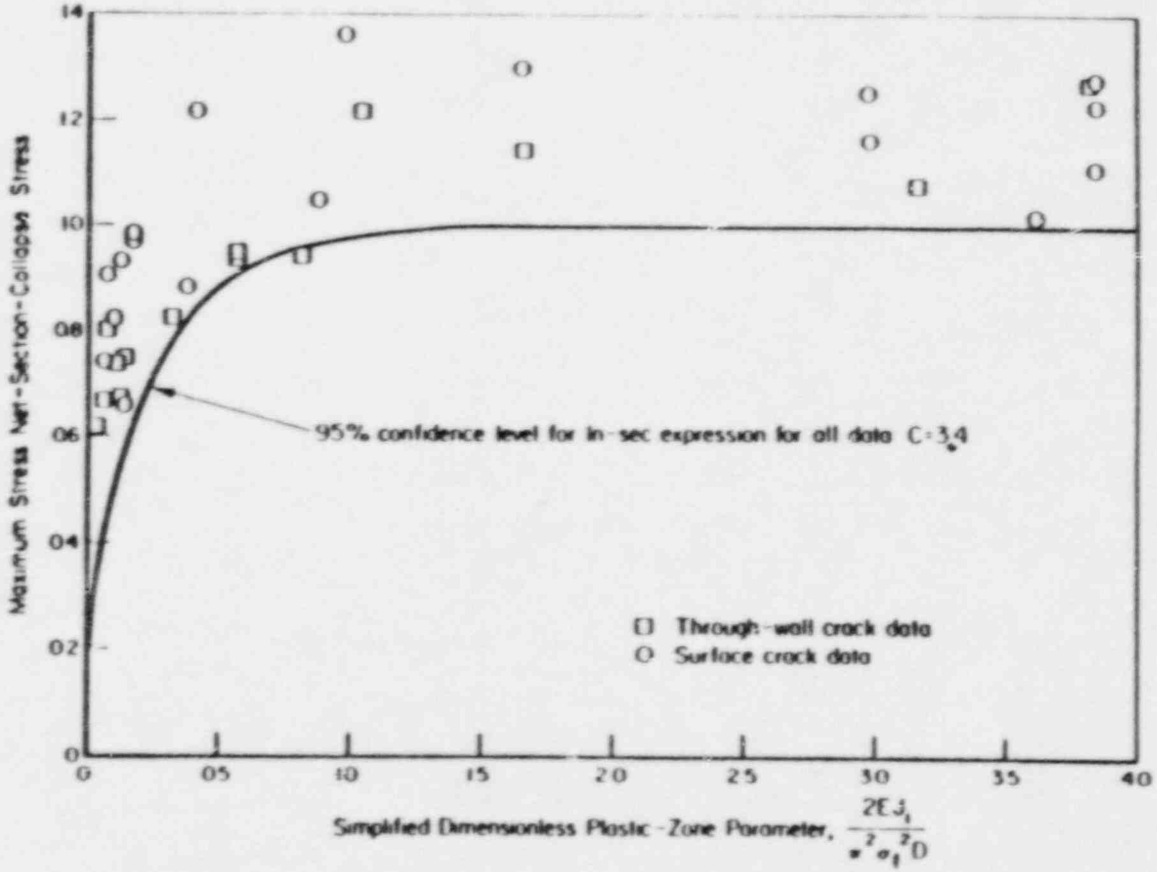

J_{Ic} Versus Charpy Data

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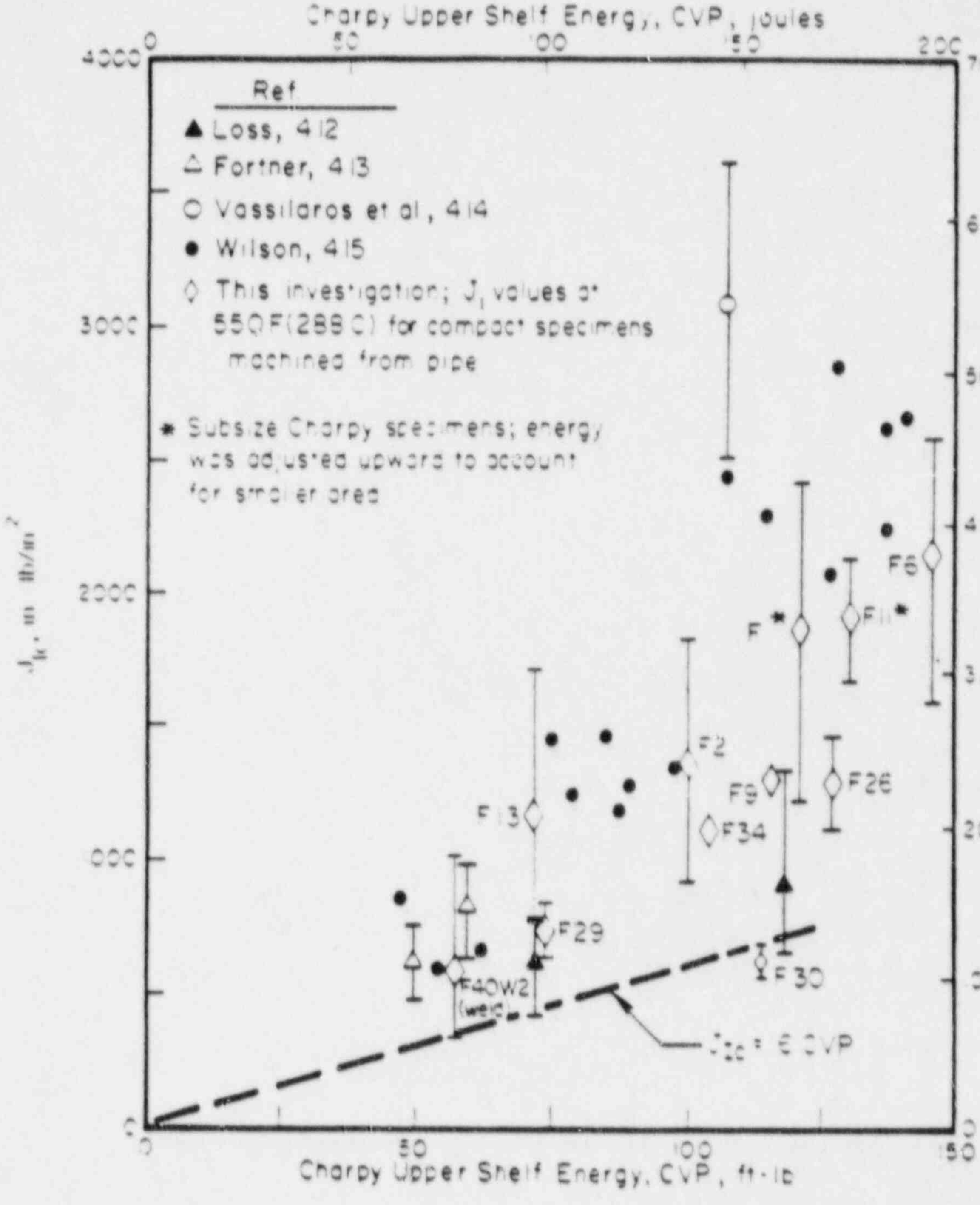

DP-GA17-6/87-GW

Modification of IWB-3640 Using Statistical Analysis

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Example of modification to ASME Table IWB-3641-1 -- Allowable end-of-evaluation period flaw depth[(1)] to thickness ratio for circumferential flaws--normal operating (including upset and test) conditions.

Stress Ratio [Note (2)]	Ratio of Flaw Length, ℓ_f, to Pipe Circumference [Note (3)]					
	0.0	0.1	0.2	0.3	0.4	0.5 or Greater
1.5	(4)	(4)	(4)	(4)	(4)	(4)
1.4	0.75	0.40	0.21	0.15	(4)	(4)
1.3	0.75	0.75	0.39	0.27	0.22	0.19
1.2	0.75	0.75	0.56	0.40	0.32	0.27
1.1	0.75	0.75	0.73	0.51	0.42	0.34
1.0	0.75	0.75	0.75	0.63	0.51	0.41
0.9	0.75	0.75	0.75	0.73	0.59	0.47
0.8	0.75	0.75	0.75	0.75	0.68	0.53
0.7	0.75	0.75	0.75	0.75	0.75	0.58
≤ 0.6	0.75	0.75	0.75	0.75	0.75	0.63

NOTES:

(1) Flaw depth = a, for a surface flaw
$2a$, for a subsurface flaw
t = nominal thickness
Linear interpolation is permissible.

(2) Stress Ratio = $\frac{3(P_m+P_b+P_e/2.77)}{M_c S_{flow}}$

P_m = primary longitudinal membrane stress ($P_m \leq 0.5\ S_m$)
P_b = primary bending stress
S_{flow} = $(S_y + S_u)/2$ from code tables.
M_c = $M_{DZ} M_f M_v$
M_f = $(S_y+S_u)_{actual}/(S_y+S_u)_{code\ tables}$. M_f = 1 if actual S_y and S_u values are unknown.
M_{DZ} = $(2/\pi)$ arc cos $\left[e^{-3.4(DPZP)}\right]$
DPZP = $2EJ_i/(\pi^2 S_{flow}^2 D)$
E = elastic modulus
J_i = J at crack initiation if known, otherwise use Charpy energy correlation if Charpy data are available, or use lower bound values if no data are available.
M_v = $1 + [0.25 - 0.032(R/t)][P_b/(P_m + P_b)]$.

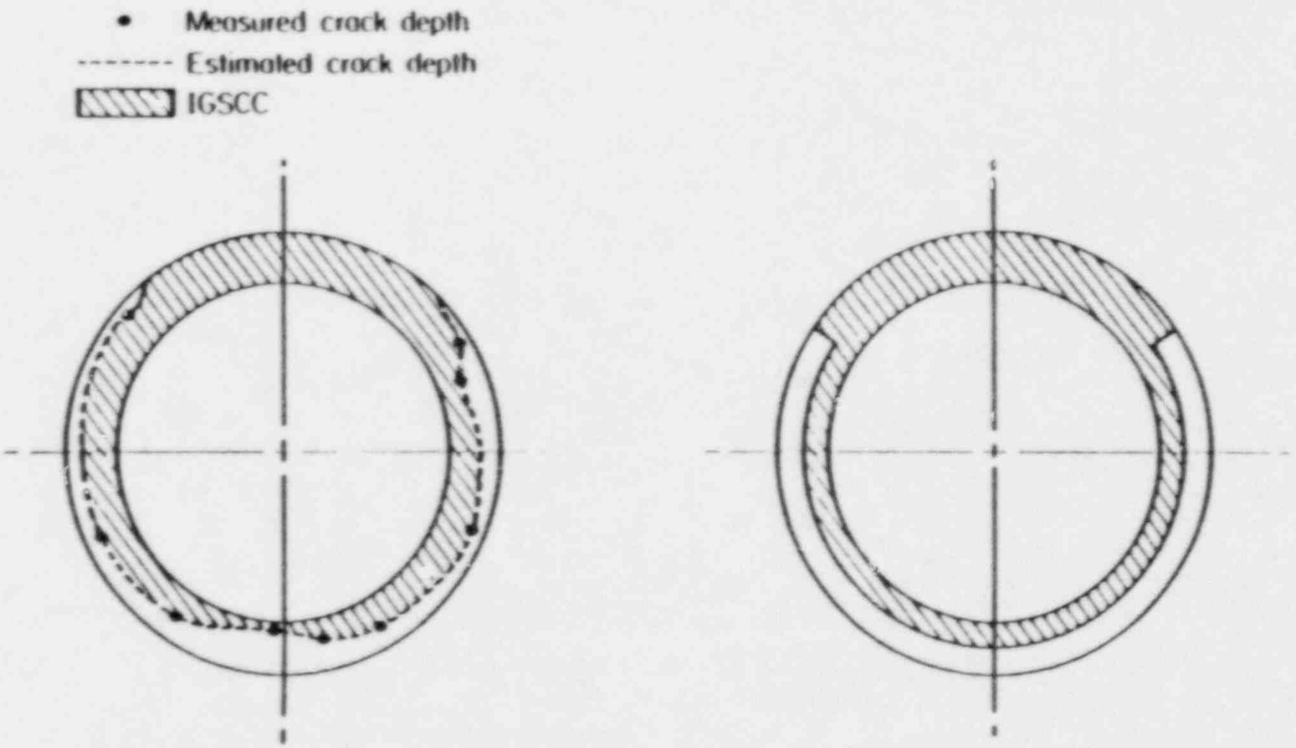
(3) Circumference based on nominal pipe diameter.
(4) IWB-3u14.3 shall be used.

Technical Aspect Addressed in DP^3II

Battelle

- Complex crack geometry (TWC with long internal SC) significantly reduces apparent fracture resistance
    - Empirical correction developed
    - Shallower d/t cracks should be evaluated

Simulated Complex-Cracked Pipe Experiments

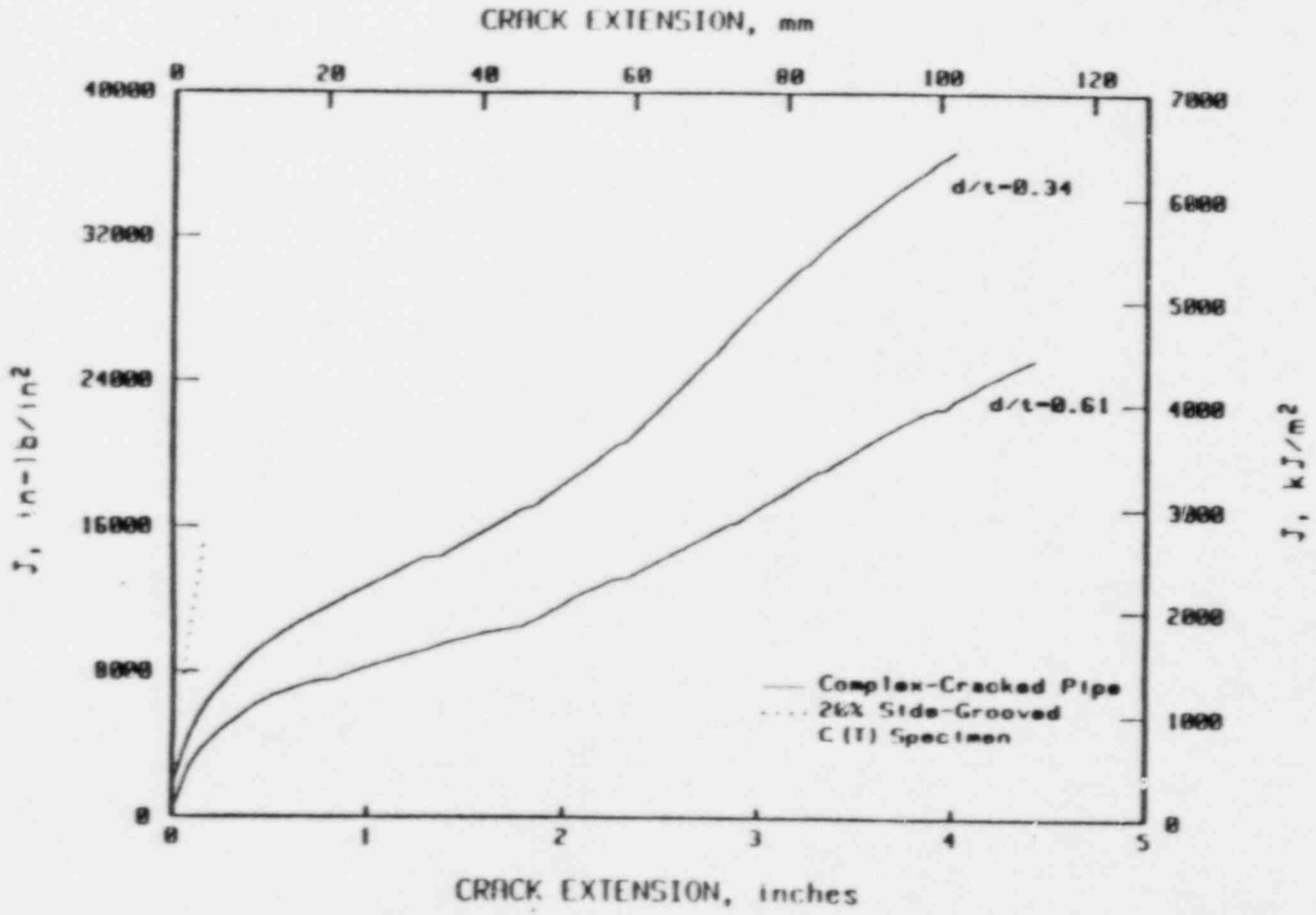


Duane-Arnold Crack Found in Service

Simulated Complex Crack

Comparison of Fracture Toughness Curves

Battelle



Effect of d/t on Complex Cracked Pipe J-R Curve

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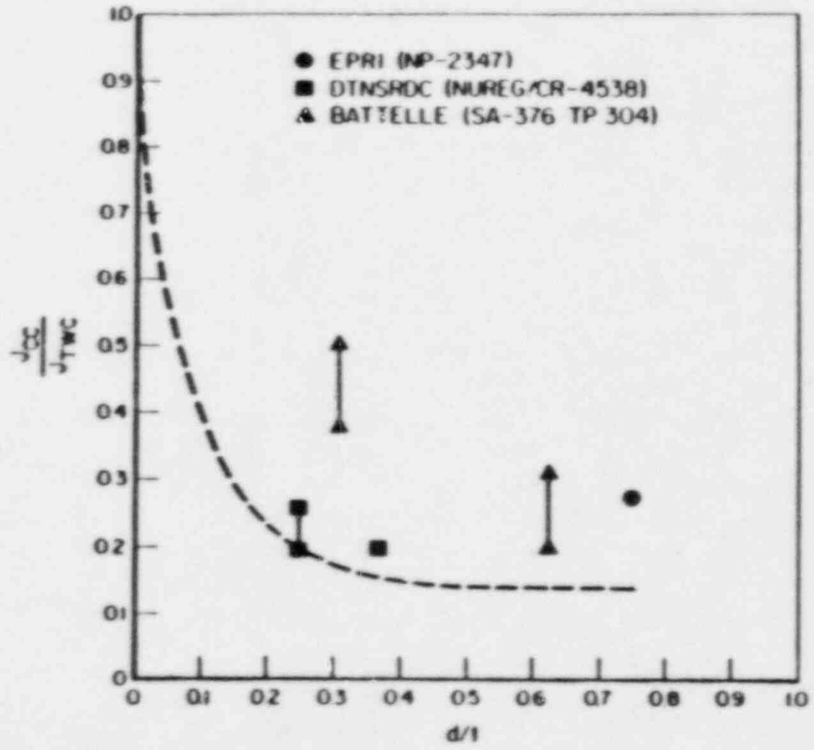

Technical Approach Developed in DP^{3}II

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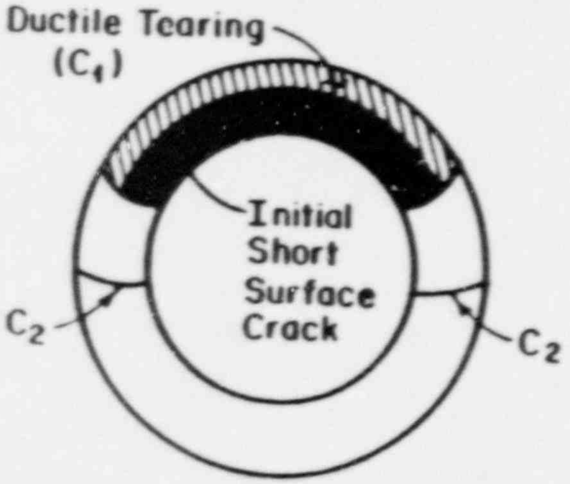
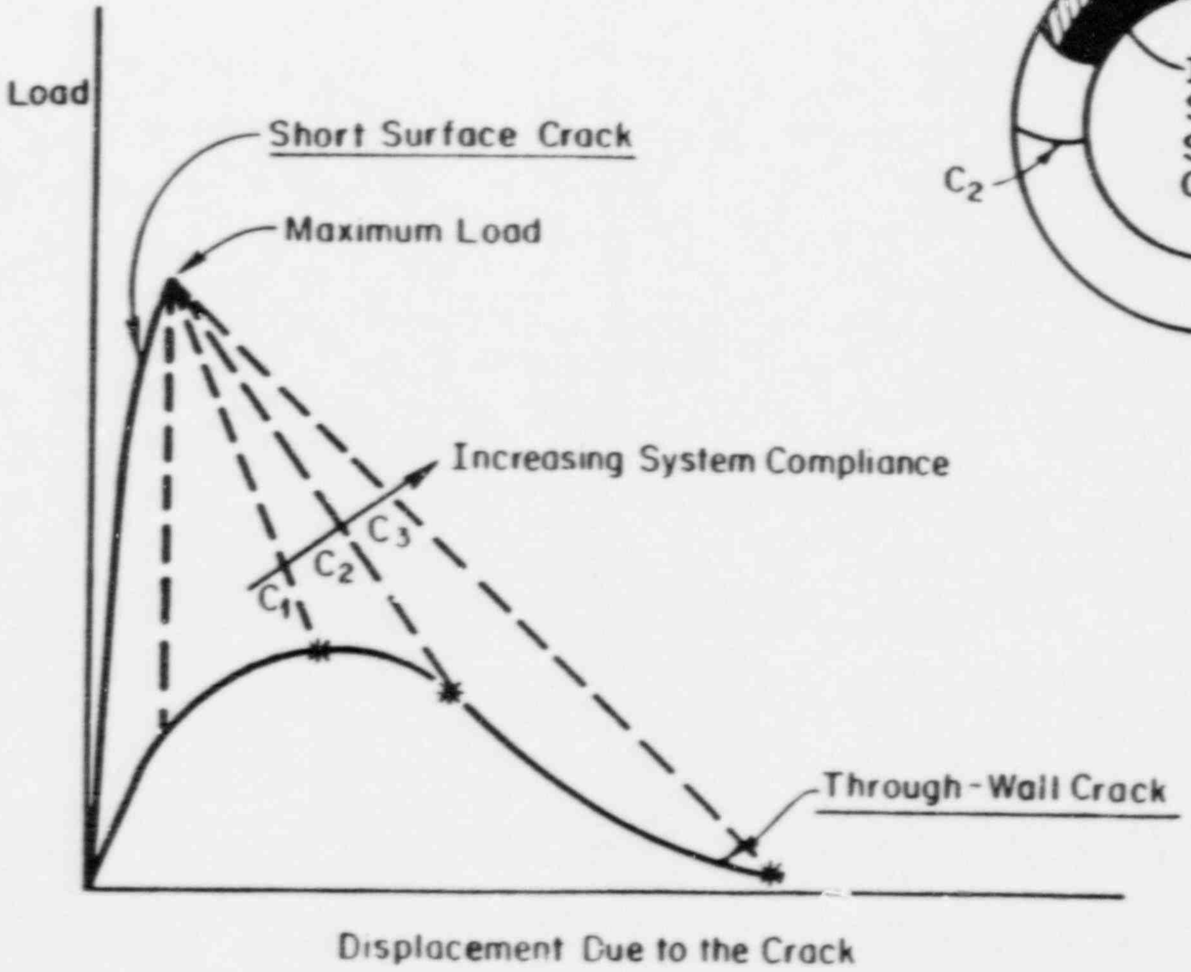
- Concepts involved in developing an energy balance approach verified to predict start of instability and arrest.

Stability Analysis of Short Surface Crack

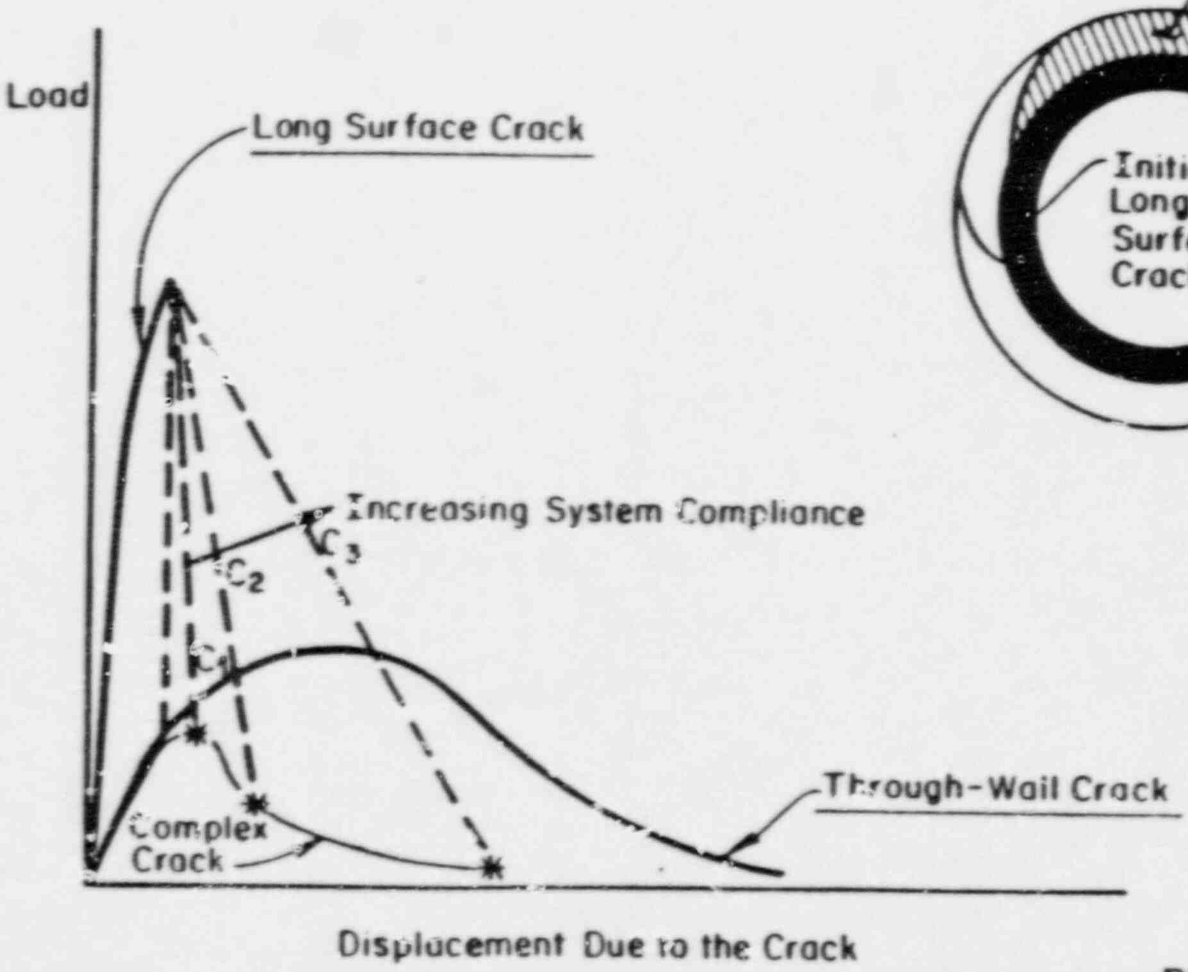
Battelle

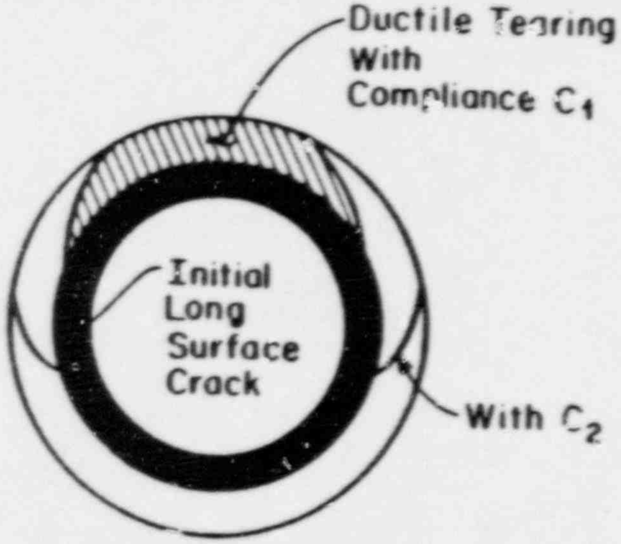




Stability Analysis of Long-Surface Crack

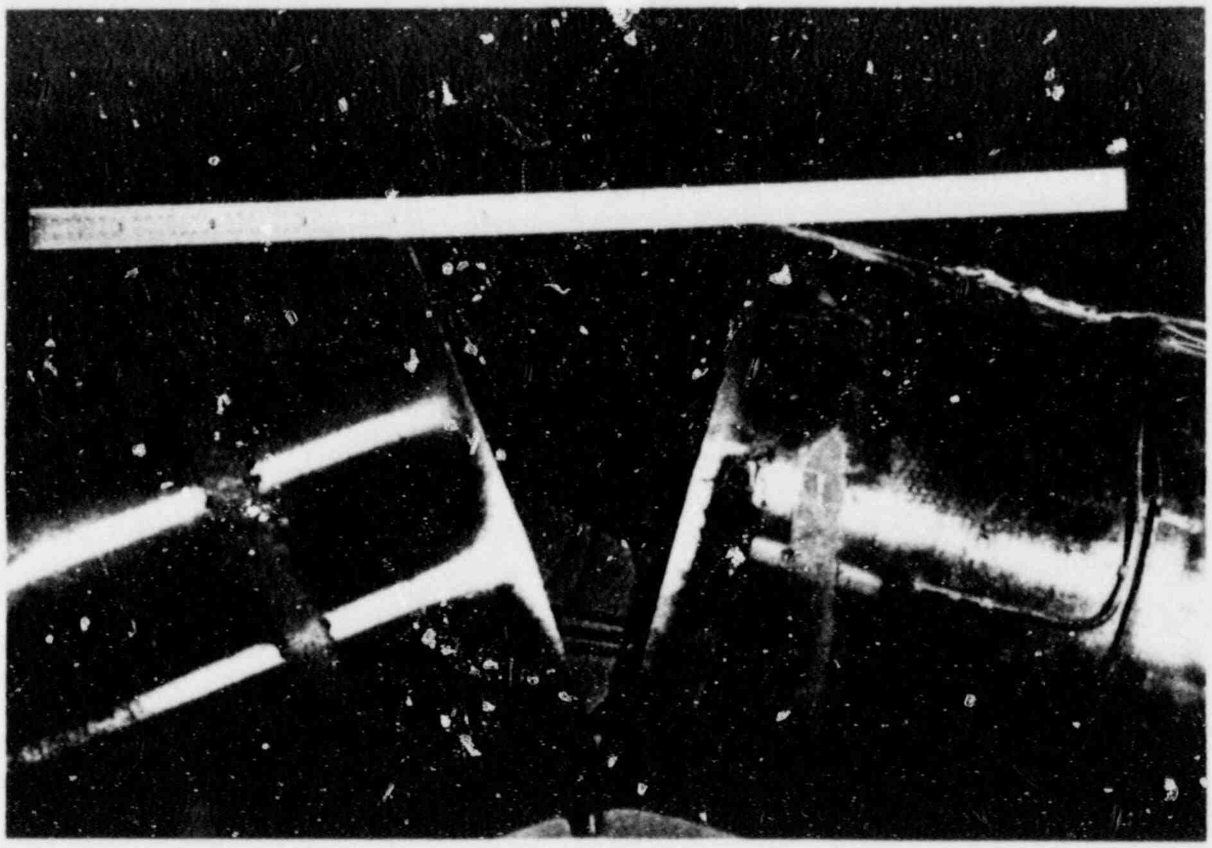
Battelle





DP-GA34-6/87-GW

Compliant SC Pipe Test

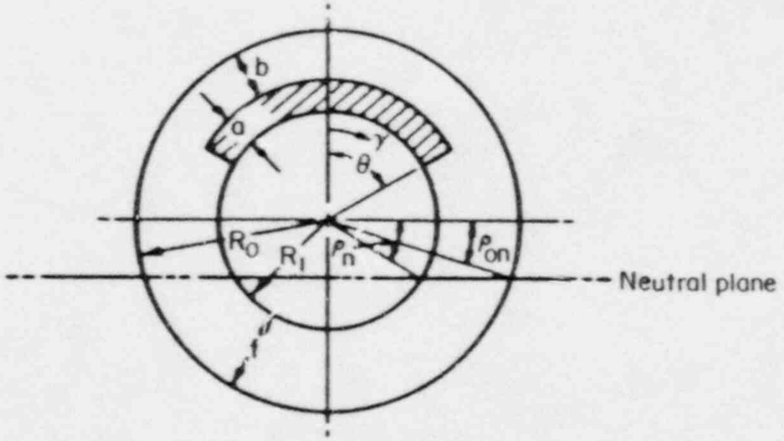


Technical Approach Developed in DP^{3}II

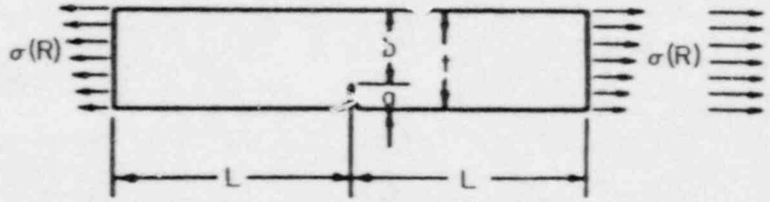
Battelle

- Estimation schemes for surface-cracked pipe developed.
  - Thick-wall solution most consistent
  - Correction to ASME Z-factor approach
  - Further sensitivity studies needed.

(a) Surface-cracked pipe under pure bending

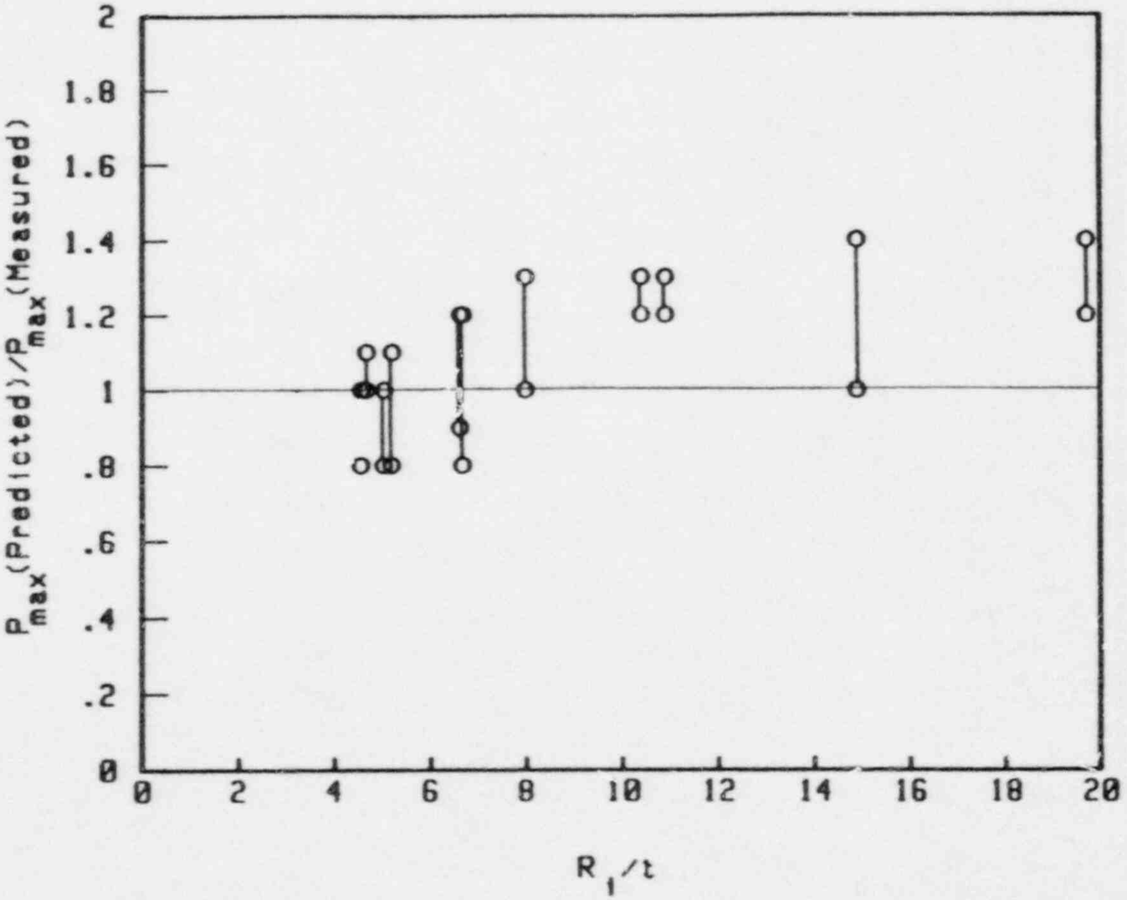


(b) Cross-section containing internal surface crack



(c) Plane at $\gamma < \theta$, L = ct

SC.TNP Predictions of Experimental Data



SC.TKP Predictions of Experimental Data

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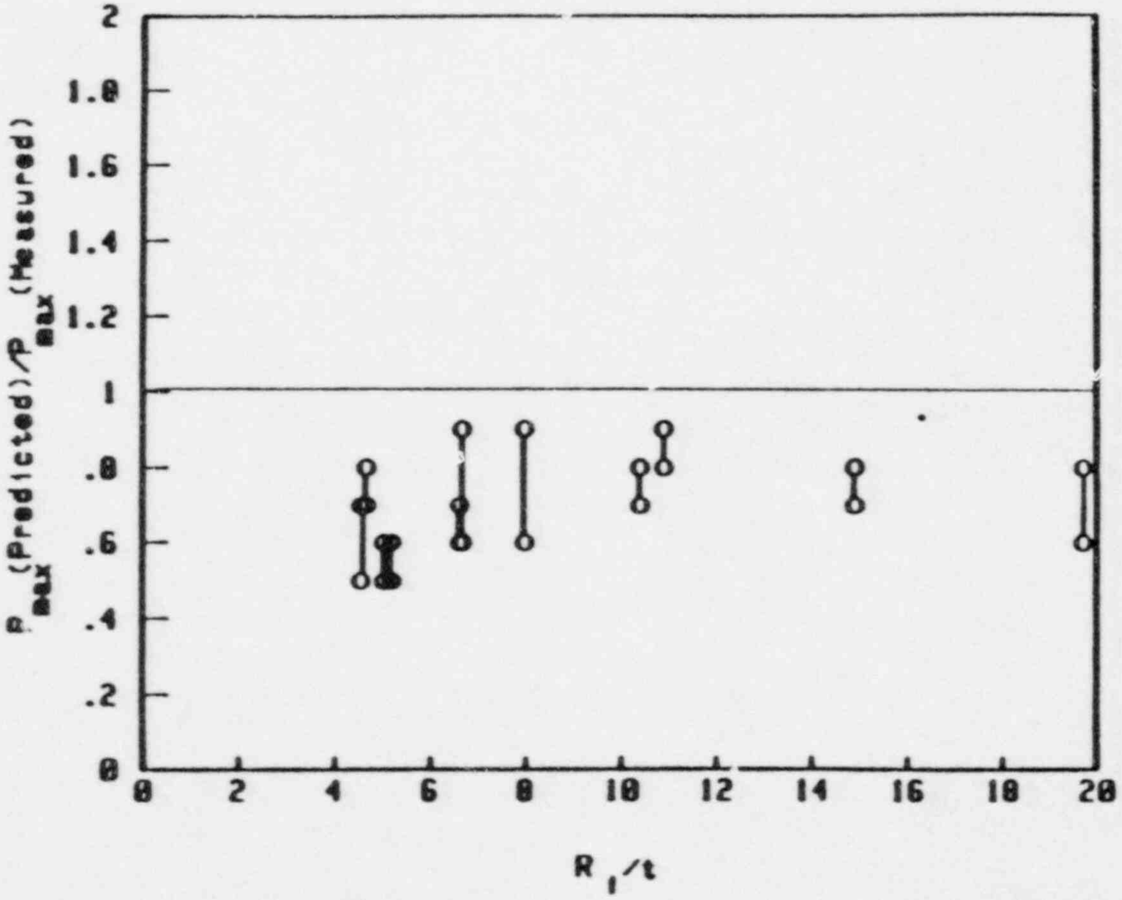
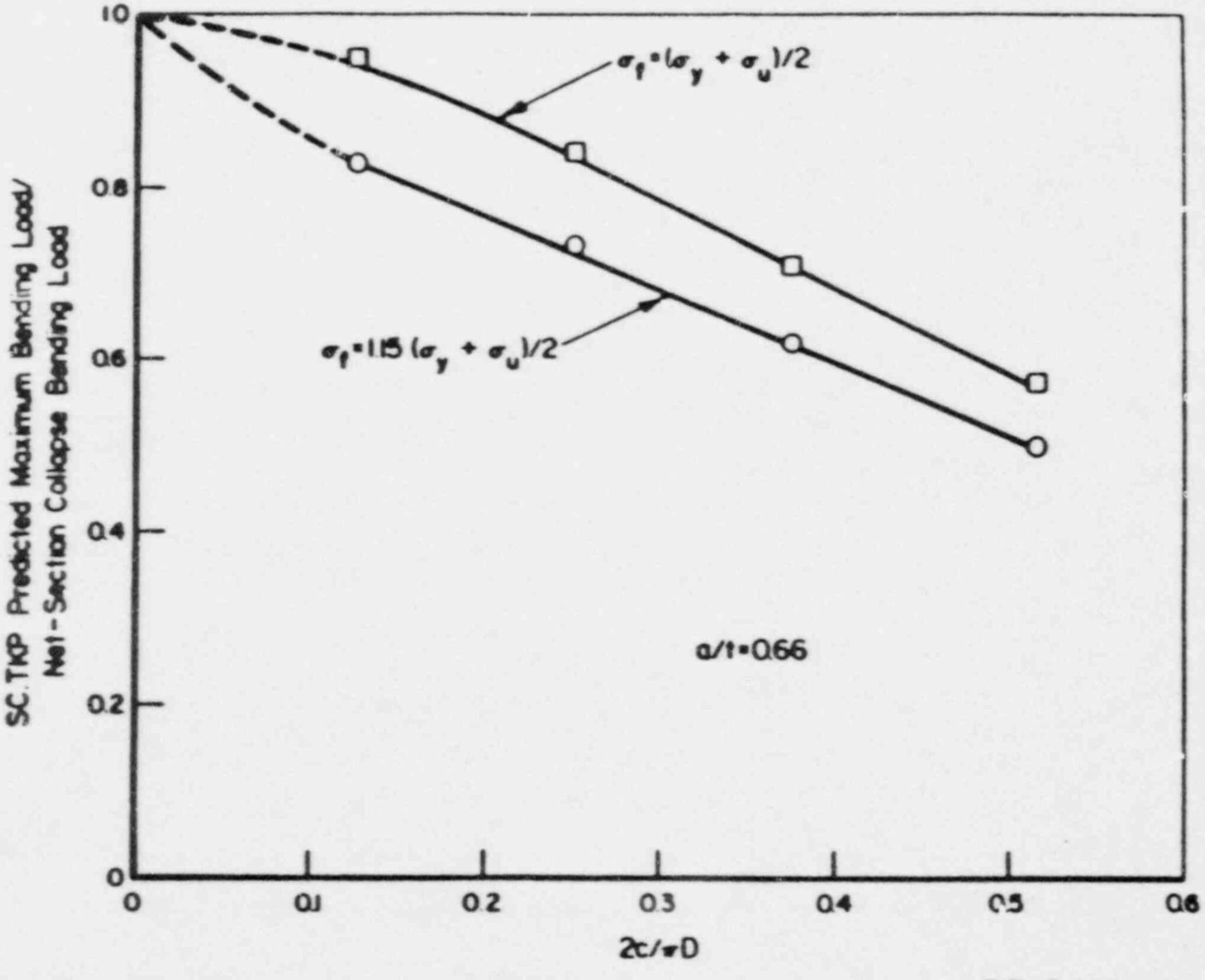


Prediction of Crack Length Effect For Pure Bending

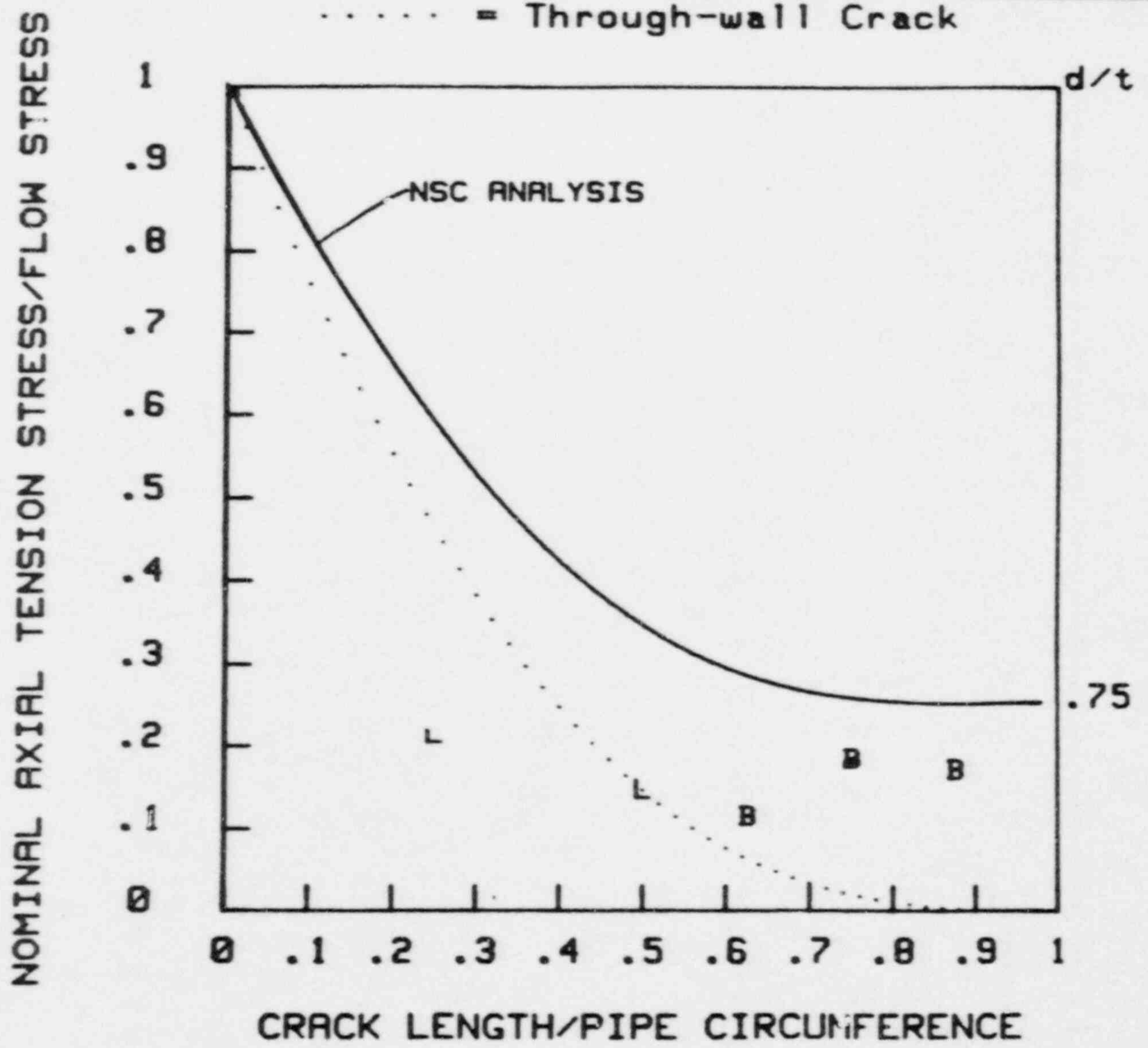
Battelle



DP-GA37-6/87-GW

Effect of Crack Length on Failure of Pressurized Pipe

Battelle



Technical Aspects Arising From DP^{3}II

Battelle

- Need more pipe fracture data for carbon steel weld and thermal-aged centrifugally cast stainless steel

- Carbon steels at 550 F exhibit dynamic crack jumps, possibly due to dynamic strain-aging

Dynamic Crack Jumps in C(T) Test at 550 F

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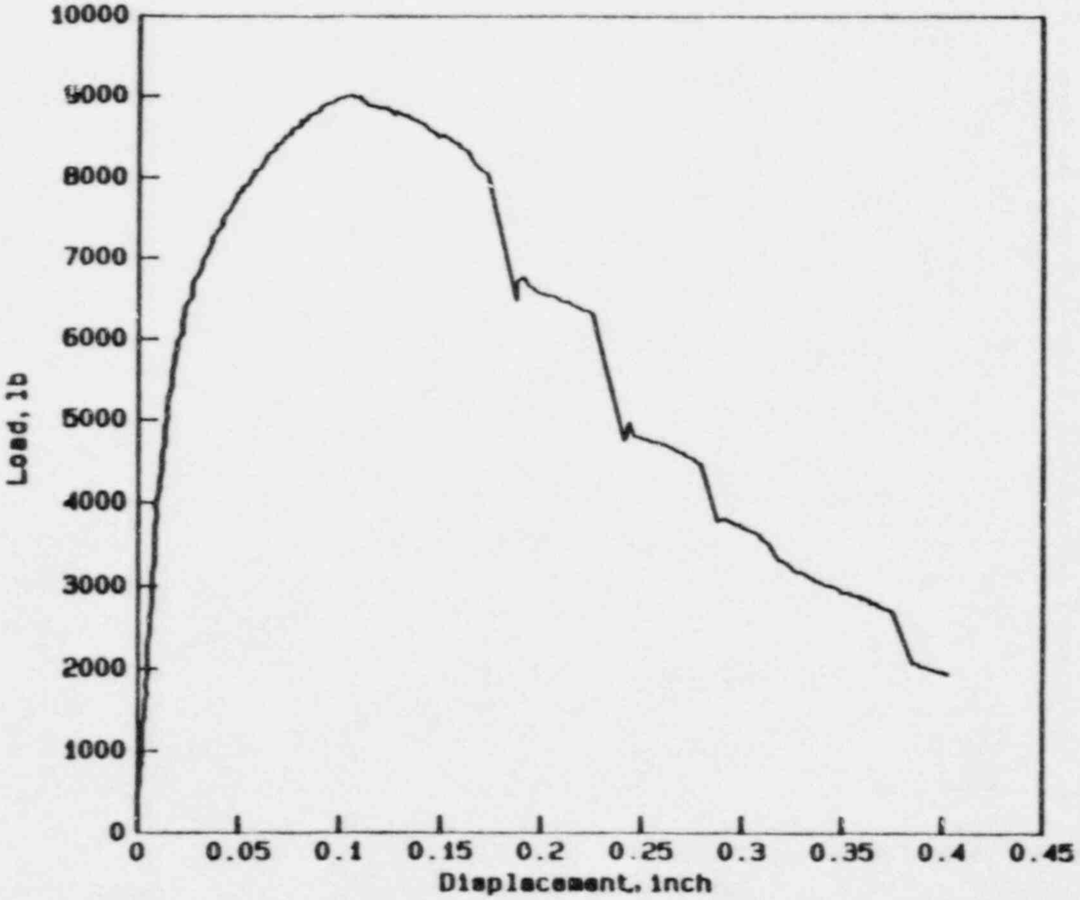
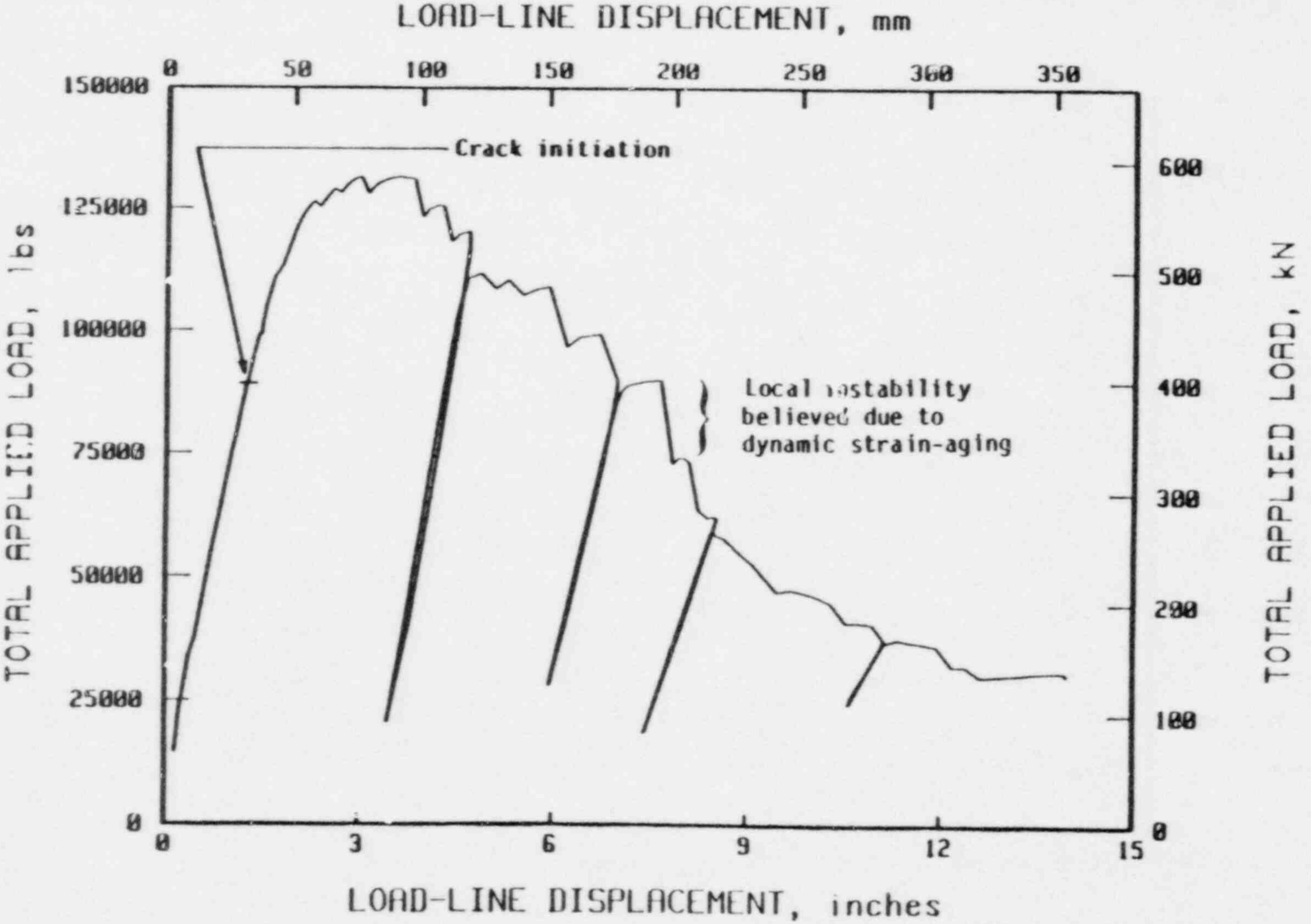


DP-GA24-6/87-GW

Dynamic Crack Jumps in 28"-Diameter TWC Pipe

Battelle



Crack initiation

Local instability believed due to dynamic strain-aging

DP-GA25-6/87-GW

Dynamic Crack Jump in Carbon Steel Weld

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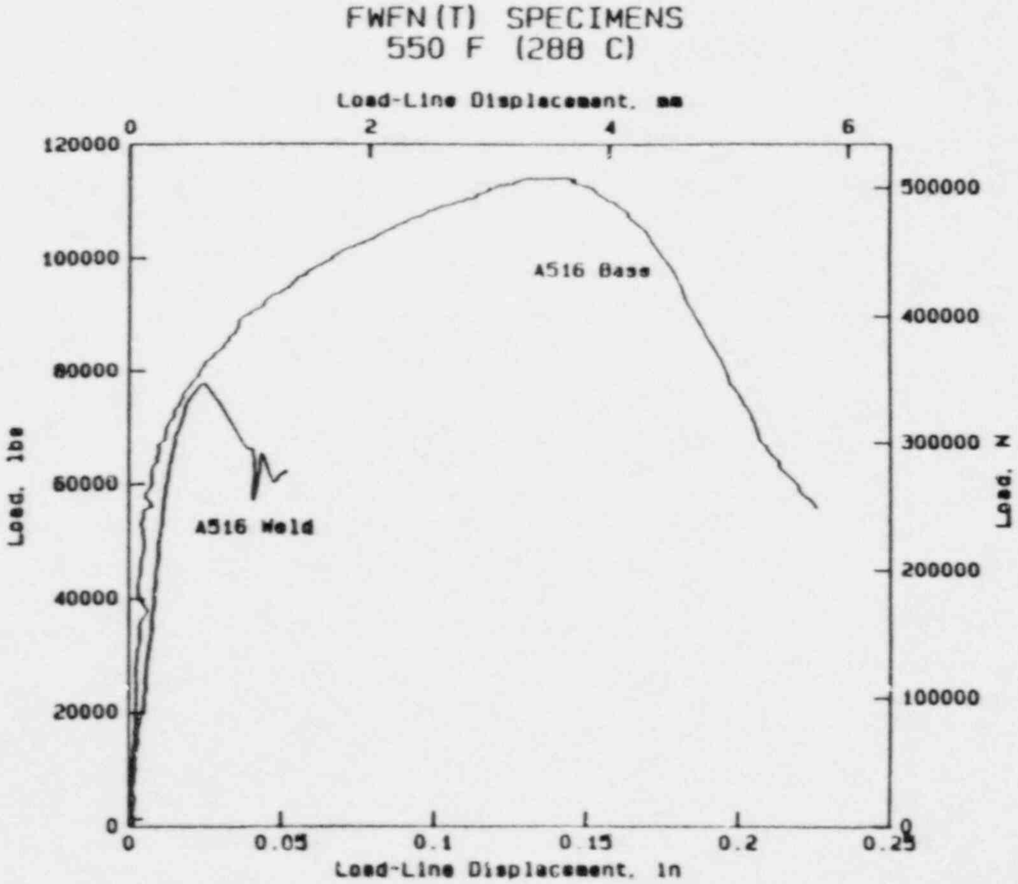
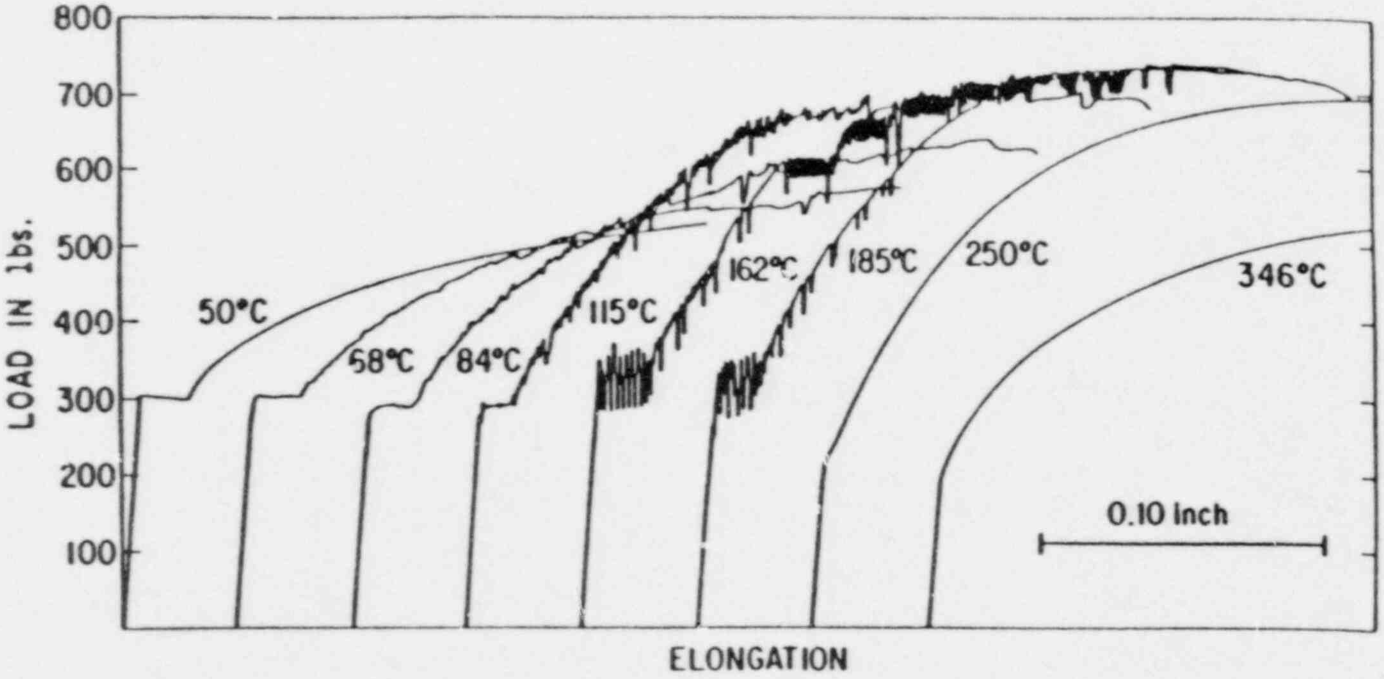


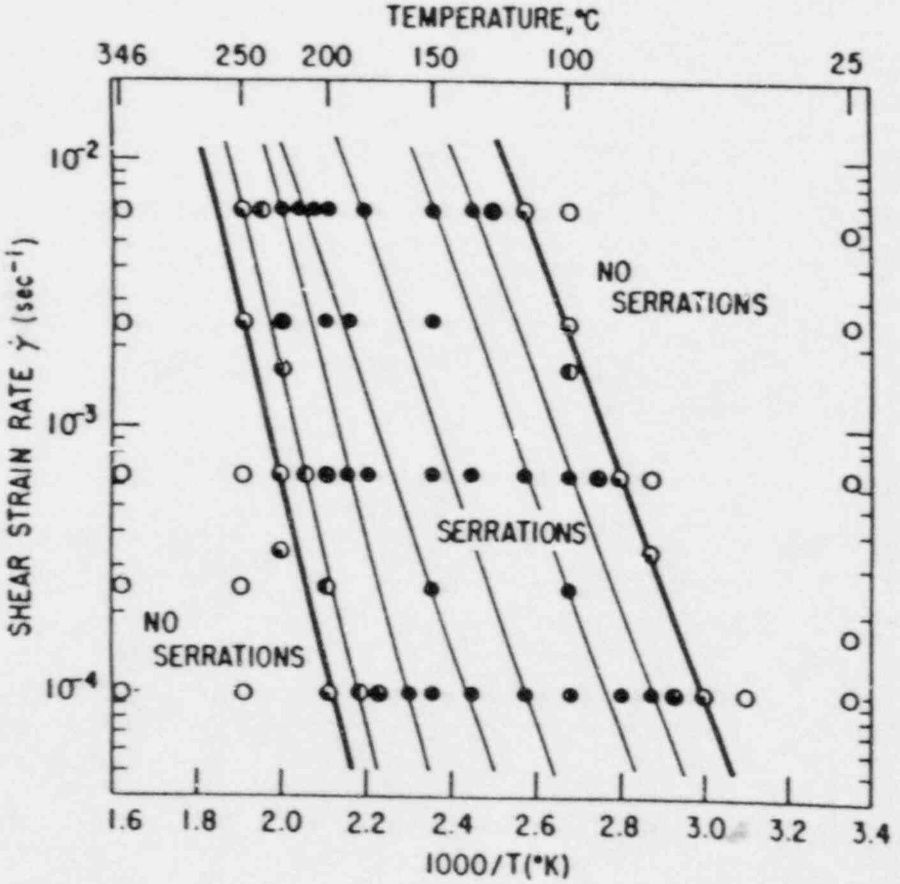
Dynamic Strain-Aging in Tensile Test Specimens

Battelle



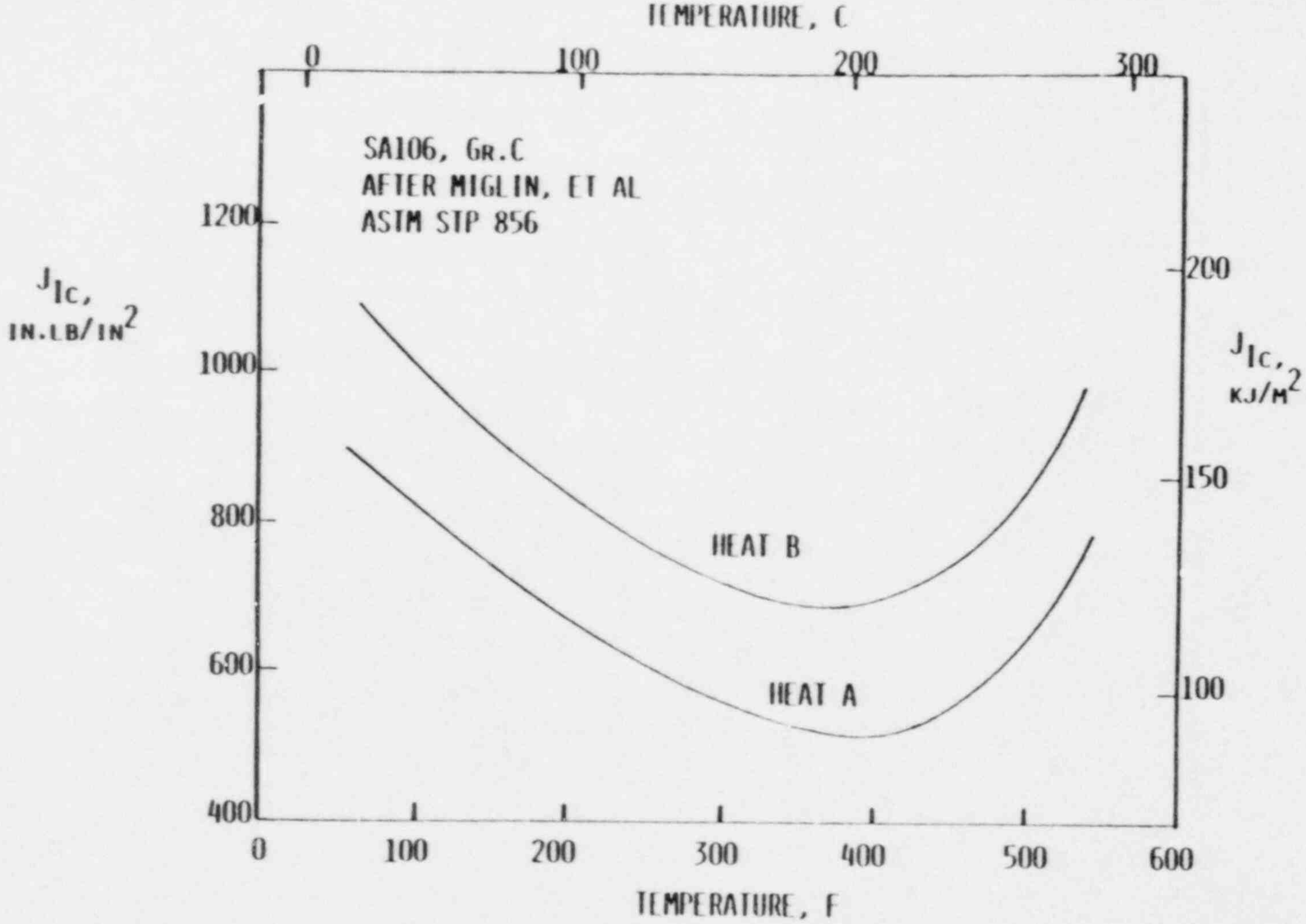
Temp. and Strain-Rate Effects on Strain-Aging

Battelle



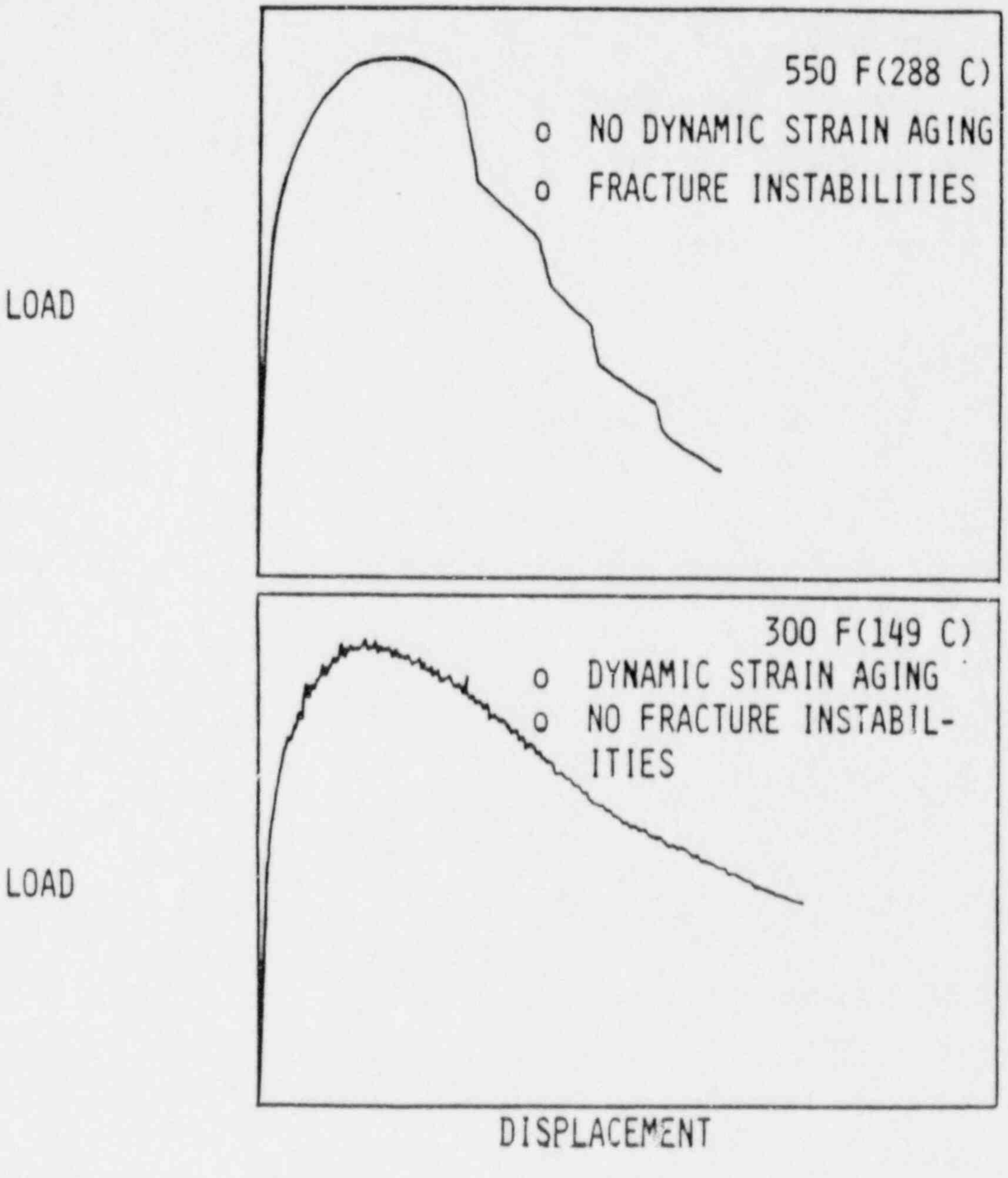
Dynamic Strain-Aging Effect on Crack Initiation

Battelle



Load-Displacement Records From Carbon Steel C(T)

Battelle



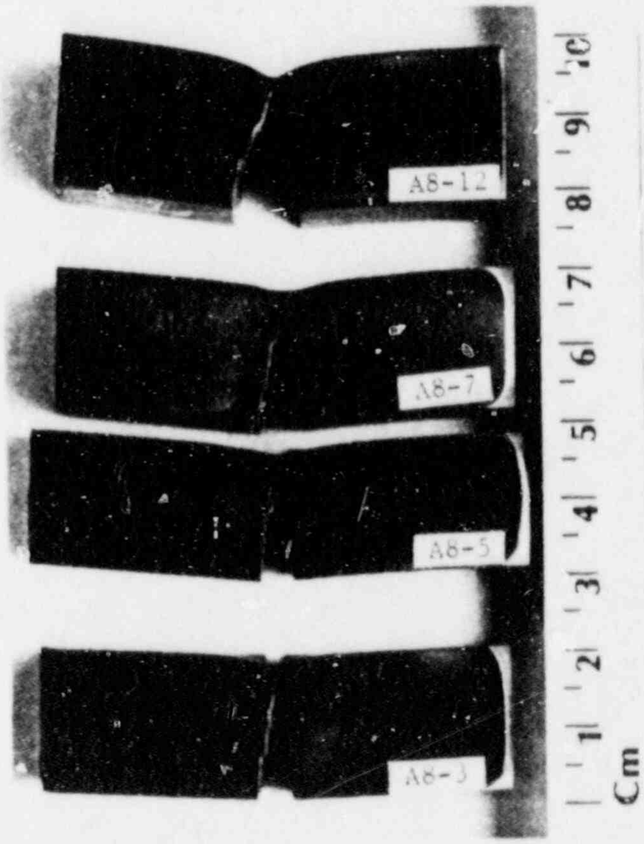
DP-GA30-6/87-GW

Technical Aspects Arising From DP^{3}II

Battelle

- Limited data show crack growth resistance along fusion line of a stainless steel flux weld found to be approximately half of SAW toughness

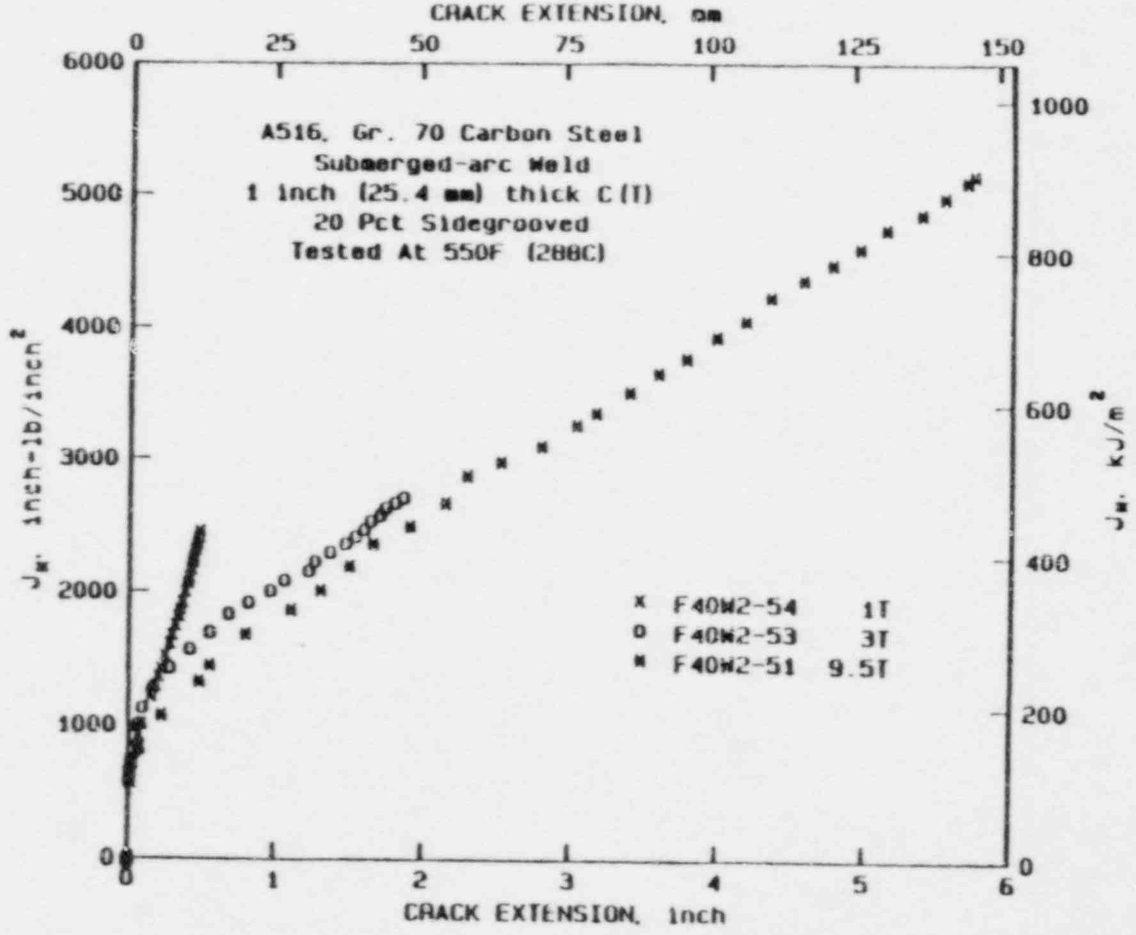
Lower CTOA in Stainless Fusion Line



Aspects Arising From Degraded Piping Program

Battelle

- Modified J (J_M) evaluated extensively
  - Small C(T) specimens give lower J_{Ic}, larger C(T) specimens give lower dJ/da
  - Fundamental concern on J_M continually increases with large crack growth. Steady-state toughness not predicted as observed with CTOA, J, T_p



DP-GA39-6/87-GW

Technical Aspects Arising From DP^{3}II

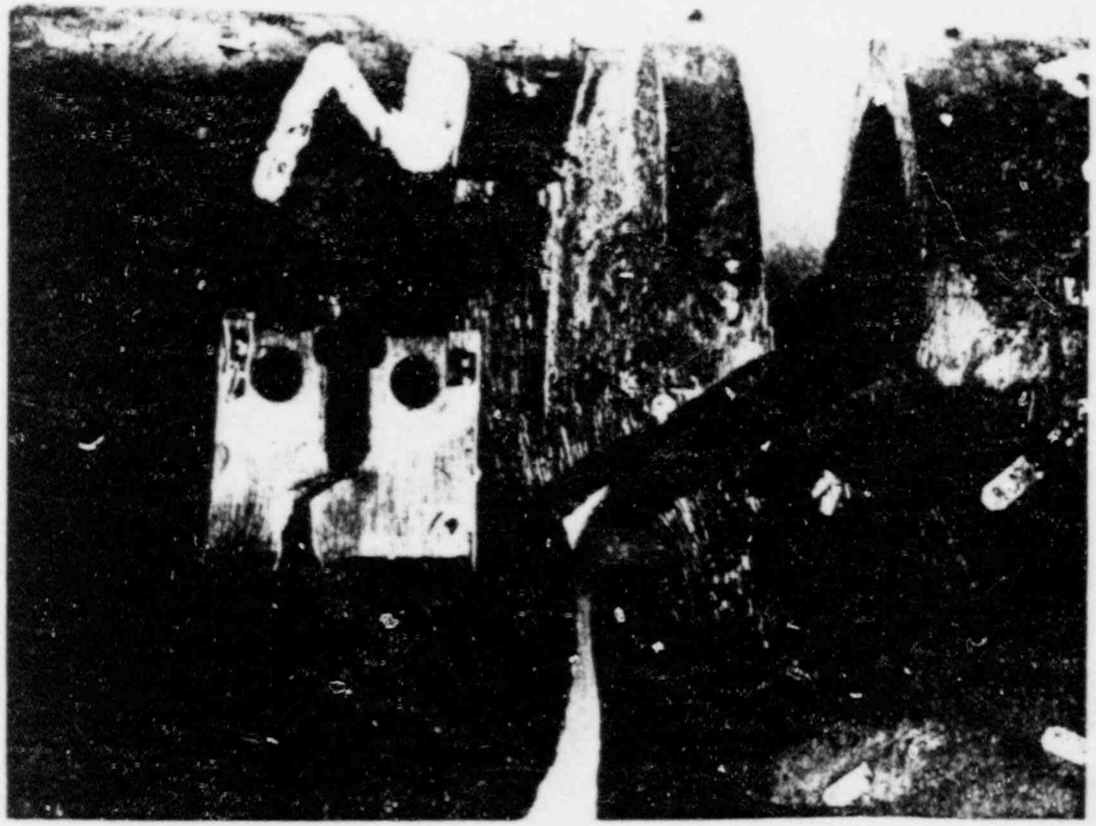
Battelle

- Anisotropy significantly effect direction of ductile crack growth in carbon steels

- Material property data base need to be expanded for carbon steel welds and fusion line

Anisotropy Effect in Carbon Steel Pipe

Battelle



**Staatliche
Materialprüfungsanstalt**

Universität Stuttgart

RECENT RESULTS AND FUTURE PROGRAMMES OF PIPE FRACTURE TESTS IN MPA STUTTGART

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ABSTRACT

The German Reactor Safety Research and Development Programme of the Federal Ministry for Research and Technology (BMFT) started in the early seventies with investigations on the phenomenological vessel burst behaviour. The aims of these experiments on components made of mostly ferritic materials were focussed on the generic issue of leak-before-break of piping and pressure vessels. It was the aim of those investigations to research the fracture as well as the crack opening process but also the crack propagation and crack arrest. The tests were conducted under actual conditions with respect to internal pressure, temperature and pressure medium, which was decisive for the transferability of the test results on actual components.

The investigations on vessels and pipes into which have been introduced longitudinal surface- and through-wall flaws were extended by circumferential flaws and an additional outer bending moment which was superposed to the internal pressure. In various phases of the programme the load history was steadily varied up to superimposed cyclic and dynamic bending load, initiating from static internal load up to failure, again under operational conditions with respect to internal pressure, temperature and pressure medium. To round the investigations on straight pipe sections off piping components as i. e. elbows and whole piping sections were included. Investigations on surface and through-wall flaws partly produced through fatigue and corrosion cracking will be performed in the future.

Parallel to these efforts German nuclear industry prepared their specific leak-before-break approach. Within the scope of that Research and Development work smaller diameters than for the Phenomenological Vessel Burst Experiments were used and also other materials including austenitic stainless steel.

Validation and verification work of existing fracture mechanics approaches and engineering-type calculational methods were performed on the basis of full scale testing under actual operational conditions. This includes also pre- and post experimental analyses which have been done by means of finite element modelling of fluid and structure interaction during crack initiation, crack propagation and crack arrest.

If the obtained results are transferred to actual components, which may be done without any restrictions because of the chosen test dimensions and conditions, the effects of the vessel burst experiments on the fracture hypothesis of primary piping systems can be comprised as follows considering especially the concept basis safety:

- The basis safety excludes catastrophic failure of the components of pressure boundaries for proper service and for upsets.
- The validity of the criterion leak-before-break is acknowledged for the whole range of upper shelf Charpy impact energy from 50 up to > 100 J.
- On the conditions given by basis safety, owing to conservative limitation of the stresses and increase of the toughness requirements, critical crack sizes can be excluded. A leakage owing to a crack can only arise in form of a small, locally limited crack ($\ll 0.1\ F_R$).

It could be demonstrated that for adequate system design the postulate of longitudinal large breaks (spontaneous catastrophic failure) is not justified. Failure probabilities are so low, as to be meaningless. This conclusion is based on the use of high toughness materials not susceptible to degradation during manufacture, processing and operation. Pre-service and in-service quality assurance programmes, load and leakage as well as water chemistry monitoring systems provide the necessary redundancies for the LBB-concept.

1. Introduction

In 1977 a national Research and Development Programme (Phenomenological Vessel Burst Experiments) was launched focussed on the generic issue of leak-before-break (LBB) of piping and pressure vessels. The goal was to investigate the fracture and crack opening process and to develop an unimpeachable deterministic safety concept. This concept allows the possibility of catastrophic failure to be completely excluded, without invoking probabilistic arguments. The Basis Safety Concept was developed in the Federal Republic of Germany to render the probabilistic approach unnecessary for safety cases relating to nuclear power plants, /1/. The process of evaluation started in 1972, and in 1977 the Basis Safety Concept was adopted in principle by the German Reactor Safety Commission. In 1979 it was officially published and thus became a legal requirement.

The Reactor Safety Commission prepared the "RSK Guidelines for Pressurized Water Reactors" as a compilation of the safety-related requirements which, in the Commission's opinion, have to be compiled within the design, construction and operation of a nuclear power plant with pressurized water reactor, /2, 3/. As of October 1981, the Reactor Safety Commission will use the present Guidelines as a basis in its deliberations concerning site and safety concept of pressurized water reactors prior to issuance of a construction permit, and it will measure the construction phases of the corresponding nuclear power plants which do already exist or are under construction or in operation. The scope of application of the Guidelines to these plants will have to be examined on a case-by-case basis.

The major purpose of the Guidelines is a facilitation of the discussion process within the Reactor Safety Commission and an early submission of references to safety-related requirements which the Reactor Safety Commission considers necessary. Should manufacturer and licensee comply with the Guidelines the Reactor Safety Commission will proceed with the short-term issuance of comments on individual projects.

For application of leak-before-break the requirements mentioned in the Reactor Safety Commission Guidelines will provide the pressure-retaining boundary with a basic safety that will preclude any disastrous failure of a plant component as a result of defects, **Fig. 1.** The required inherent safety of the components and systems of the external system shall be assured on the basis of the General Specification Basic Safety.

2. Experimental and Analytical Research Programmes

2.1 Component Tests

Completing a status report on "Research in the Field of Nuclear Pressure Vessels" /4/ in 1970 generated an important requirement for a German Reactor Safety Programme, which started by the Federal Ministry for Education and Sience then, is now carried on and sponsored by the Federal Ministry for Research and Technology (BMFT). Part of this reactor safety programme deals with the pressure boundary of nuclear power plants, so for example with the primary pressure vessels and piping systems. The most important piping research programmes which were sponsored by the Federal Ministry for Research and Technology are listed in **Fig. 2.**

The main goal of the experimental research programmes is to investigate the phenomenology of fracture on cylindrical vessels which partly have the dimensions of the primary piping system of pressurized water reactors, **Fig. 3.** Pressure and temperature should correspond to actual conditions. It takes first place to think about the time dependence of the crack opening process after crack initiation but also about the nature of crack formation, that means leakage or catastrophic failure. Hereby the behaviour of the pressure medium is of direct influence on crack extension as well as on crack arrest.

Further goals of the piping research programmes are the verification of analytical models as well as the application of fracture mechanics and computer codes.

Loading and test conditions are described in **Fig. 4**, the dimensions of straight pipes and cylindrical vessels tested so far as well as the flaw types, flaw orientations and flaw locations are shown in **Fig. 5.**

Fig. 6 contains the materials used.

Since under upset conditions such as earthquake, aircraft crash, water hammer etc., high additional loading from primary bending can arise along with the internal pressure loading due to operation, the load bearing capacity reserves of piping must be known and all the more so if it contains faulty circumferential joints.

For the experimental analysis of the mechanical behaviour of piping of important safety related significance under upset conditions, appropriate tests have been carried out for some years at MPA Stuttgart on pipes and also on flat tensile specimens made from pipes mainly of ferritic materials.

These research projects, aimed at demonstration of the integrity of piping and promoted partly by the Federal Ministry for Research and Technology and partly by industry and plant operators, may be divided into four individual areas. Whilst in the impact tensile and component tests, the test pieces are subjected to a single, dynamically-applied loading, tests for the demonstration of cyclic strain behaviour are basically conducted under multiple i. e. cyclic loading. The impact tensile- and component tests provide information on the load bearing capacity and deformation behaviour whereas tests for the demonstration of cyclic strain behaviour for which bar and tubular specimens are likewise used give information on cyclic deformation behaviour.

The impact tensile tests were carried out on flat specimens taken from pipes, some containing circumferential joints /5/.

The objective of the investigation was to determine the strength and deformation behaviour as a function of defect size and temperature at various loading rates of the ferritic material 17 MnMoV 6 4 used for power plant piping components by means of quasistatic and impact type tensile tests conducted on flat tensile specimens, in particular ones bearing defects such as notches, cracks and porosity, taken from pipes.

The velocity of straining for the dynamically tested specimens was preset at 6.5 to 7.5 m/s. Thereby the mean rate of stress rise during the elastic loading phase in the gross cross section region had a value of about 6 MN/mm^{2}s. The mean strain rate up to specimen fracture in the unnotched specimens related to the total gauge length of 150 mm was correspondingly determined as up to 30/s.

In notched specimens where in practice deformation was confined to the ligament (notch opening), mean strain rates related to the notch width in the region of the notch tip (ca. 0.5 mm notch flank separation distance) were calculated to be up to 15,000/s.

In order to apply the transferability criteria on wide plate specimens and pipes e. g. with circumferential welds under static loading conditions and tests with high strain rate, MPA Stuttgart has built a 12 MN high rate tensile testing machine operating with gun powder as a propellant, /6/.

The research programme which includes the construction of this machine is shown in **Fig. 2.**

According to Fig. 2 and 7 pure internal pressure tests with pipes as well as pipe bend tests have to be carried out to quantify the safety margin and to determine the load bearing capacity under single quasistatic and dynamic load.

The Phenomenological Vessel Burst activity /7/ has been divided into two phases. Phase I finalized in 1984 concentrated on primary piping as used for German 1300 MWe PWR's and on longitudinal defects. Phase II deals with circumferential defects. The full scale tests again under operational conditions of pressure and temperature include such tests with superimposed static and dynamic bending load. The topics of the Phenomenological Burst Behaviour-Programme, Phase I are shown in **Fig. 8.**

Cylindrical pipes with 700 mm internal diameter, 47.5 mm wall thickness and a length of 2,500 and 5,000 m, closed on both ends, were used as test vessels. The vessels have artificial notches in longitudinal and circumferential direction. They were made of two materials: 20 MnMoNi 5 5 in basis-safe quality with high upper shelf impact toughness ($A_v \geqslant 150$ J) and a special melt 22 NiMoCr 3 7 with low upper shelf impact toughness ($A_v \leqslant 50$ J), **Fig. 9.** The wall thickness of the test vessels were chosen such that a nominal stress of approximately 130 N/mm^2 under operating conditions can be obtained. Except for the energy stored in the pressure medium, the time-history of the pressure decay after crack initiation has, among other aspects, a direct influence on the fracture behaviour. The measured pressure decay for vessel BV7 042 which was tested under air-conditions as well as for vessels which were tested under PWR-conditions is shown in **Fig. 10.** After crack initiation, which means after penetrating the ligament of the longitudinal notch, the pressure fell to saturation pressure within 2 or 3 milliseconds, whereas under air-conditions it takes nearly 15 milliseconds.

The crack opening is measured indirectly and the shape of the vessel cross-section, shown in **Fig. 11,** is obtained by numerical integration of the particular radies of the vessel. Finally the actual crack opening area is plotted against time, **Fig. 12.**

In **Fig. 13** the experimentally developed ratio of maximum crack opening area F_L to the cross-sectional area of the vessel F_R is plotted against the ratio of initial flaw length l_0 to the critical flaw length l_{crit}. It can be seen that a ratio F_L/F_R of about 0.1 corresponds to an initial flaw length l_0 that is equal to the critical flaw length l_{crit}. From Fig. 13 it is to be concluded that a noteworthy leakage cross section of about 10 % (0.1 F criterion) only occurs with surface defects which have original lengths amounting to at least 85 % of through thickness critical length.

Several vessels failed are shown in **Figs. 14, 15 and 16.**

The "leak-before break curves" of the vessels with longitudinal defects are of materials with different toughness values are plotted in **Fig. 17**. The comparison of the calculated and the experimental leak-before break curve is shown in **Fig. 18.**

Phase II of the Phenomenological Vessel Burst Experiments /8/ concerns component tests consisting of bending tests on vessels which are loaded by internal pressure and additionally by an externally applied quasistatic or dynamic bending, Figs. 2 and 7. At present there are four 4-point bending test rigs available at the MPA Stuttgart. **Fig. 19** shows a 4-point bending test rig with a 3 MN pressure cylinder which provides bending moments up to 3 MNm. Pipes up to diameters of about 600 mm with wall thicknesses up to 25 mm may be tested in this rig.

A further bending test rig having a 10 MN pressure cylinder, giving bending moments up to about 10 MNm and taking pipes up to 800 mm diameter and 50 mm wall thickness is available, **Figs. 20 and 21.** Tests on vessels of 800 mm outer diameter and 47 mm wall thickness statically preloaded by an internal pressure of 15 MPa are underway. These tests are closely related to the life time estimations for piping systems with postulated circumferential flaws in the case of additional stresses caused by earthquake, water hammer or aircraft crash.

As can be seen from **Fig. 22** the critical flaw lenght becomes smaller as the bending moment increases, assuming a constant nominal stress level. Failure in the form of a leakage can only be expected with a high bending moment and a deep flaw ($a/t > 0.9$).

In contrary to the examination of straight pipe components the investigation of complex piping systems is part of the HDR Safety Programme /9/. According to **Fig. 23** both blowdown tests after a simulated double-ended guillotine break under operating conditions as well as temperature stratification tests especially with respect to the evaluation of stresses under thermal load are carried out. Furthermore the failure behaviour of the piping system is being investigated with the aim of evaluating the safety margin of cracked piping elements both in the weld regions as well as in the elbow regions under operating conditions and superposed cyclic bending, **Figs. 24.** The necessary test setup consists of a pipe DN 400 which is coming from the pressure vessel, leads via several elbows to a valve resp. a device with a burst disc, and which has an overall length of approx. 22 m.

A further programme deals with the Inelastic Analysis of Elbows , **Figs. 25 and 26.** It is the aim of this project to investigate the behaviour of pipe elbows in piping under mechanical loading as to stiffness and deformation especially in the non-linear range /10/. The experimental investigations are accompanied by a theoretical analysis aided by inelastic 3D computer codes. The tests were performed with a 90° elbow, O. D. 470 mm and 41 mm wall thickness, fabricated out of the ferritic steel 15 MnNi 6 3, **Fig. 27**, and with the load case "in-plane"-bending.

The longterm behaviour of complex piping components especially at temperatures in the creep range is being investigated within the compass of several research projects which are financed jointly by nuclear industry and the Federal Ministry for Research and Technology, **Fig. 28.** In-plane tests on elbows made of ferritic material under internal pressure at temperatures up to 550 °C are being conducted over a duration of up to 30,000 hours, **Fig. 29.**

These creep tests are completed by strain tests which investigate the cyclic strain behaviour of smooth and notched tensile bars and pipes, **Fig. 30.**

The behaviour at cyclic inelastic load is significant for the safety-technical judgement of piping systems especially when the system is activated i. e. by aircraft crash, waterhammer or earthquake. In **Fig. 31** the procedure at earthquake simulation on pipes is shown schematically; it is contained in a following Programme of the Phenomenological Vessel Burst Experiments Fig. 2, 7 and **32.** The test pipes are weakened for those investigations in a defined manner partly by circumferential flaws, **Fig. 33.** Pipes, tested with the same strain amplitude as smooth tensile bars show a lower number of load cycles at crack initiation, resp. fracture. The presence of crack-type flaws in the component will reduce the number of cycles considerably.

The component tests are completed by investigations with a corrosive medium. These corrosion tests with crack growth measurements are conducted on specimen bars and tubular specimens under static, slow rate and cyclic loading.

2.2 Analytical Activities

Validation work of existing fracture mechanics approaches had to be performed on the basis of full scale testing under actual operational conditions, **Fig. 34.**

For the validation of the particular computer codes a great part of the experimental component tests is being accompanied by pre- and/or post-calculations. Especially in the Phenomenological Vessel Burst Programme the fluid and structure behaviour and the dynamic behaviour of bursting vessels was carried out in comparison of experiment and calculation /11, 12/. The fluid-structure interaction was investigated with the aid of the computer code DAISY (**Fig. 35**), a coupling of the fluid code DAPSY and the structure programme ASKA, as well as with the finite element computer code SAN (Structural Analysis Programme).

The fracture process from crack initiation to crack arrest is confined to few milliseconds and during this short period of time no gross fluid motion has to be expected. With respect to the material properties, the pressurized water is considered as compressible fluid. Fast fracture propagation is modelled by releasing the equivalent nodal forces of the ligament according to a constant average crack propagation speed.

3. Summary

The German Reactor Safety Research and Development Programme of the Federal Ministry for Research and Technology (BMFT) started in the early seventies with investigations on the phenomenological vessel burst behaviour. The aims of these experiments on components made of mostly ferritic materials were focussed on the generic issue of leak-before-break of piping and pressure vessels. It was the aim of those investigations to research the fracture as well as the crack opening process but also the crack propagation and crack arrest. The tests were conducted under actual conditions with respect to internal pressure, temperature and pressure medium, which was decisive for the transferability of the test results on actual components.

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Parallel to these efforts German nuclear industry prepared their specific leak-before-break approach. Within the scope of that Research and Development work smaller diameters than for the Phenomenological Vessel Burst Experiments were used and also other materials including austenitic stainless steel /15, 16, 17/.

Validation and verification work of existing fracture mechanics approaches and engineering-type calculational methods were performed on the basis of full scale testing under actual operational conditions. This includes also pre- and post experimental analyses which have been done by means of finite element modelling of fluid and structure interaction during crack initiation, crack propagation and crack arrest.

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BASIS OF THE
SAFETY PHILOSOPHY FOR NUCLEAR COMPONENTS
MEANS
LEAK BEFORE BREAK

INCREDIBILITY OF CATASTROPHIC FAILURE

- BASIS SAFETY AND INDEPENDENT REDUNDANCIES
  - HEAVY SECTION COMPONENTS
  - PIPING
- REPLACEMENT
- CODE WORK

FOR

- **VESSELS, PUMPS, ETC.**
- **STEEL CONTAINMENTS**
- **PIPES, BENDS, BRANCHES, ETC.**

SAFETY STRATEGY

- DESTRUCTIVE TESTING
- NONDESTRUCTIVE TESTING
- ANALYTICAL APPROACHES

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STUTTGART

Fig. 1. Basis of the safety philosophy for nuclear components

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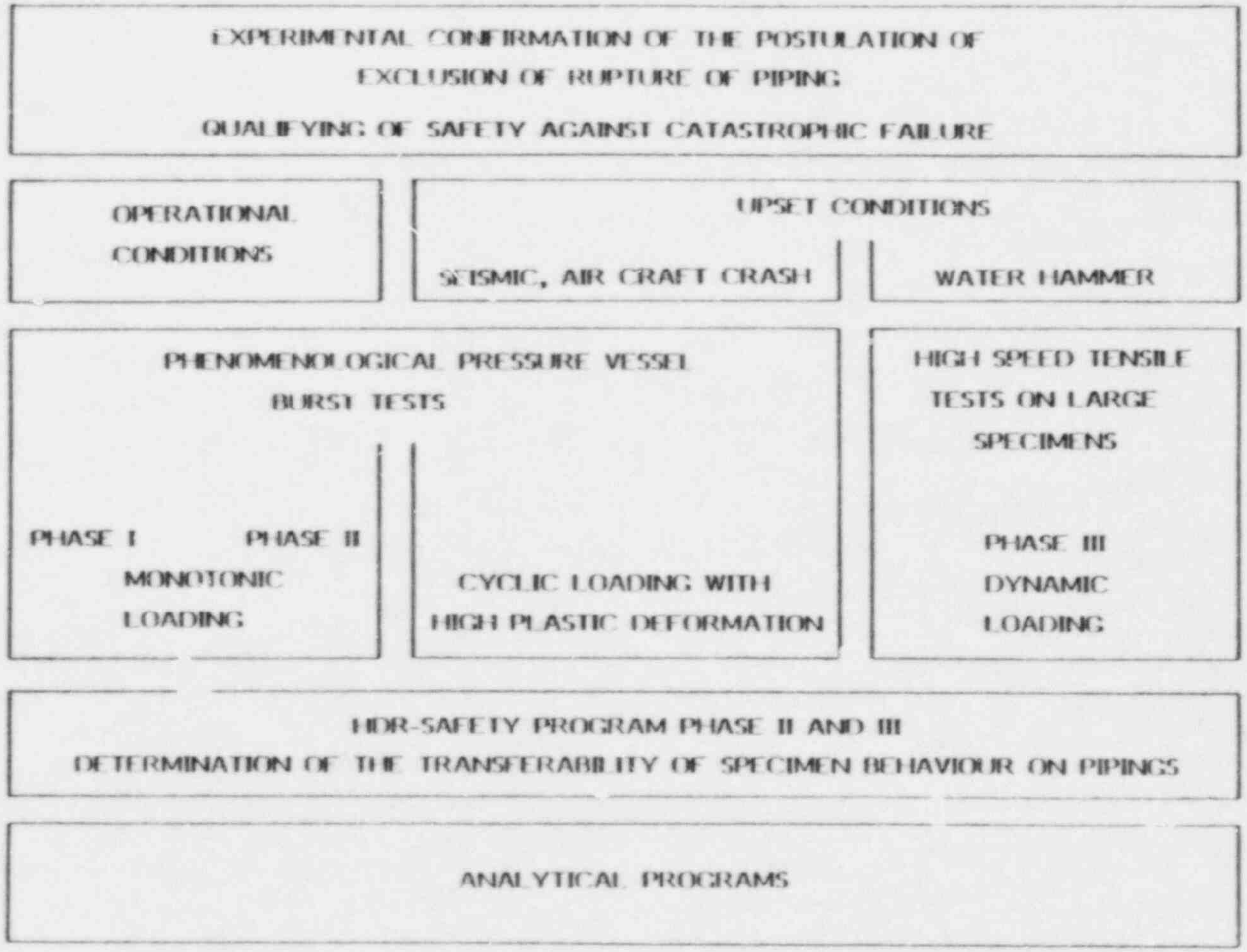

Fig. 2. Piping research in MPA Stuttgart

RECENT RESULTS AND FUTURE PROGRAMMES OF PIPE TESTS IN MPA STUTTGART

SCOPE

MATERIALS

TEST VESSELS AND PIPES

TEST PROGRAMMES

PIPE AND BEND TESTS
- STRENGTH BEHAVIOUR
- CRACK OPENING
- CRACK ARREST

IMPACT TENSILE AND BEND TESTS
- STRENGTH BEHAVIOUR
- STRAIN BEHAVIOUR

CYCLIC STRAIN TESTS

PIPE BEND TESTS

PIPE FAILURE TESTS

CORROSION TESTS

ANALYSIS

MPA
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Fig. 3. Recent results and future programmes of pipe tests in MPA Stuttgart

PIPING RESEARCH
IN THE
FEDERAL REPUBLIC OF GERMANY

LOADING:	INTERNAL PRESSURE	STATIC/QUASISTATIC/DYNAMIC
	EXTERNAL BENDING	STATIC (up to 33,000 h) QUASISTATIC DYNAMIC CYCLIC
	THERMAL TRANSIENTS	
TEST CONDITIONS:	WATER PRESSURIZED WATER CONDENSATE IN BWR STEAM SODIUM AIR ROOM TEMPERATURE, 220 °C, 300 °C, 550 °C	

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Fig. 4. Loading and test conditions

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PIPING RESEARCH
IN THE
FEDERAL REPUBLIC OF GERMANY

COMPONENT:	STRAIGHT PIPE VESSEL BEND
DIMENSIONS: O.D. x T (mm)	133 x 14 200 x 30 279 x 10 324 x 22 352 x 21 350 x 11 368 x 12
	400 x 25 407 x 10 447 x 24 450 x 15 470 x 41 800 x 47
FLAW TYPE:	PART THROUGH-WALL FLAW THROUGH-WALL FLAW NATURAL FLAW (FATIGUE, CORROSION)
FLAW ORIENTATION:	AXIAL (PART) CIRCUMFERENTIAL
FLAW LOCATION:	BASE MATERIAL WELD METAL HAZ

Fig. 5. Components, dimensions, flaws

PIPING RESEARCH
IN THE
FEDERAL REPUBLIC OF GERMANY

MATERIALS

20 MnMoNi 5 5 (A_v = 60 - 200 J USE)

15 Mo 3
15 MnNi 6 3
15 NiCuMoNb 5
17 MnMoV 6 4
14 MoV 6 3

X 20 CrMoV 12 1
X 10 CrNiNb 18 9
X 6 CrNi 18 11
X 2 CrNiMoN 17 12

INCONEL 600
INCOLOY 800

MPA
STUTTGART

Fig. 6. Materials

MPA
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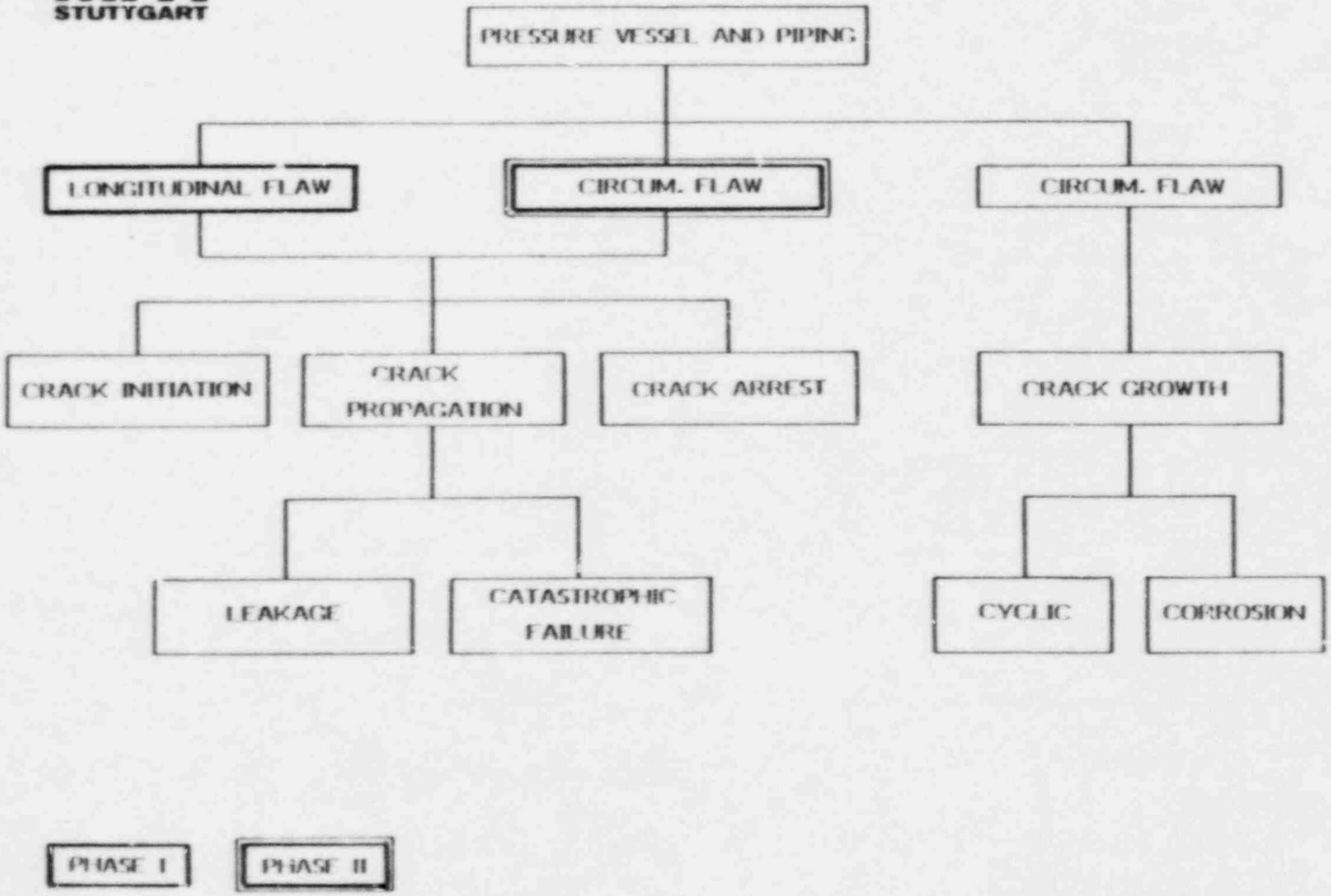


Fig. 7. Aims of the Phenomenological Burst Behaviour Programme

FV - BEHÄLTERVERSAGEN

TOPICS

1. Experimental determination of the time dependence on
    - INTERNAL PRESSURE
    - GAPING
    - CRACK OPENING AREA

   as well as the relationship between

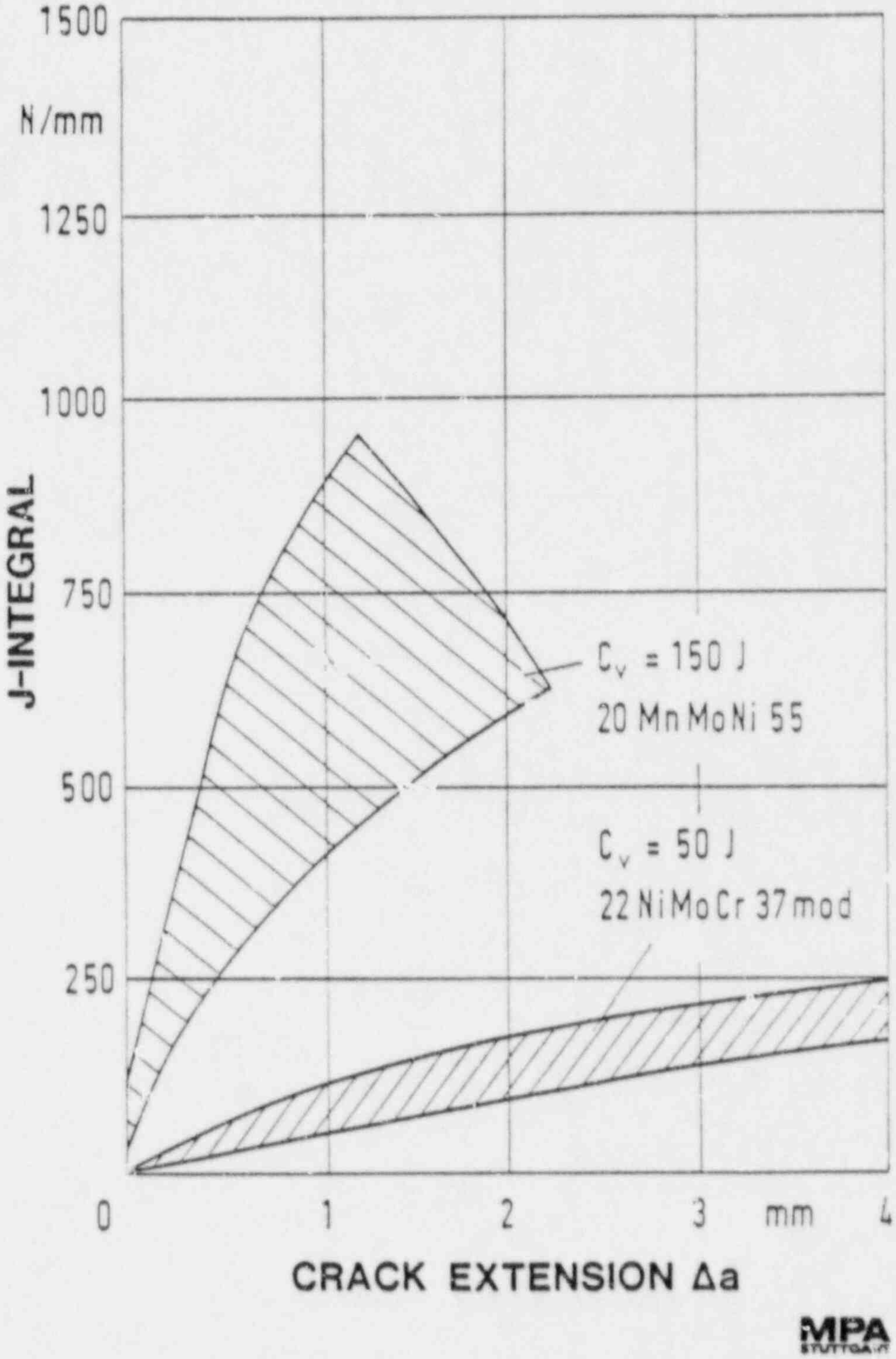
   INTERNAL PRESSURE and CRACK OPENING AREA

   in sight of structure dynamic and fluid dynamic calculations for vessel failure

2. Investigation of the CRACK ARREST BEHAVIOUR

MPA STUTTGART 6641

Fig. 8. Topics of Phenomenological Burst Behaviour Programme



MPA STUTTGART

Fig. 9. Toughness conditions for pipe materials

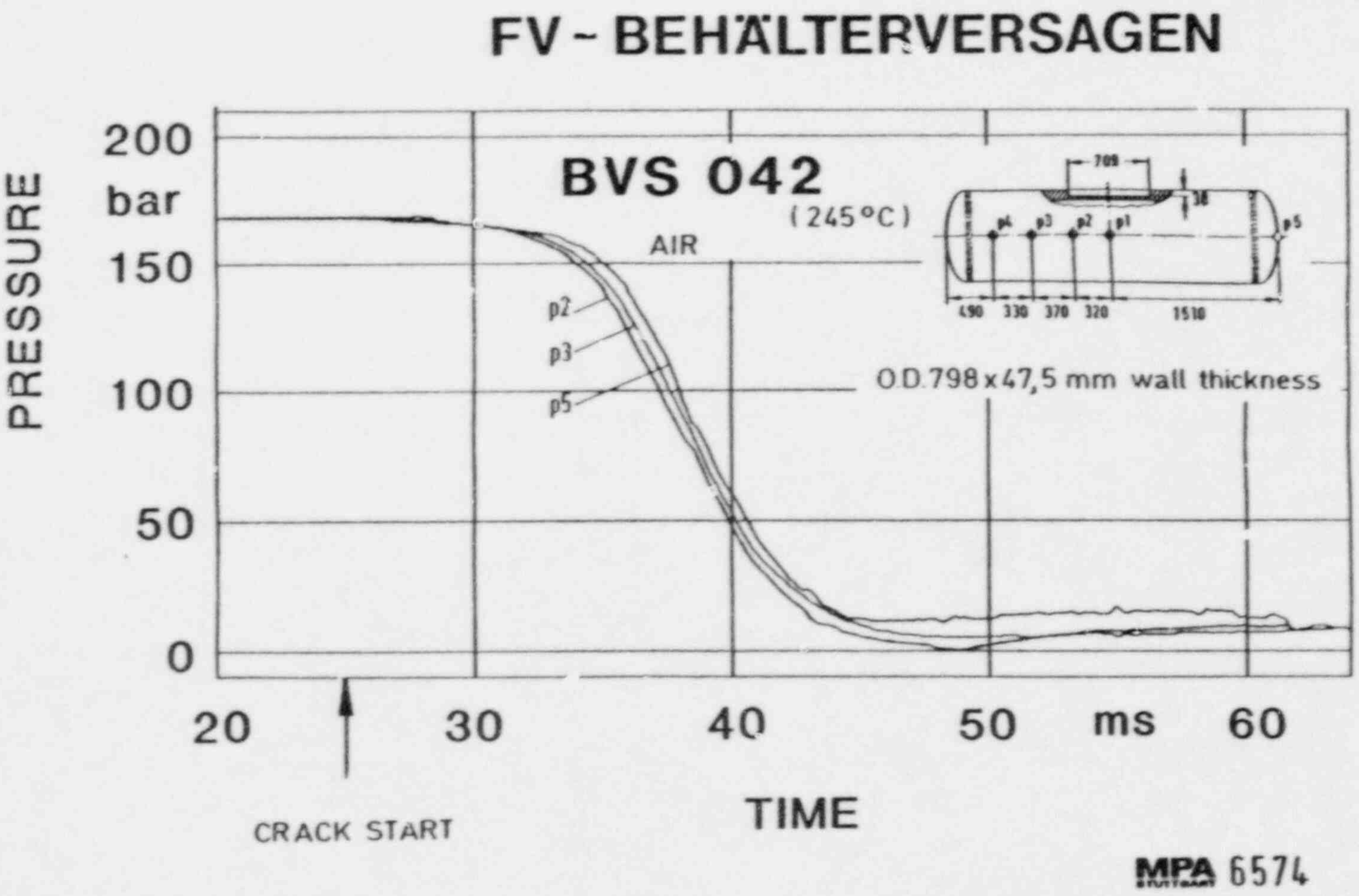


Fig. 10. Pressure history of test vessel BVZ 042 (air, [illegible] °C)

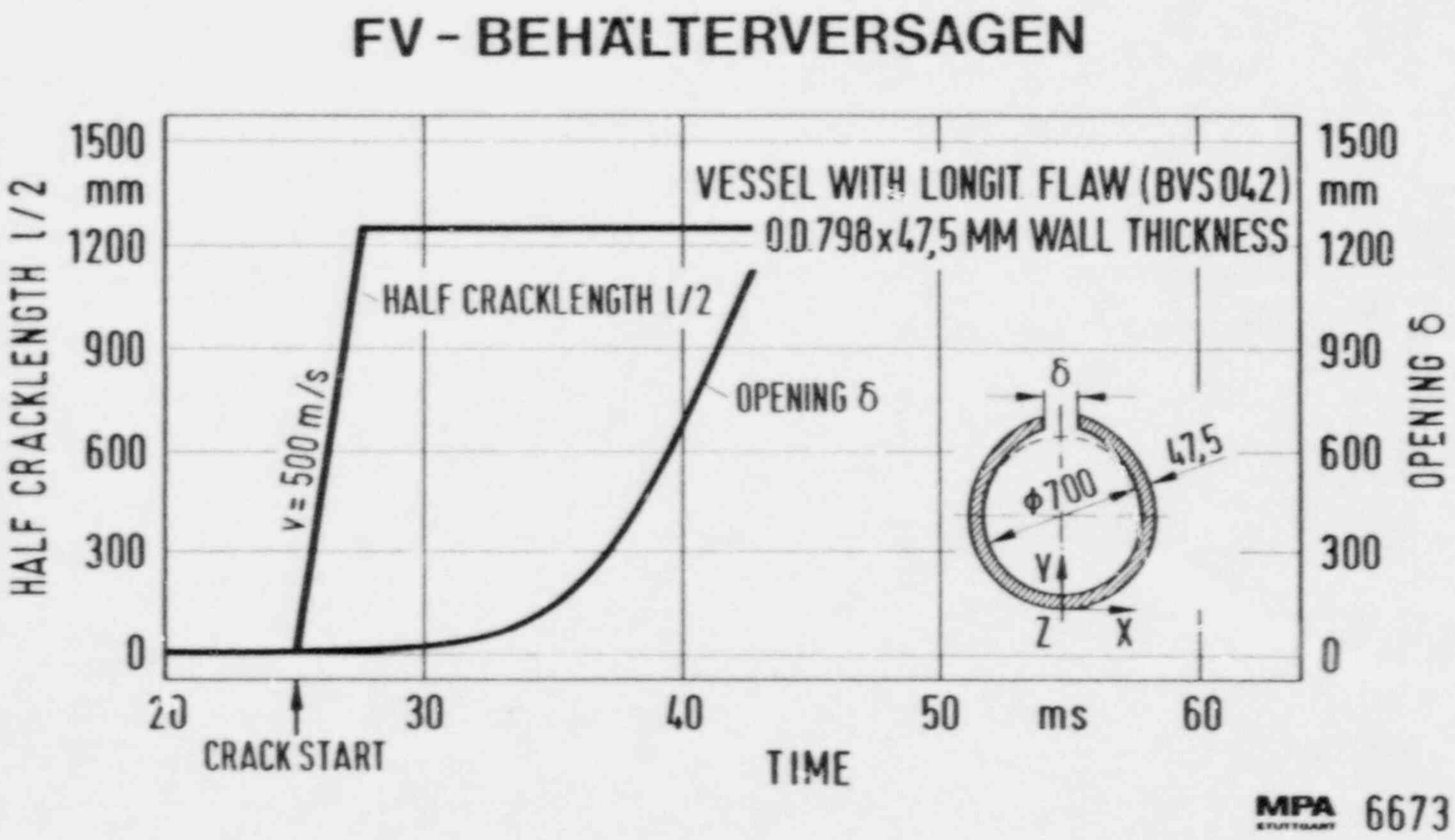


Fig. 11. Longitudinal crack length and crack opening as a function of time, test vessel BVS 042 (air, 245 °C)

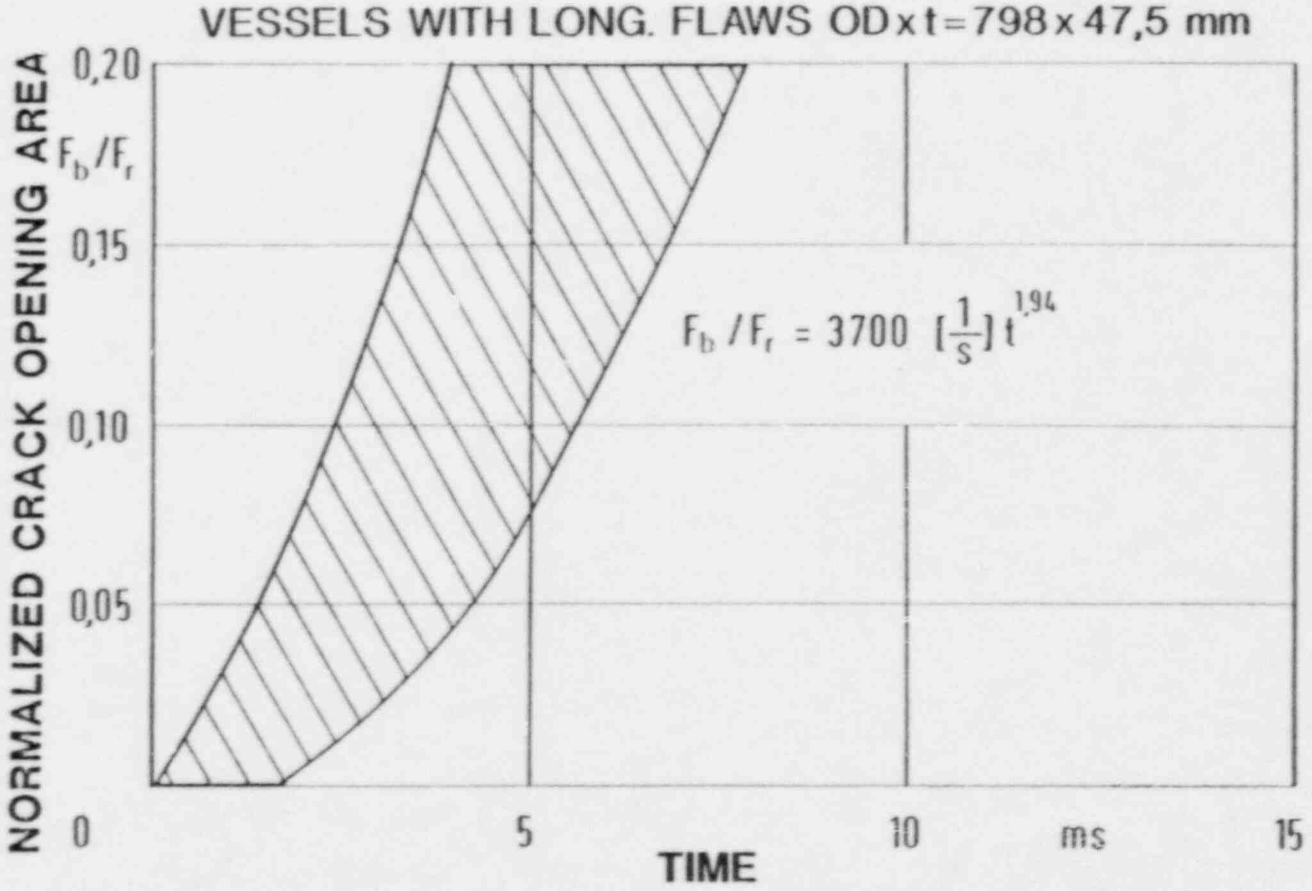


Fig. 12. Relationship between internal pressure and crack opening area in vessel dimensions 800 o. d. x 47 mm wall under pressurised water conditions (305 °C)

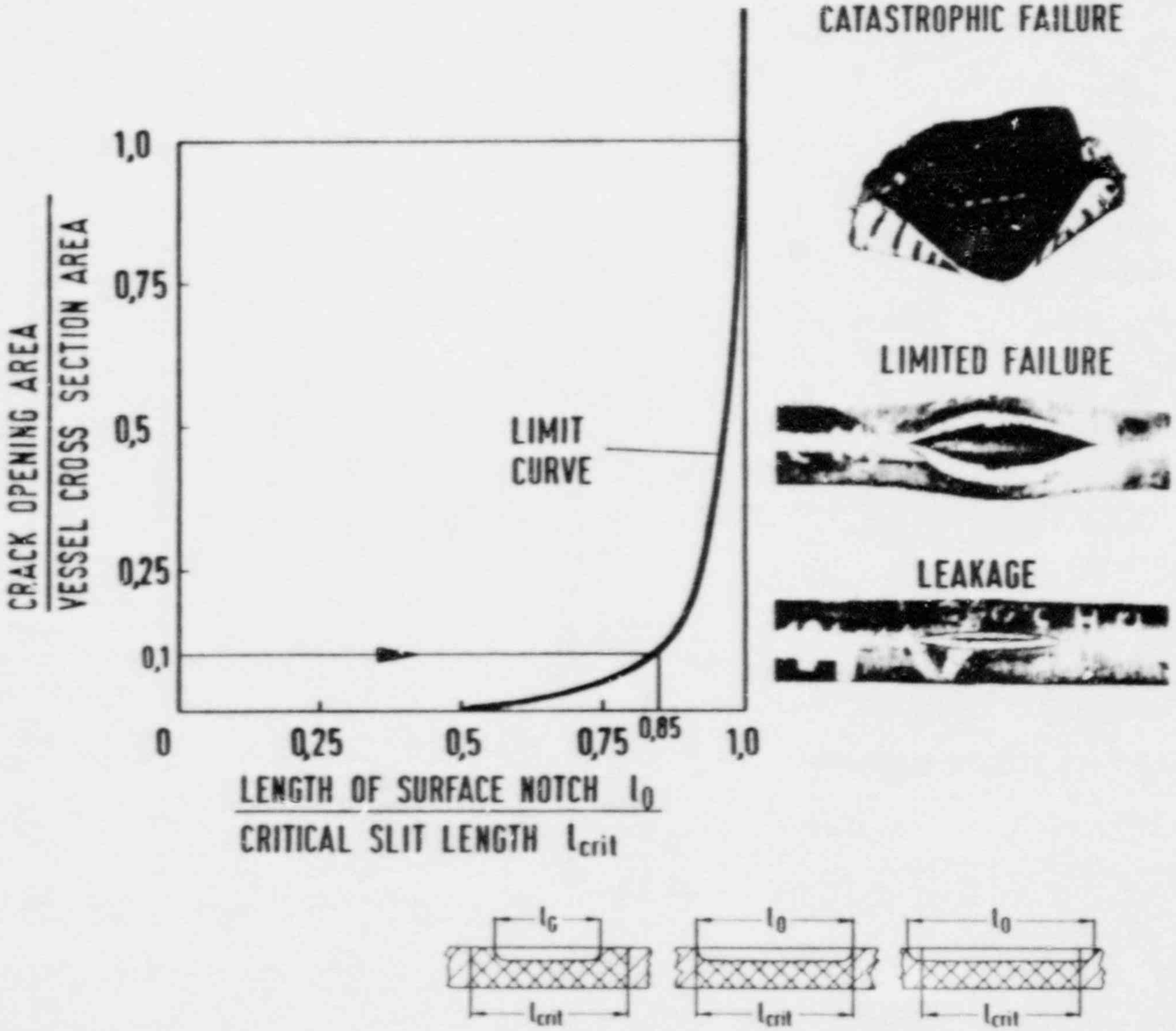
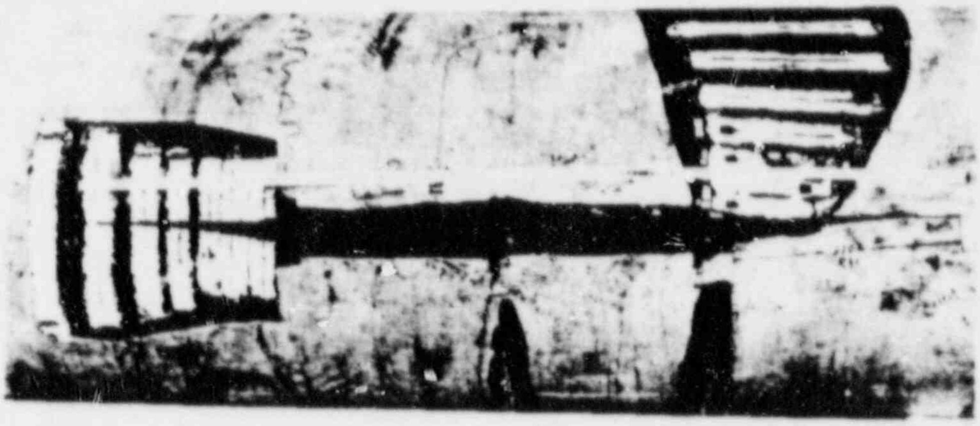


Fig. 13. Normalised crack opening area versus initial surface flaw length (limit curve)

MPA STUTTGART 7527

FV - BEHÄLTERVERSAGEN



Vessel : BVS010
with longitud. slit
Material : 22NiMoCr37 (A_V=47J)
Dimensions : O.D.xTxL = 793,9 x 47,2 x 2500 mm
Slitlength : l_0=800 mm
Burstpressure : p_{max}=17,5 MPa at 155°C

MPA 5951

Fig. 14. Fracture in vessel BVS 010

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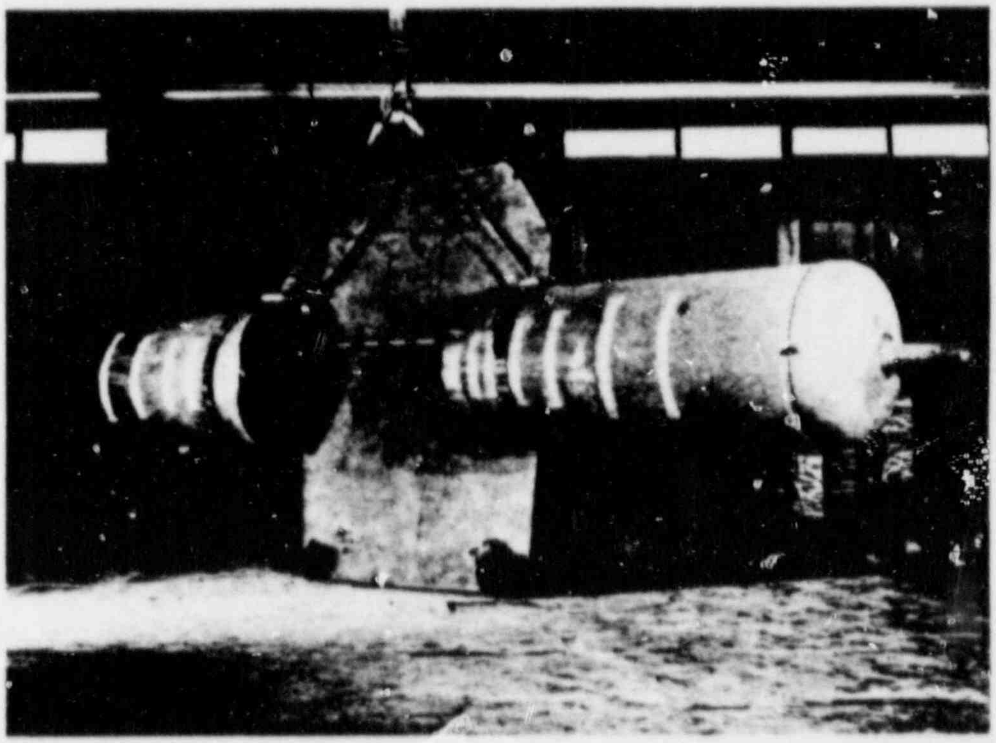
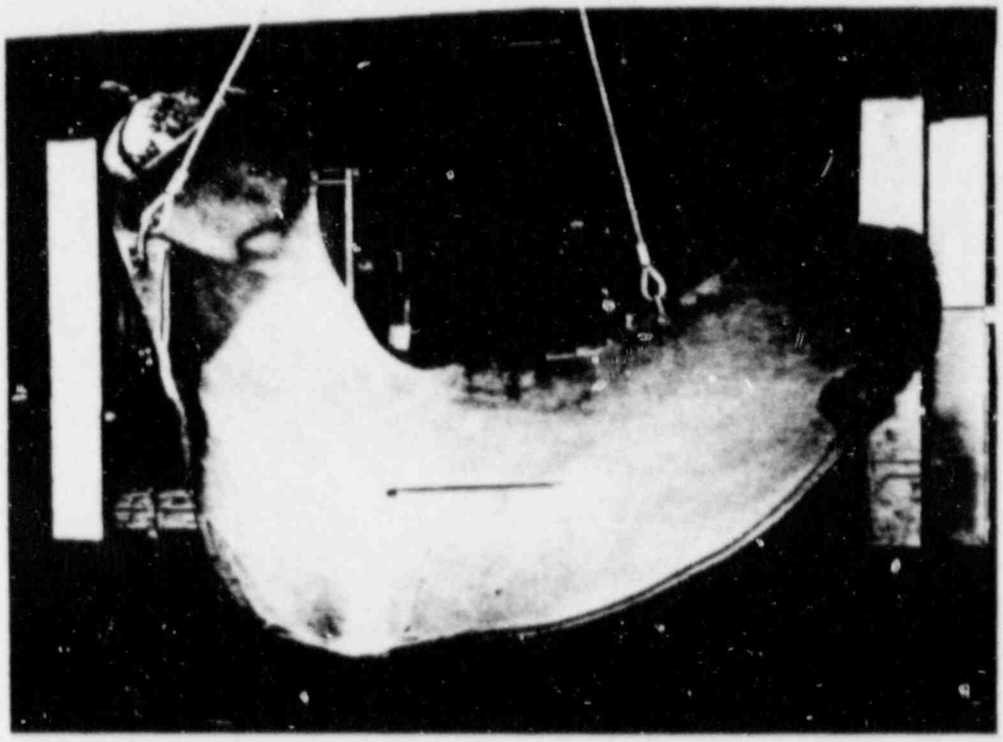


Vessel : BVZ030
with longitud. flaw
Material : 20MnMoNi55 (A_v = 202 J)
Dimensions : O.D. x T x L = 797,9 x 47,2 x 5000 mm
Notchdepth/-length: 36,2/1500 mm
Burstpressure : p = 19,5 MPa at 300 °C

MPA 5933

Fig. 15. Fracture in vessel BVZ 030

FV - BEHÄLTERVERSAGEN



Vessel with longitudinal flaw	:	**BVS 030**
Material	:	22 Ni Mo Cr 37 (A_V = 42 J)
Dimensions	:	O.D.xTxL=793,9x47,2x5000 mm
Notchdepht/-lenght	:	35 / 1100 mm
Burstpressure	:	p = 13,1 MPa at 305°C

MPA 6237

Fig. 16. Fracture in vessel BVS 030

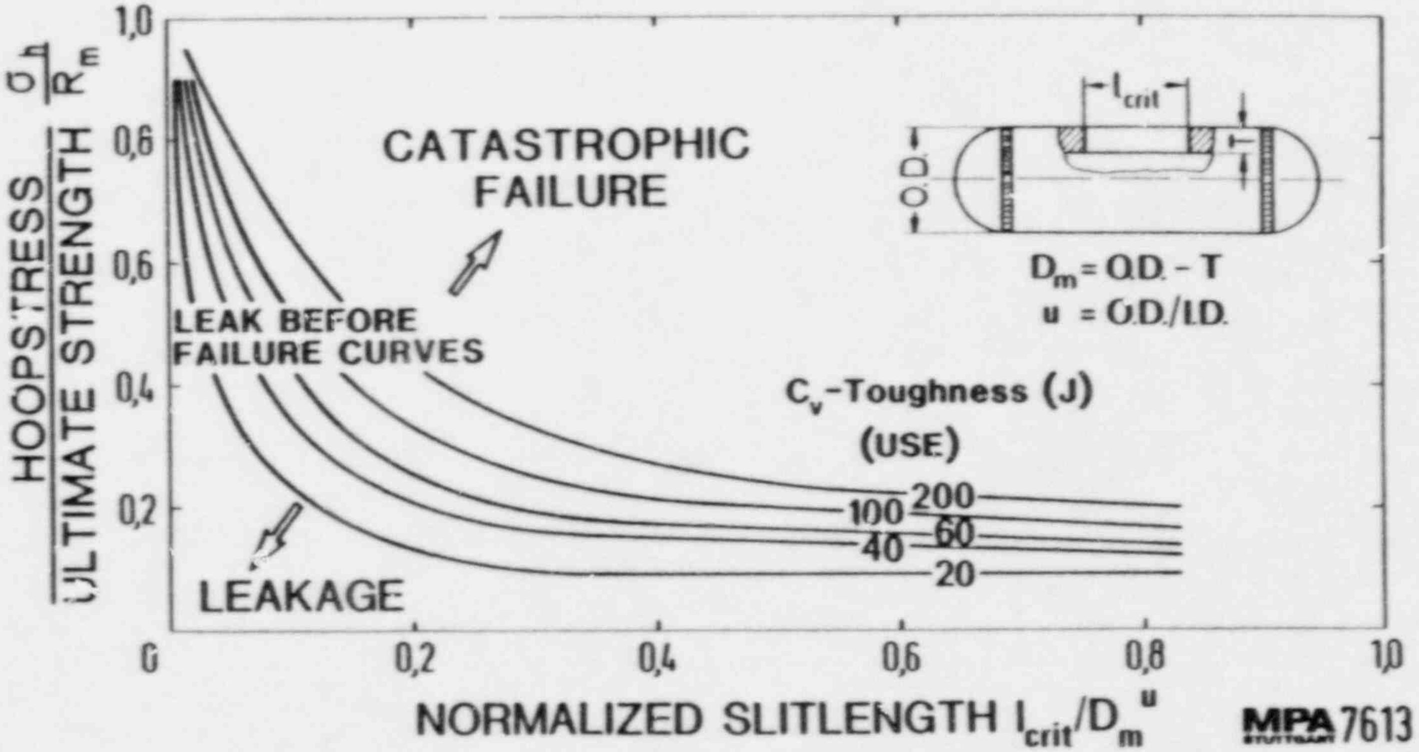


Fig. 17. Influence of the toughness of the Leak-Before Break Curve of pipes

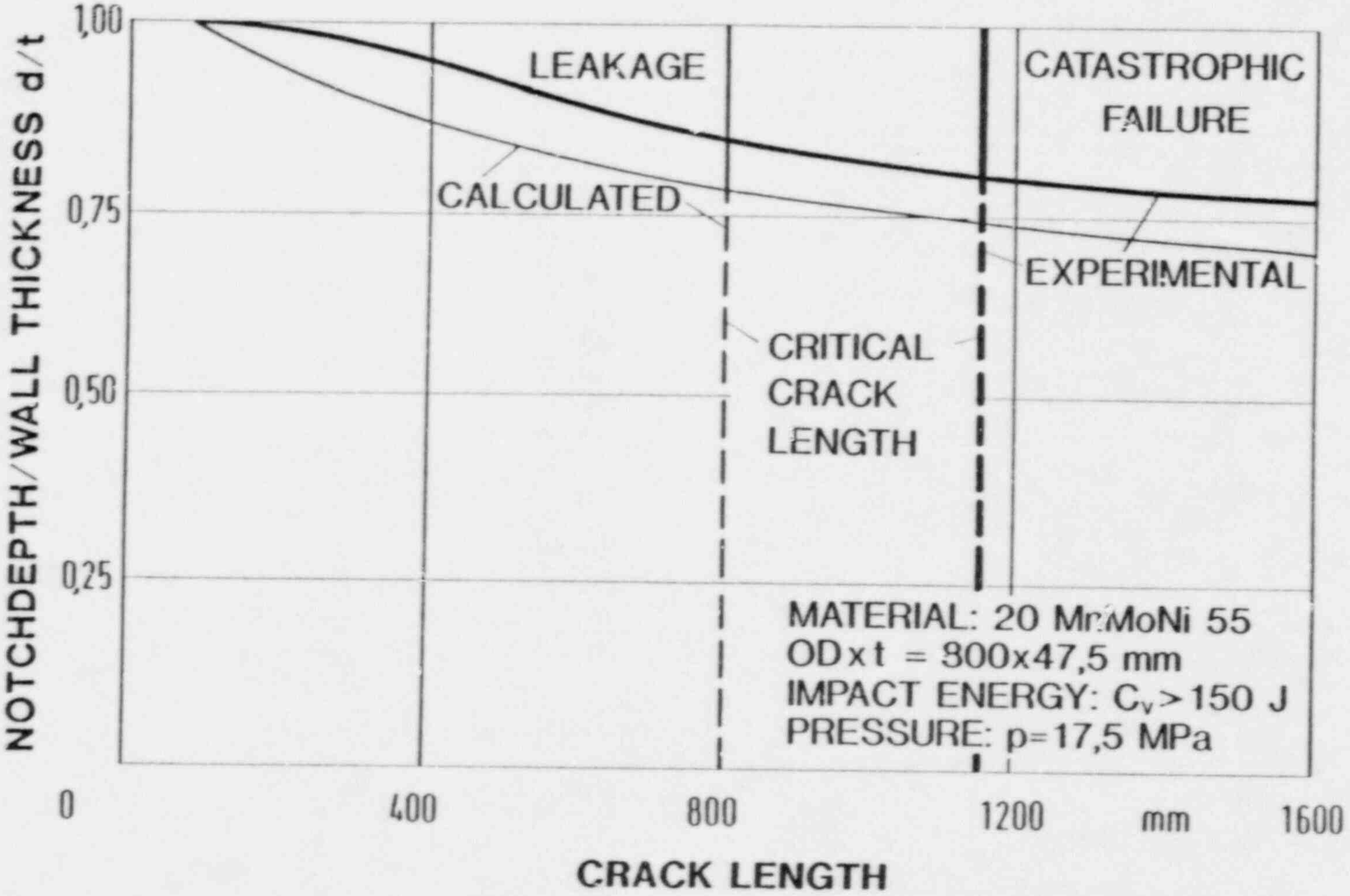


Fig. 18. Experimentally determined and calculated strength behaviour of vessels with longitudinal defects

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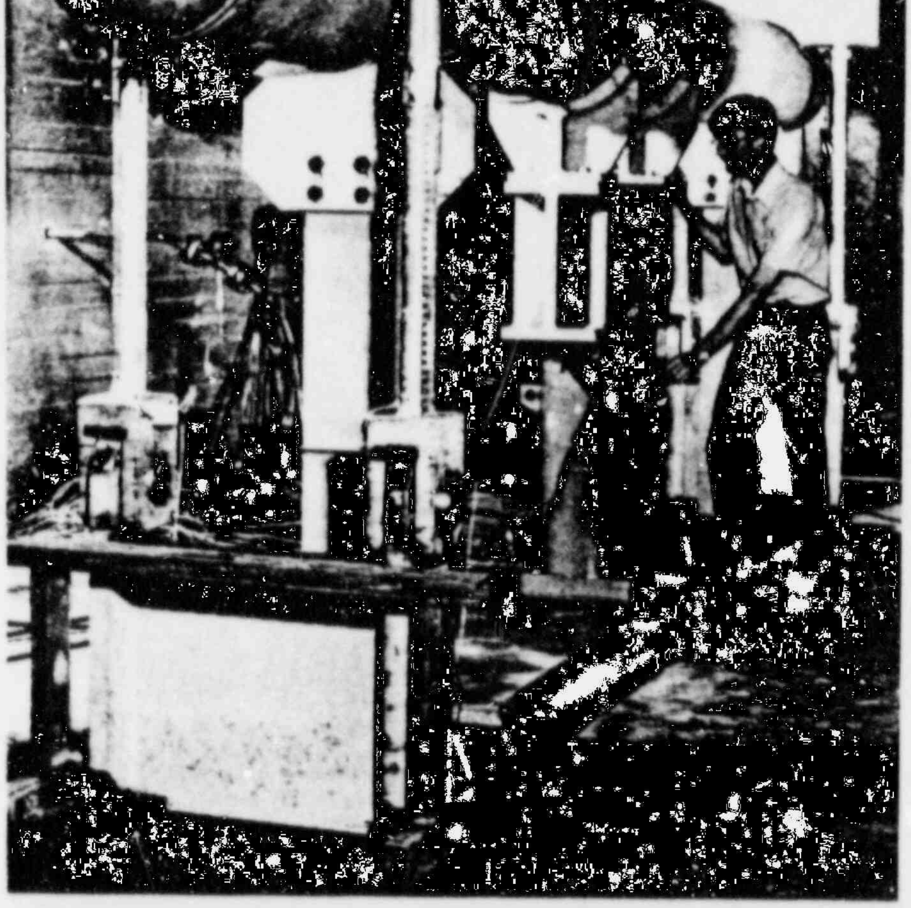
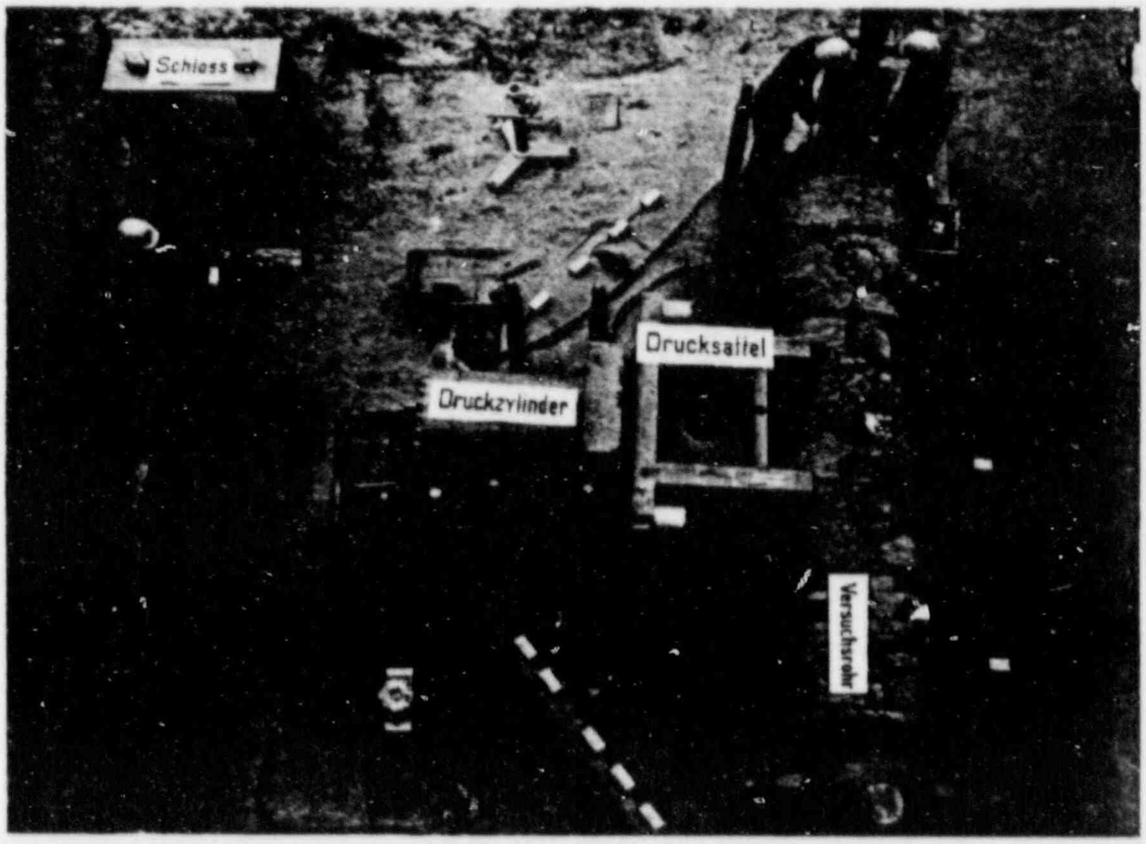


Fig. 19. 3 MNm bending device

BURST BEHAVIOUR PROGRAMME

PHASE II



10 MNm-BENDING DEVICE

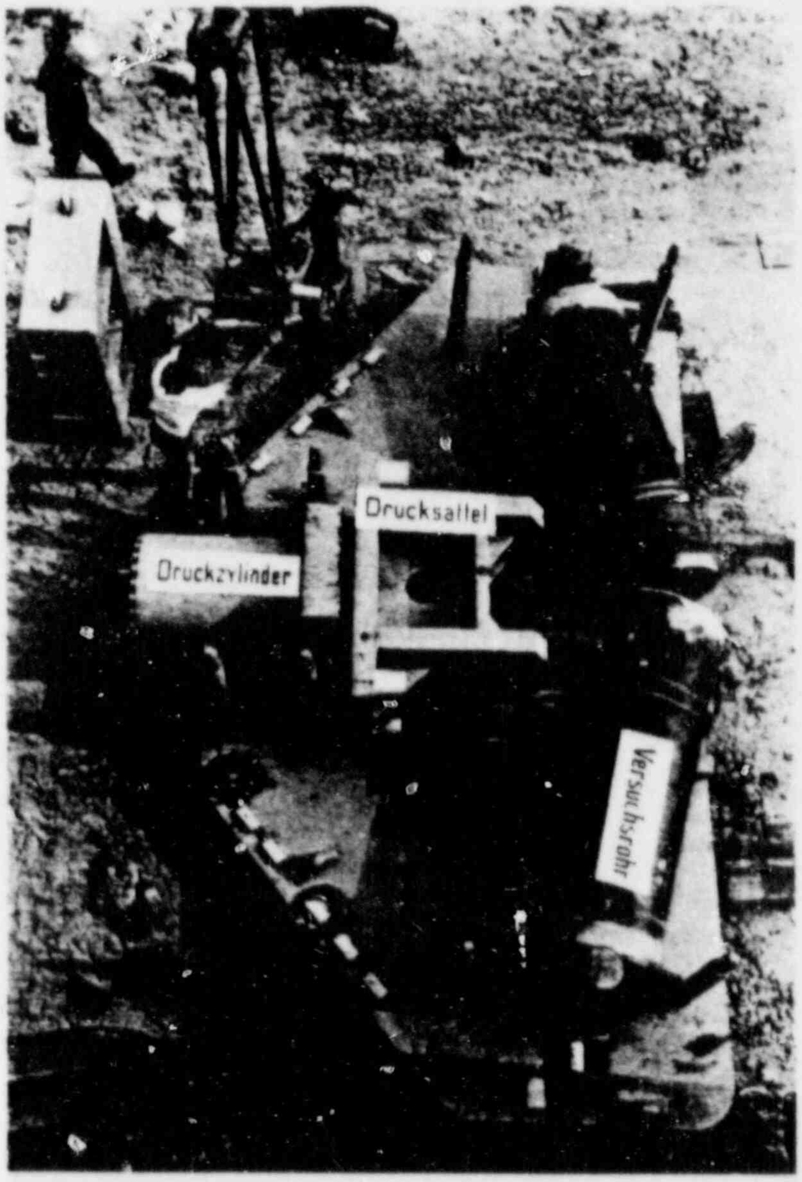
PIPEDIAMETER	800 mm
LENGTH	5000 mm

MPA STUTTGART 9855

Fig. 20. 10 MNm bending device

BURST BEHAVIOUR PROGRAMME

PHASE II



10MNm-BENDING DEVICE

PIPEDIAMETER	800 mm
LENGTH	5000 mm

MPA STUTTGART 10172

Fig. 21. Pipe bend test

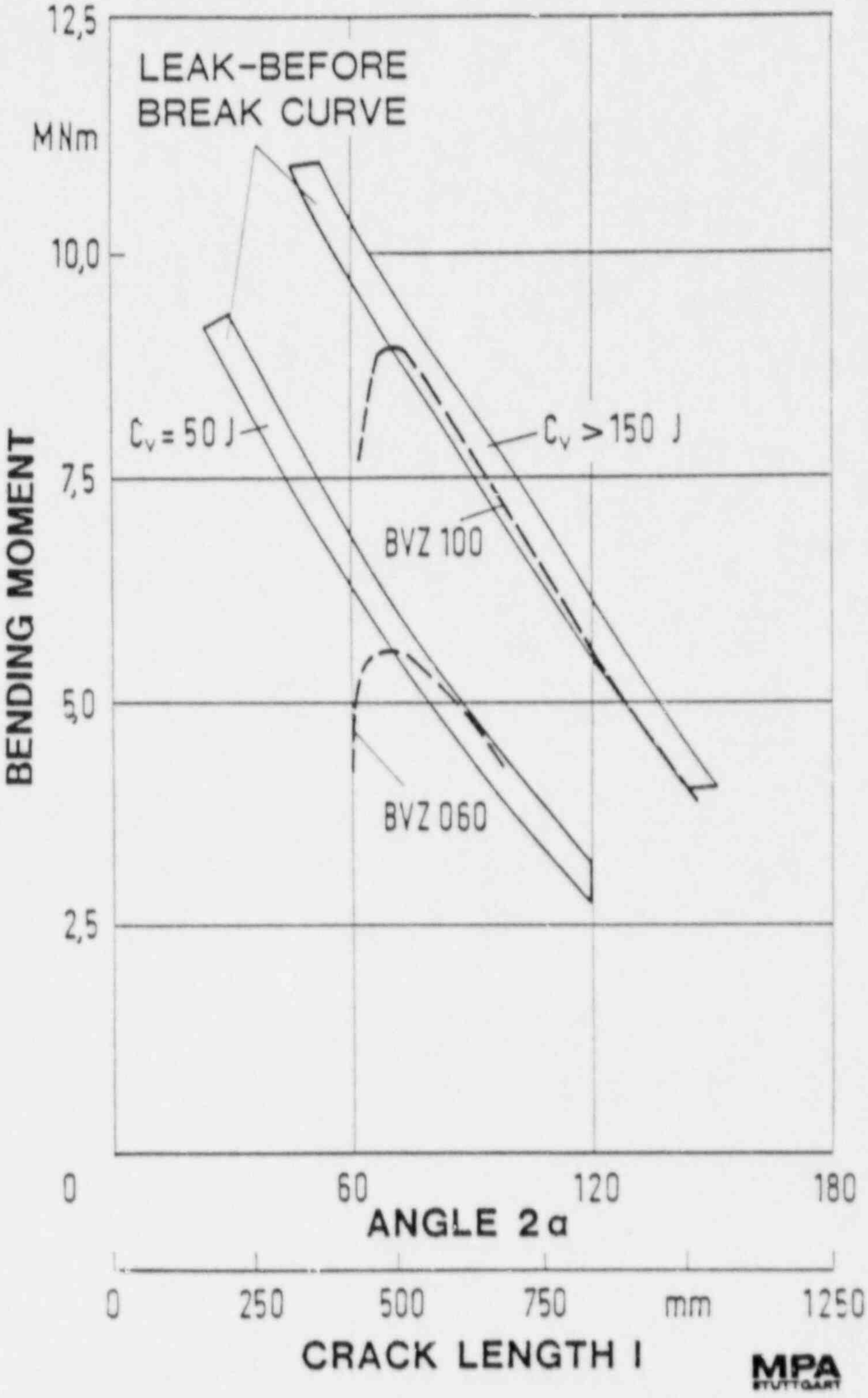
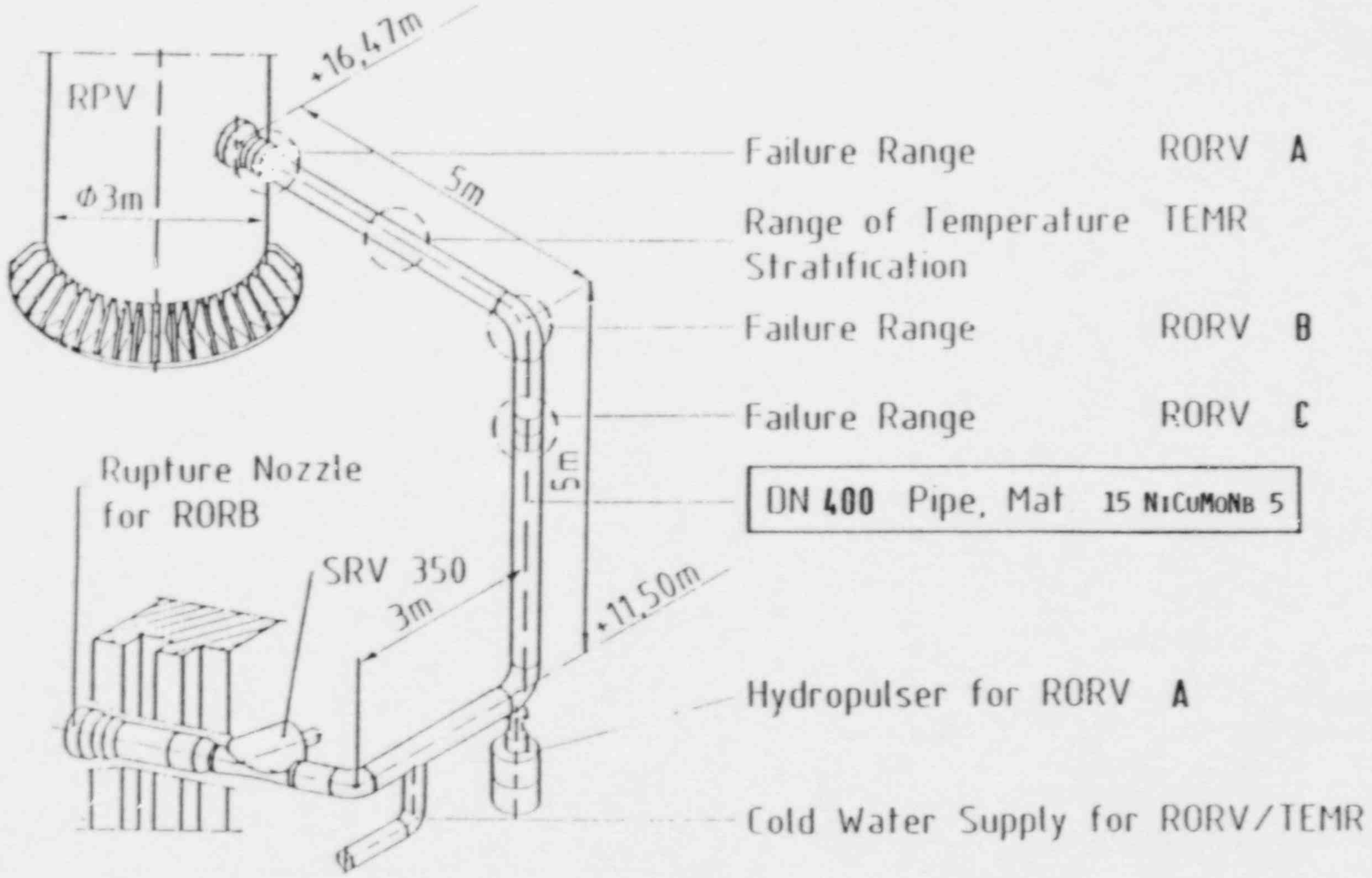


Fig. 22. Strength behaviour of pipe with circumferential flaws 800 o. d. x 47 mm wallthickness

Blowdown on Piping Systems (RORB)	Simulated Double Ended Guillotine Break under Operating Conditions with - optimized (Test 1) - worst case (Test 2) Feed Water Damping Valve Closure
Temperature Stratification (TEMR)	Evaluation of Wall Stresses under Distributed Thermal Load
Failure Behavior of Piping Systems (RORV)	Evaluation of Safety Margins of Cracked Piping Elements A weld region B elbow region under Operating Conditions and Superposed Cyclic Bending (2 Tests)

Fig. 23. HDR-Pipe-Failure-Tests



MPA

Fig. 24. Pipe failure test setup

INELASTIC ANALYSIS OF ELBOWS
(RS 1500705 9, MPA, SDK) 1985

AIMS:	FORMATION OF PLASTIC AREAS DUE TO GEOMETRY AND LOADING VALIDATION OF INELASTIC 3D-COMPUTER CODES EVALUATION OF FLEXIBILITY FACTORS FOR SIMPLIFIED INELASTIC CALCULATIONS
MATERIAL:	15 MnNi 6 3
PIPE DIMENSIONS:	O.D. x T = 470 x 41 mm ELBOW R = 2 x O.D.
LOADING:	IN-PLANE BENDING (OPENING MOMENT)
TEST CONDITIONS:	ROOM TEMPERATURE
PLANNED FUTURE TESTS:	- IN-PLANE BENDING (CLOSING MOMENT) - OUT-OF-PLANE BENDING

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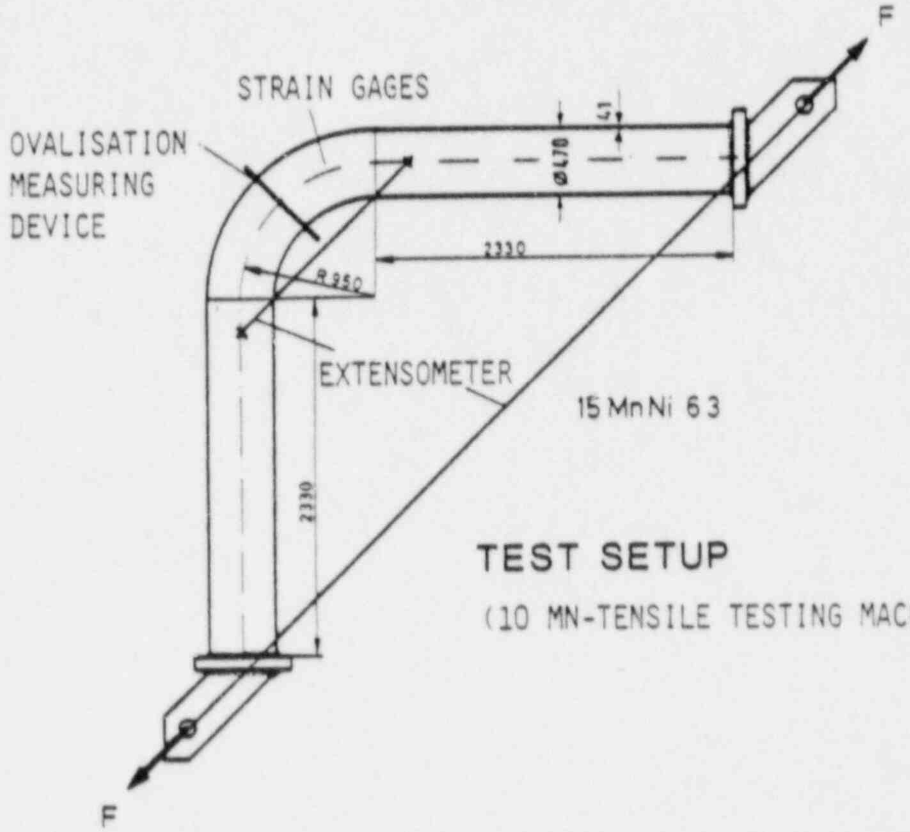
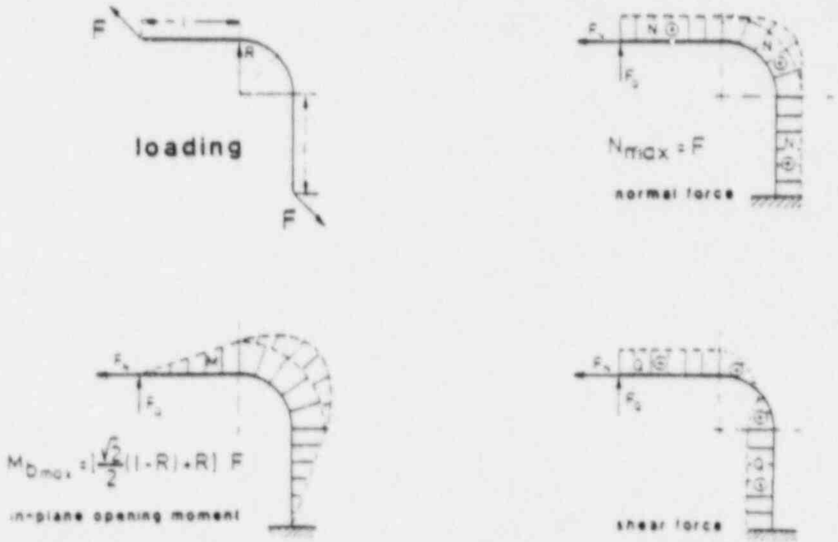
Fig. 25. Research programme "Inelastic Analysis of Elbows"

INELASTIC PIPE-BEND ANALYSIS



TEST SETUP

(10 MN-TENSILE TESTING MACHINE)



DISTRIBUTION OF LOADING COMPONENTS

Fig. 26. Distribution of loading components

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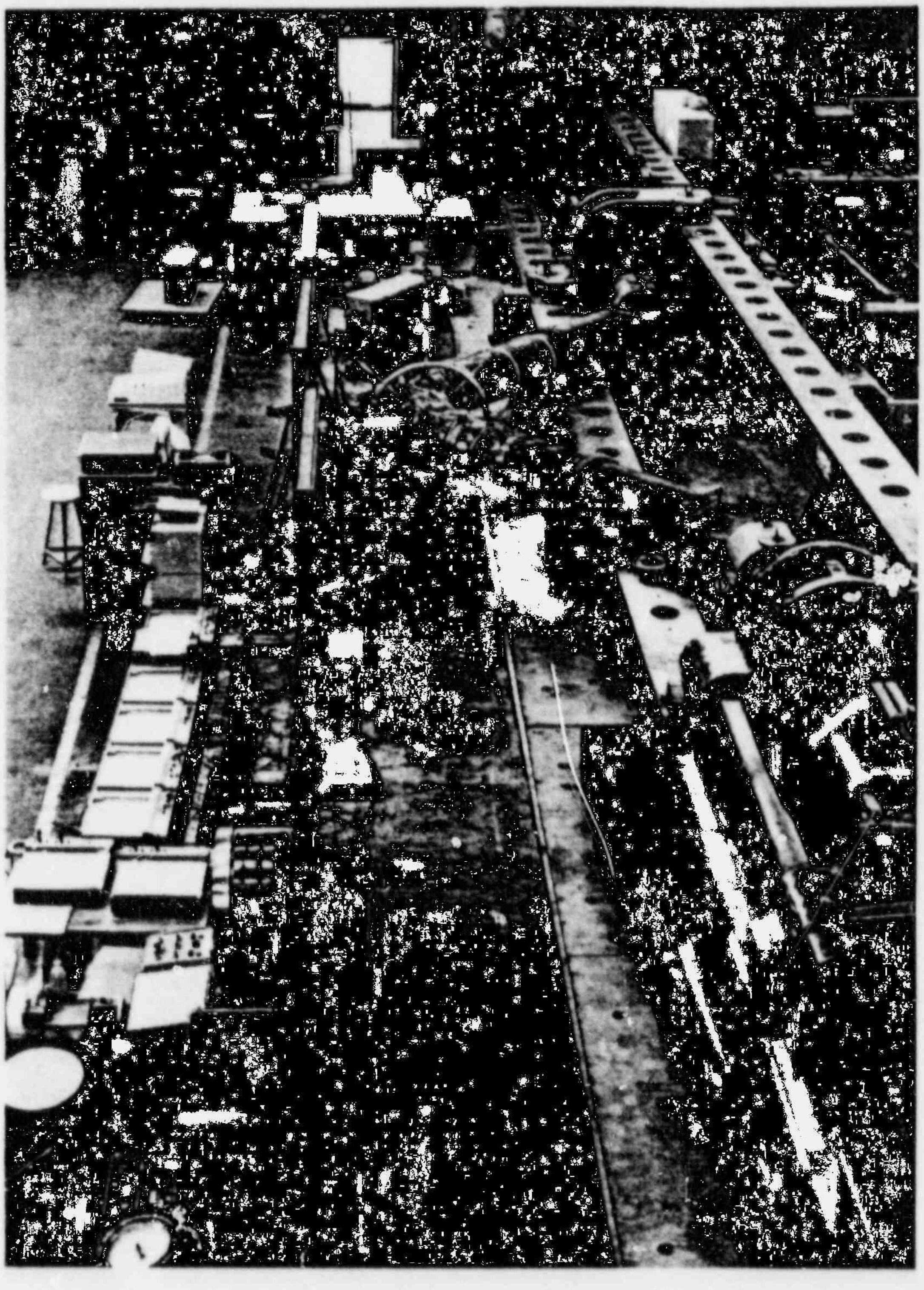


ELASTIC-PLASTIC PIPE BEND ANALYSIS :
TEST SETUP

MPA 9515

Fig. 27. Test setup for pipebend test

INTERNAL PRESSURE TESTS WITH PIPE BENDS OF FERRITIC STEELS UNDER IN-PLANE BENDING AT TEMPERATURES IN THE CREEP REGIME

(150 ..., COOPERATIVE PROGRAMME, PROJECT MANAGEMENT MPA, INTERATOM) 1986-1991

AIMS: EXCLUSION OF CATASTROPHIC FAILURES ON PIPE BENDS
IMPROVEMENT OF CALCULATION METHODS
QUANTIFICATION OF DAMAGE HISTORY
INVESTIGATION OF THE RESIDUAL LIFETIME

MATERIALS: X 20 CrMoV 12 1
14 MoV 6 3

PIPE DIMENSIONS: O.D. × T = 200 × 30 mm
R/D = 1,5

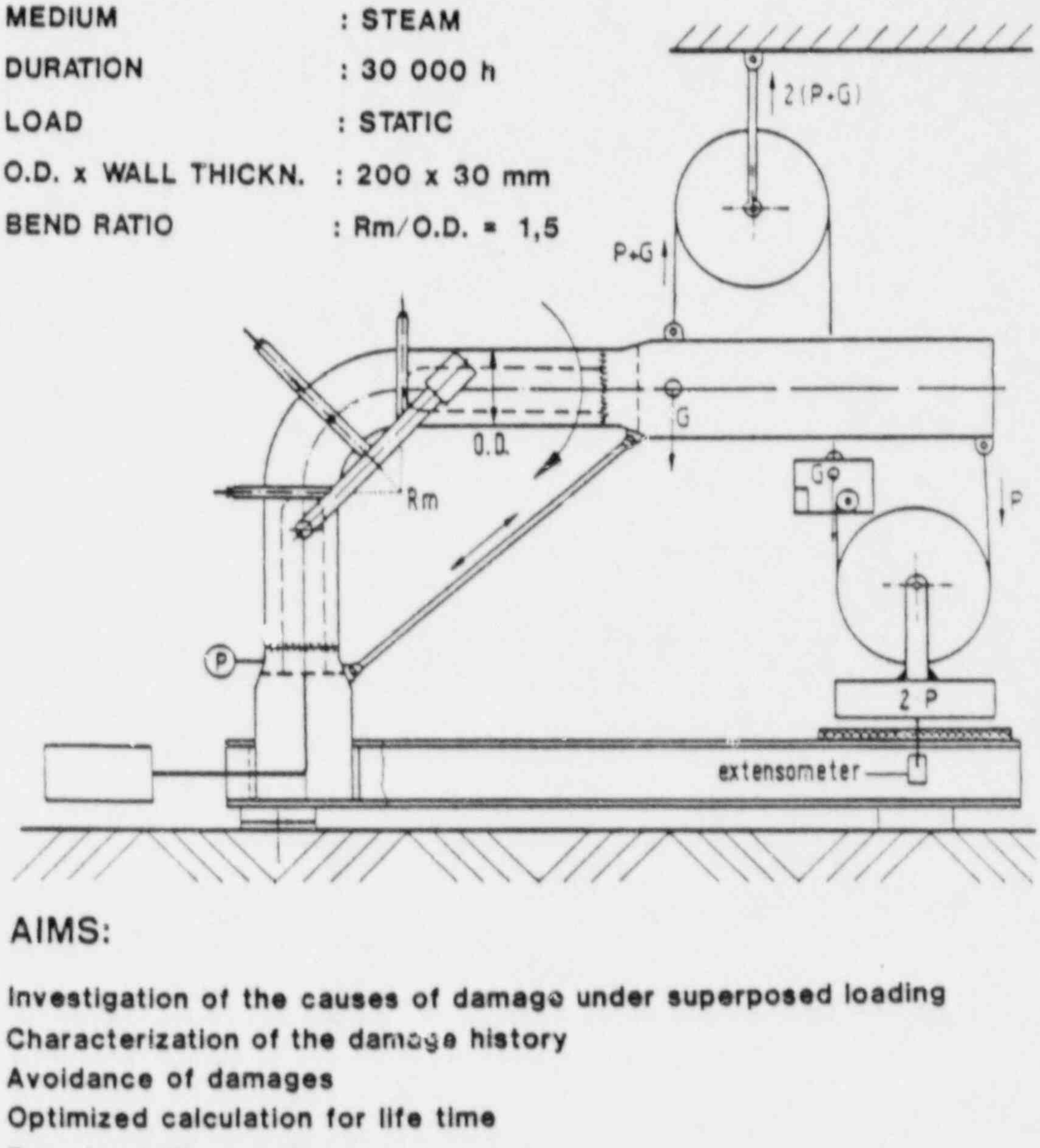
LOADING: STATIC INTERNAL PRESSURE AND BENDING MOMENT

TEST CONDITIONS: STEAM; 550 ^{0}C, 18 MPa, 30,000 h

Fig. 28. Pipebend test in the creep regime

PIPE BEND TEST

TEMPERATURE : 550 C
PRESSURE : 18 MPa
MEDIUM : STEAM
DURATION : 30 000 h
LOAD : STATIC
O.D. x WALL THICKN. : 200 x 30 mm
BEND RATIO : Rm/O.D. = 1,5



AIMS:

Investigation of the causes of damage under superposed loading
Characterization of the damage history
Avoidance of damages
Optimized calculation for life time
Transferability specimen/component
Optimization of measuring procodures

MPA

Fig. 29 Pipe bend test

CYCLIC STRAIN TEST

Cyclic strain behaviour of smooth and notched tensile bars and pipes

Protection against upset conditions

MPA
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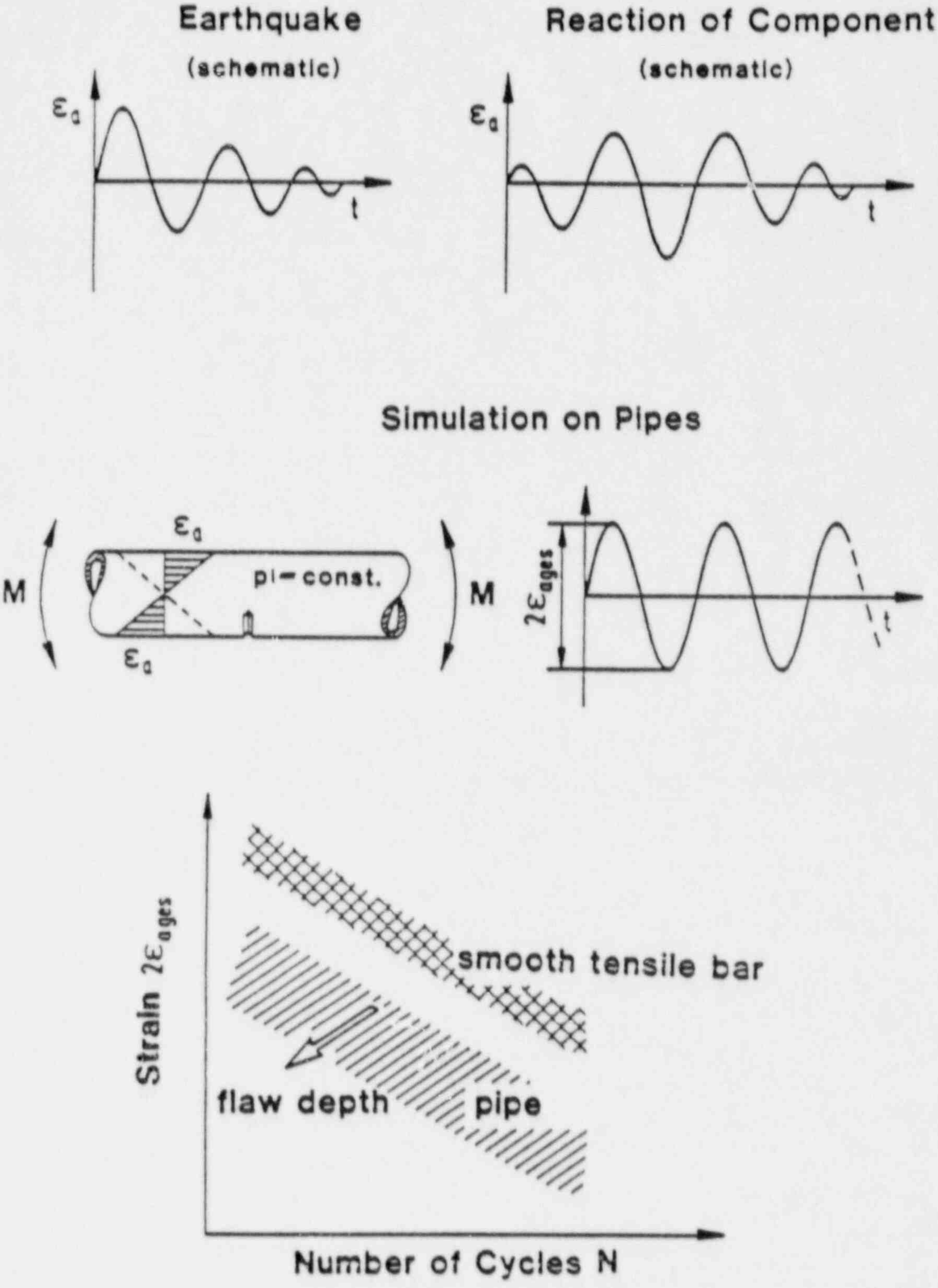
Fig. 30. Cyclic strain test



MPA 8995

Fig. 31. Earthquake simulation on pipes

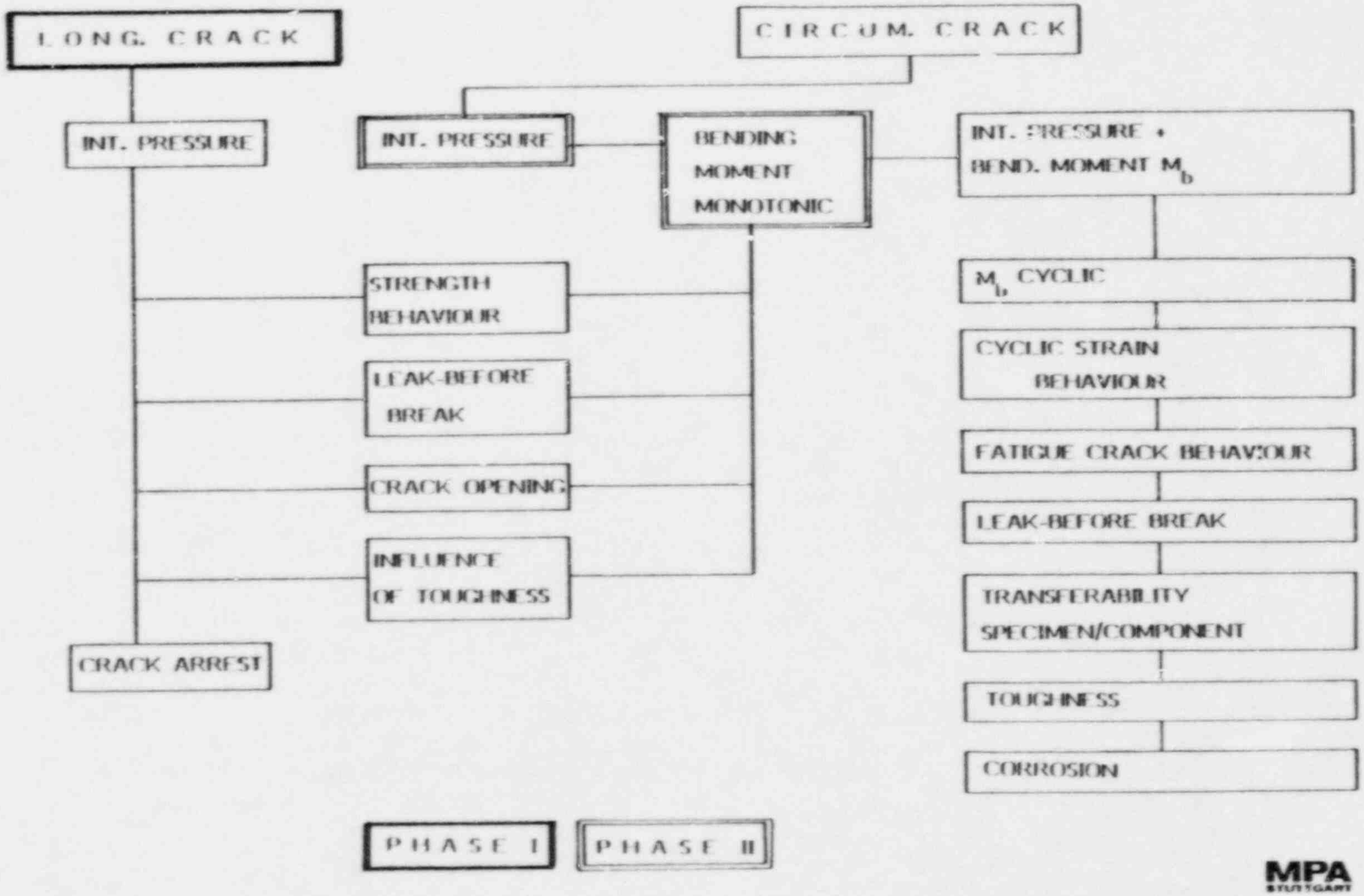


Fig. 32. Parameter structure of Leak-before Break Programmes at MPA Stuttgart

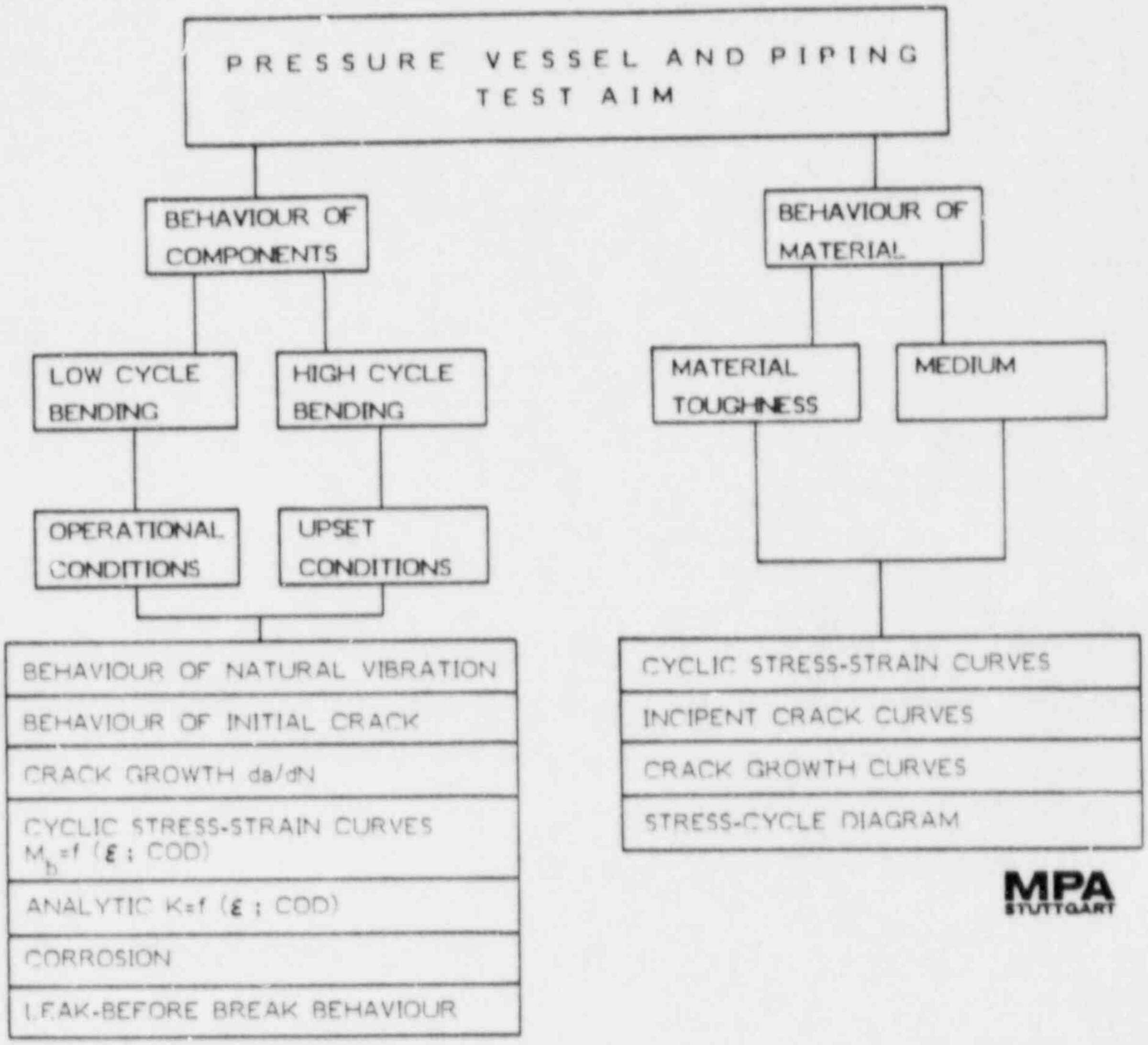


Fig. 33. Test aims of Leak-before Break Programmes at MPA Stuttgart

ANALYTIC

- LINEAR-ELASTIC FRACTURE MECHANICS

- DUCTILE FRACTURE MECHANICS
  - J-Integral

- PLASTIC LIMIT LOAD
  - FLOW STRESS
  - PLASTIC INSTABILITY
  - LIGAMENT STRESS

ADDITIONAL
- DYNAMIC OF STRUCTURE RS 477 GRS ADINA
- DYNAMIC OF FLUID/ -STRUCTURE SDK SAN

MPA
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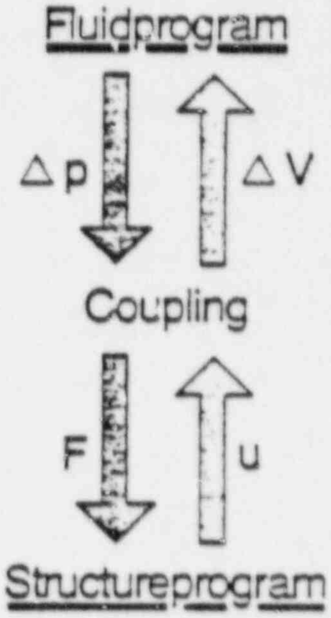
Fig. 34. Analytic within the Piping Research Programmes

Fluid – Structure – Interaction



Fluidprogram

Δp ΔV

Coupling

F u

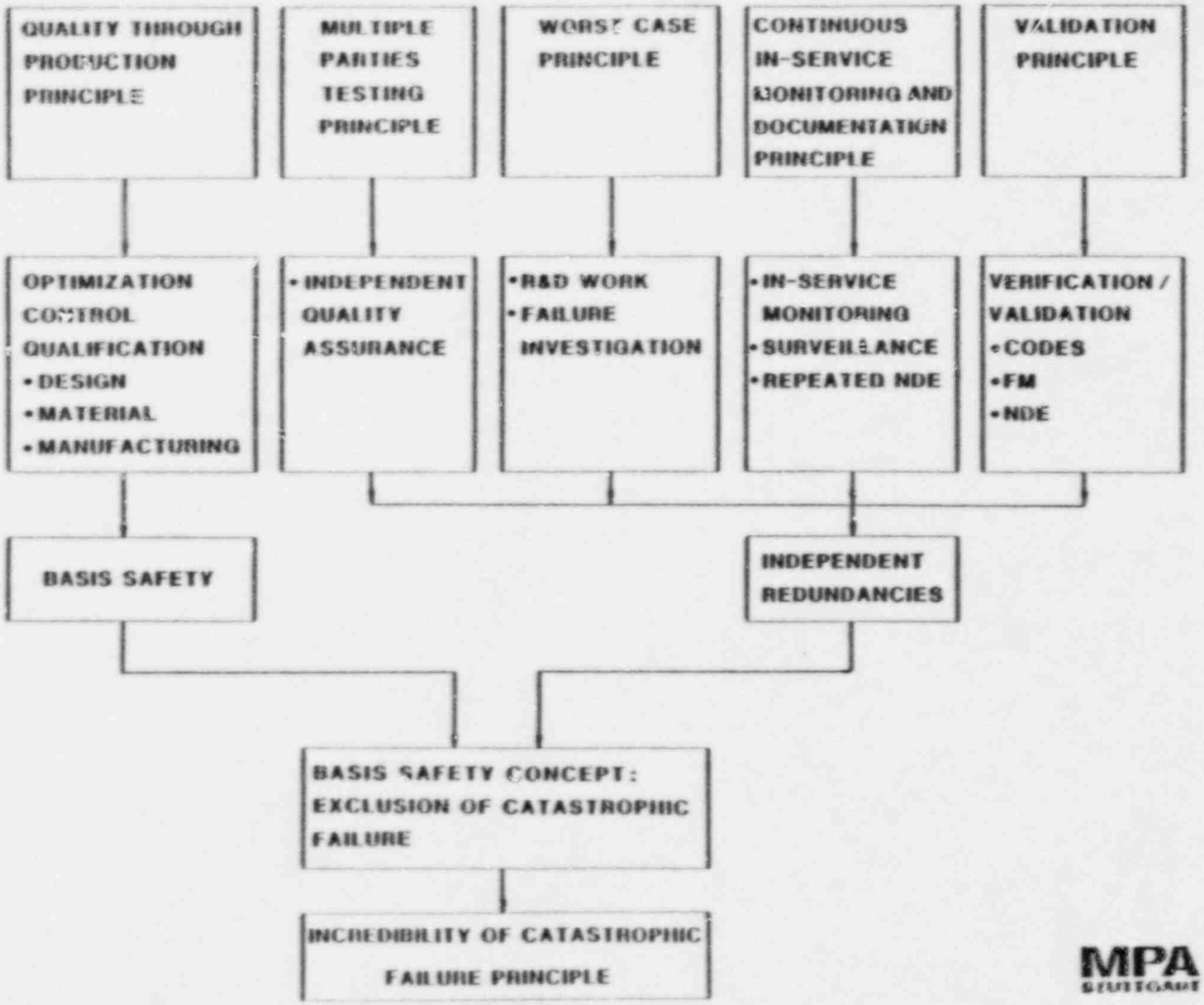
Structureprogram

DAPSY
Network Code
Method of Characteristics
Homogenous Non-Equilibrium

ASKA
Finite Element Code
Central Difference Method

1675K PROGRAM **DAISY**

Fig. 35. Programme DAISY



MPA 9004
STUTTGART

Fig. 36. Summary of the basis safety concept and the incredibility of catastrophic failure principle

ENEA-DISP APPROACH TO LEAK BEFORE BREAK
LATEST DEVELOPMENTS AND FUTURE SUBJECTS

P.P. Milella
HEAD, Division of Mechanical Analysis and Technology
ENEA-DISP

INTRODUCTION

The hypothesis of complete severance of a pipe, known as Double End Guillotine Break (DEGB), historically was introduced in order to design both containment and ECCS from a thermohydraulic stand point. The lack of advanced, reliable knowledge of pipe fracture behaviour strongly supported that hypothesis that was introduced as Design Base Accident.

The consequence of that assumption was that enormous jet impingments and reaction forces had to be considered in the design of piping and mechanical structures.

In particular large size high energy pipes had to be heavily restrained in order to prevent them from crushing over other systems or components, under the DEGB resulting thrust forces.

Also of concern was the sudden depressurization of reactor vessel and its impact on internals integrity.

This has led in the past to the installation of a large number of pipe whip restraints and jet impingment barriers detrimental to the inspectability of piping.

The set up of those protective structures resulted either in a drastic reduction in pipe inspection possibility or in additional doses to workers during plant autages to remove those restraints and barriers for accessibility and inspections.

This was stressing the need for a new definition of the Design Base Accident more realistic, yet conservative, and has led ENEA-DISP to start in 1981 a massive research program to study the fracture behaviour of pipes, in particular under degraded conditions.

The result of that activity is that for the next generation of PWR reactors, LBB based design criteria /1/ have been introduced in Italy. LBB will be applied to both primary and secondary systems, while DEGB will be used to design large components supports.

LEAK BEFORE BREAK. BASIC APPROACH

The approach to LBB is to demonstrate that even though a crack either exists or is generated in a pipe it will not grow, during 40 years of plant life, to break the pipe.

The flow diagram of figure 1 clearly depicts the basic approach to LBB.

Too many uncertainties were found to exist in that basic approach, as indicated in the following pages.

In particular it was recognized that common UT technique of sizing a crack in depth was not reliable at all on austenitic stainless steel and that fatigue crack growth mechanism was not well understood.

Such considerations suggested ENEA/DISP to address the problem from a more straightforward point of view.

It was not argued any more whether or not a crack existed in the pipe and of what size and how it would grow during plant life.

It was assumed, instead, that the crack was already grown throughout the thickness of the pipe.

This assumption relieves, in particular, uncertainty relative to UT sizing in depth and fatigue crack growth rate.

Under such limiting conditions only one question arises as to what crack size (this time only length) would become unstable under applied loads.

Since ENEA/DISP was thinking in terms of General Design Criteria to be applied to plants to be built for which specific stresses were not yet available, it was assumed to consider as reference applied loads the maximum allowable by Code, namely the ASME Sec. III.

ENEA research program, then, was focused mainly on the study of fracture behaviour of pipes containing longitudinal and circumferential through-wall cracks.

Figure 2 depicts the difference in the initial assumption between Basic Approach and ENEA/DISP Approach to LBB.

ENEA-DISP POSITION ON LBB

The results of ENEA research and study so far conducted on LBB have indicated that the most limiting cracks are the circumferential ones which may be broken open by the maximum ASME Sec. III allowable bending moment only if their length exceeds at least 140° of circumferential elongation. Figure 3 shows some experimental results on carbon steel supporting that position.

Circumferential cracks are also the most probable since ENEA/DISP requires for new plants the use of seamless pipe, both on carbon an stainless steel. Also elbows shall not contain longitudinal welds.

The goal, now, becomes the exclusion of such large crack being present or developed in the pipe or, better, in the circumferential welds, during plant operation.

This can be done by double inspecting pipe welds before the starting of plant operation and conducting an inservice program during operation.

First inspection, which can be defined as a pre-preservice is conducted in the shop where most of welds are performed.

The reason for that is that comfort, time and accessibility to weldments are available in the shop as opposite to the field and this allows a better inspection as well the resolution of possible doubts on UT indications.

Piping segments assembled and inspected in the shop are than shipped to the field and reinspected at the plant after hydrotest and hot non nuclear tests.

This procedure strongly reduces the probability of existence of a crack at the start of plant operation and, at the same time, introduces a clear picture of the initial condition of the welds for any future inspection.

Piping is then UT checked during scheduled outages. If an indication is found, during an inservice inspection, its size, only length since no credit is given to depth, is then compared to an allowable crack size obtained dividing by a safety factor of 4 the limiting 140° crack length as shown in figure 2. The result is that any indication smaller than 35° is not considered as safety related and may only lead to a leak.

Since the limiting crack size is of the order of 140° along the circumference it appears to be reasonable to assume as design leak area the area associated to that flaw.

Reaction forces to design piping systems are, then, calculated on the base of that area.

Experiments show that the leak area associated to a 140° crack reaches a maximum of 10% of the net cross section area and doesn't go further than a 5% of the cross section at the moment when stable tearing commences, as shown in figure 4 /2/.

With a thrust stemming out of a 10% leak area practically no restraint is needed.

This allows a better inspectability of the piping during scheduled outages. It also reduces the effects of jet impingment. Also the secondary system will be design with the above mentioned rules.

As far as inservice inspection and materials are concerned, secondary system has been upgraded to a primary system standard. Materials used for pipe construction will be produced under Quality Assurance procedures.

Small pipes and pipes with a radius to thickness ratio over 10 will not benefit LBB considerations.

Containment and ECCS design will continue to be based on the assumption of complete severance of a primary pipe. DEGB will also be used to design large component supports, such as pumps, SG and pressurizer, to provide adequate margins of safety on their stability.

Coolability and functionality of reactor core is of particular concern. ECCS shall be designed to exclude that the failure of a train could damage the functionality of all other trains.

UNCERTAINTIES IN BASIC LBB APPROACH

It has been mentioned earlier that uncertainties were found to exist on basic approach to LBB that have suggested ENEA/DISP to depart from it. This will be further discussed in this section.

A fundamental role in the basic LBB approach is played by UT sizing in depth, fatigue crack growth prediction and leak detection systems.

Those three factors are tightly related to assure that LBB is effective, as shown in figure 1, and unfortunately affected by uncertainties.

The unknowns are listed in annex 1. Reference is made to steps No 1, 2, 3, 4, 5, 6, 7, 8 of the flow diagram of figure 1.

In particular for step No 3 the fatigue crack growth rates normally used for SS and carbon steel are those of ASME Sec. XI.

It must be understood that those data are not design data. They are recommended to be used to verify the growth of a crack found in service.

The difference is essential. In the design phase a safety factor must always be introduced on data of experimental derivation while in the verification of something already built often there is little when no ground at all left to conservativism.

A typical example is ASME Sec. III fatigue curves where a factor of 20 is introduced on number of cycles N and a factor of 2 on allowable alternate stress S_a on the lower bound curve of all experimental results.

Point B of step No 3 in annex 1 underlines some factors enhancing fatigue crack growth rate.

They refer to carbon steel nevertheless they are quoted first because secondary systems are made out of carbon steel, secondly because, as pointed out at point A, the same extensive testing has not been conducted jet on SS.

Particularly, sulfur can have a stronger effect on SS than on carbon steel since the larger grain size of the former results in a greater concentration at grain boundary.

As far as porosity is concerned it shall be noted that most if not all fatigue crack growth rate measurements are run on CT specimens free, as much as possible, from such internal defects as porosity.

Designers are not as much interested in the fatigue behaviour of "clean" materials as in the responce of those in which a "dirty" surface is interested by a crack expecially because the two factors, crack and extensive defectology, are likely to be associated.

Point C of step No 3 enphasizes the fact that most fatigue crack growth rates obtained on carbon steel come from specimens which were behaving linear-elastic. This may not be the case for stainless steel in particular at high temperature and thin sections with deep cracks.

The plastic behaviour of the latter may results in greather crack growth rate.

On the other hand shallow cracks have been found to propagate faster than predicted by fatigue curves.

REFERENCES

/1/ ENEA/DISP, "General Design Criteria". Rev. April 86.

/2/ P.P. Milella, "Outline of Nuclear Piping Research Conducted in Italy", NUREG/CP 0072, Vol. 2, Feb. 1986.

/3/ W. Cullen et alt., "A Review of the Models and Mechanisms for Environmentally. Assisted Crack Growth of Pressure Vessel and Piping in PWR Environment", NUREG/CR 4422, Dec. 1985.

/4/ C.E. Pugh, "HSST Program Sem. Progr. Rep. for October 1985-March 1986", NUREG/CR 4219, Vol. 3, No 1, June 1986.

ANNEX 1

UNCERTAINTIES IN BASIC APPROACH TO LBB

STEP No 1 & 2

a) SEAMS ON PRIMARY SYSTEM APPROACH A LENGTH OF 100 m. FOR A 8-10 P[illegible] PROGRAM THE OVERALL LENGTH REACHES A 1000 m. WITH BRANCH [illegible] AND SECONDARY SYSTEMS WELDMENTS CAN BE JUDGED TO EXEDE SEVERAL KILOMETERS. THE POSSIBILITY OF HAVING A DEFECT ESCAPING UT INSPECTION CANNOT BE NEGLECTED.

b) UT INSPECTION CAPABLE OF SIZING A CRACK IN LENGTH BUT NOT IN DEPTH, PARTICULARLY ON STAINLESS STEEL.

STEP No 3

a) RELATIVELY FEW FATIGUE CRACK GROWTH DATA ON STAINLESS STEEL (SS) COMPARED TO CARBON STEEL

b) FATIGUE CRACK GROWTH RATE OF MATERIALS (CARBON STEEL) EXPOSED TO PWR PRIMARY COOLANT ENVIRONMENTS SUSCEPTIBLE OF ENHANCEMENT DUE TO:

- FATIGUE FREQUENCE (SEE FIG. 5)
- IMPURITIES CONTENT (PARTICULARLY SULFUR, SEE FIG. 6)
- R RATIO (SEE FIG. 7)
- ORIENTATION (SEE FIG. 8)

- POROSITY

c) FATIGUE CRACK GROWTH RATES ARE OBTAINED ON SPECIMENS BEHAVING LINEAR-ELASTICALLY. THIS MAY NOT BE THE CASE WHEN DEEP CRACKS ARE PRESENT IN SS. LARGER CRACK TIP PLASTIC ZONE CAN RESULT IN HIGHER RATES.
CARBON STEEL: $\Delta K = 40$ Mpa$\sqrt{m}$ requires $B = 2$ cm
FATIGUE CRACK GROWTH SPECIMEN SIZE $B = 5$ cm (2T-CT)

d) ACTUAL SHAPE OF A FATIGUE GROWING CRACK NOT WELL KNOWN (SEE FIG. 9)

STEP No 4

. LOAD SEQUENCE NOT DEFINIBLE "A PRIORI".
A CRACK ASSUMED TO GROW TRHOUGH THE THICKNESS MAY ELONGATE IN THE OTHER DIRECTION BECAUSE OF A SUDDEN OVERLOAD AND BECOME UNSTABLE (SEE FIG. 10)

STEP No 5

. LEAK AREA NOT WELL PREDICTED BY THEORETICAL MEANS

STEP No 6

- LEAK DETECTION SYSTEMS NOT RELIABLE

STEP No 7 & 8

- SAME UNCERTAINTIES AS IN STEPS No 3 & 4

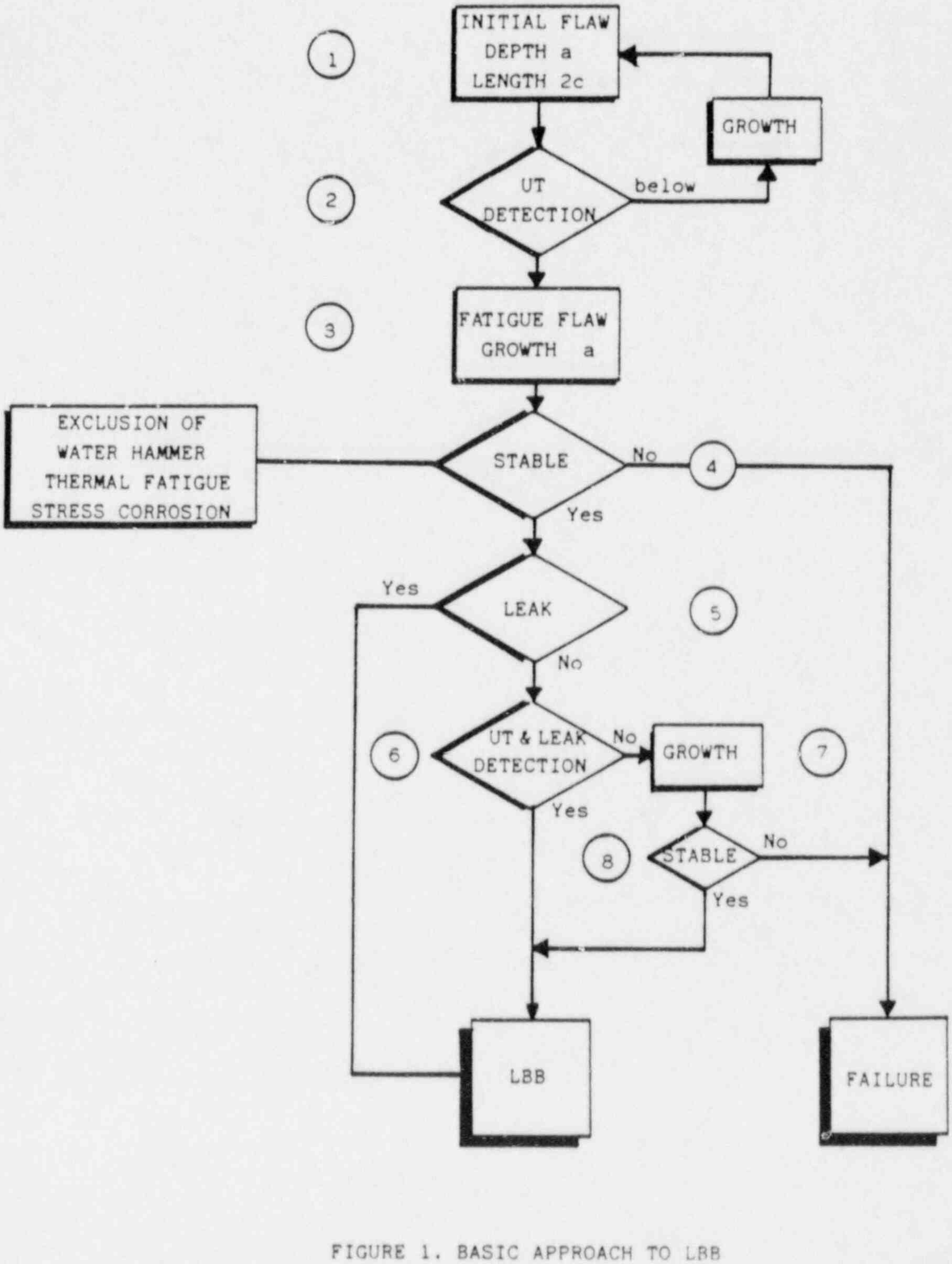


FIGURE 1. BASIC APPROACH TO LBB

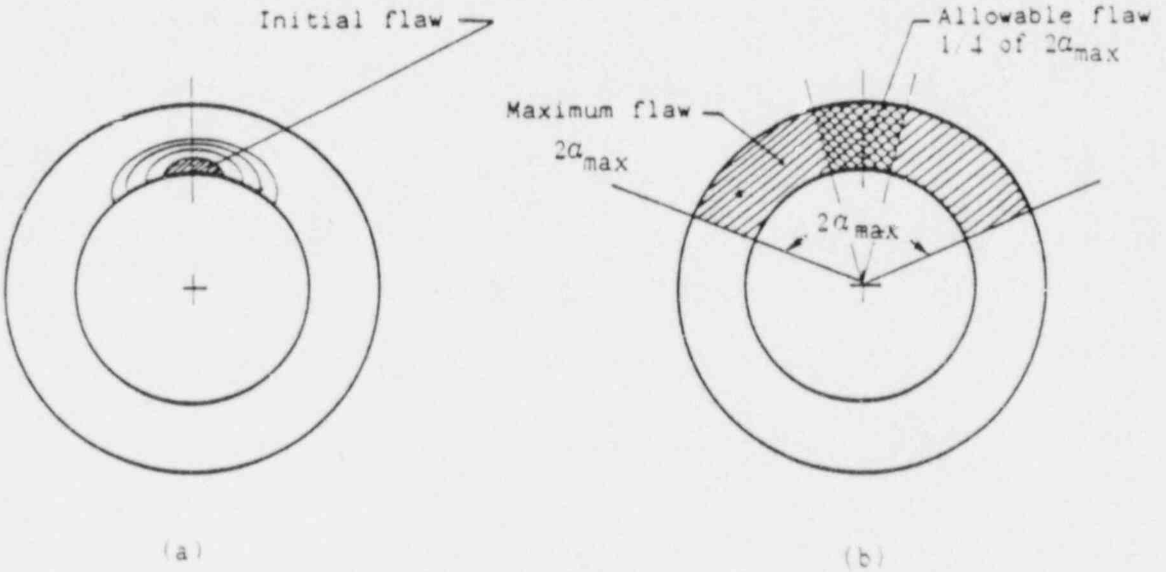


Figure 2. Difference between basic approach a) and ENEA/DISP approach b) to LBB. In a) the initial crack is assumed to grow through the thickness, in b) it is considered to have already gone throughout the thickness

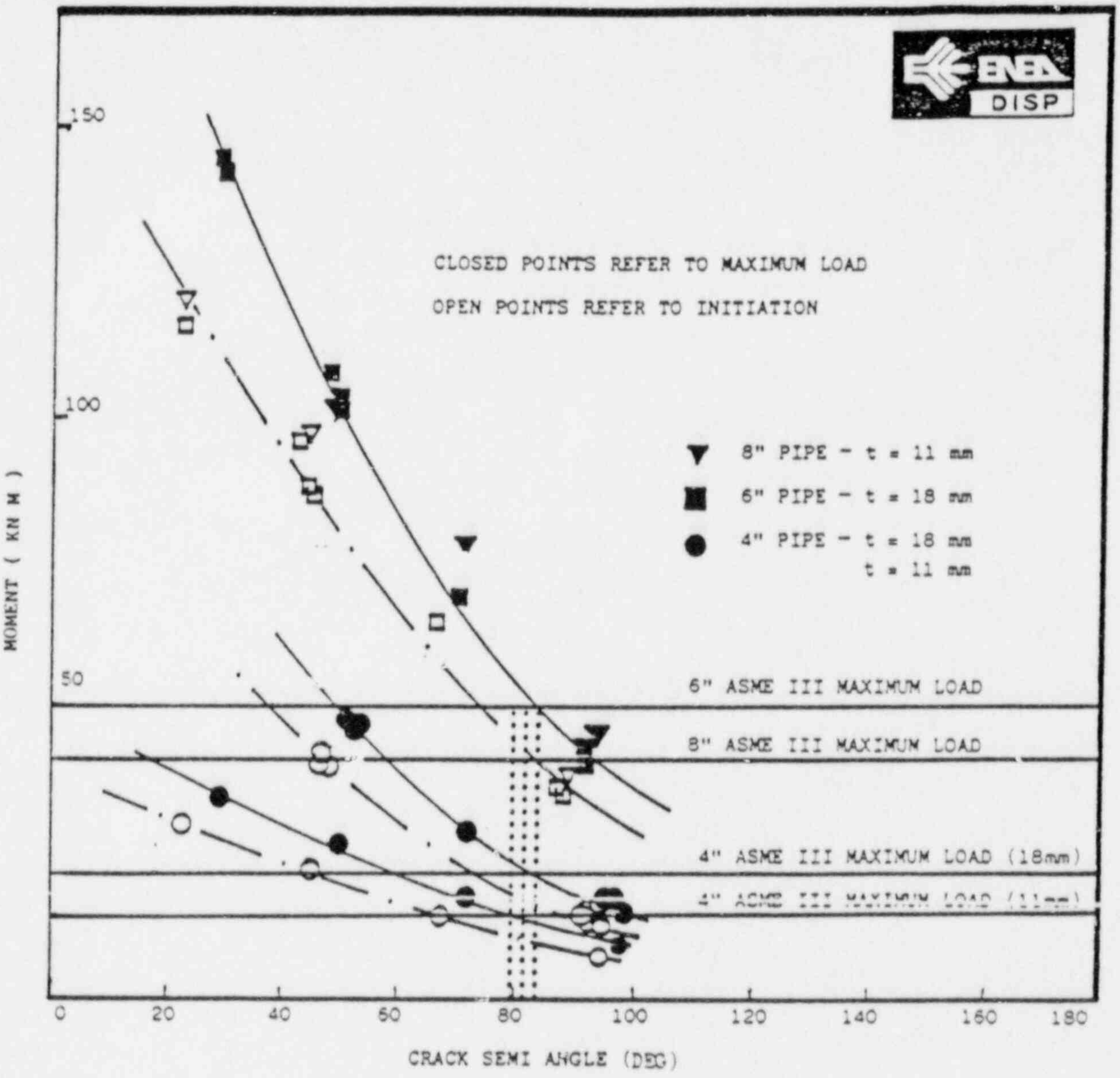


FIGURE 3 EXPERIMENTAL RESULTS SHOWING THE MOMENT AT INITIATION AND AT MAXIMUM LOAD FOR DIFFERENT PIPE SIZES VERSUS THROUGH WALL CRACK SEMI ANGLE.

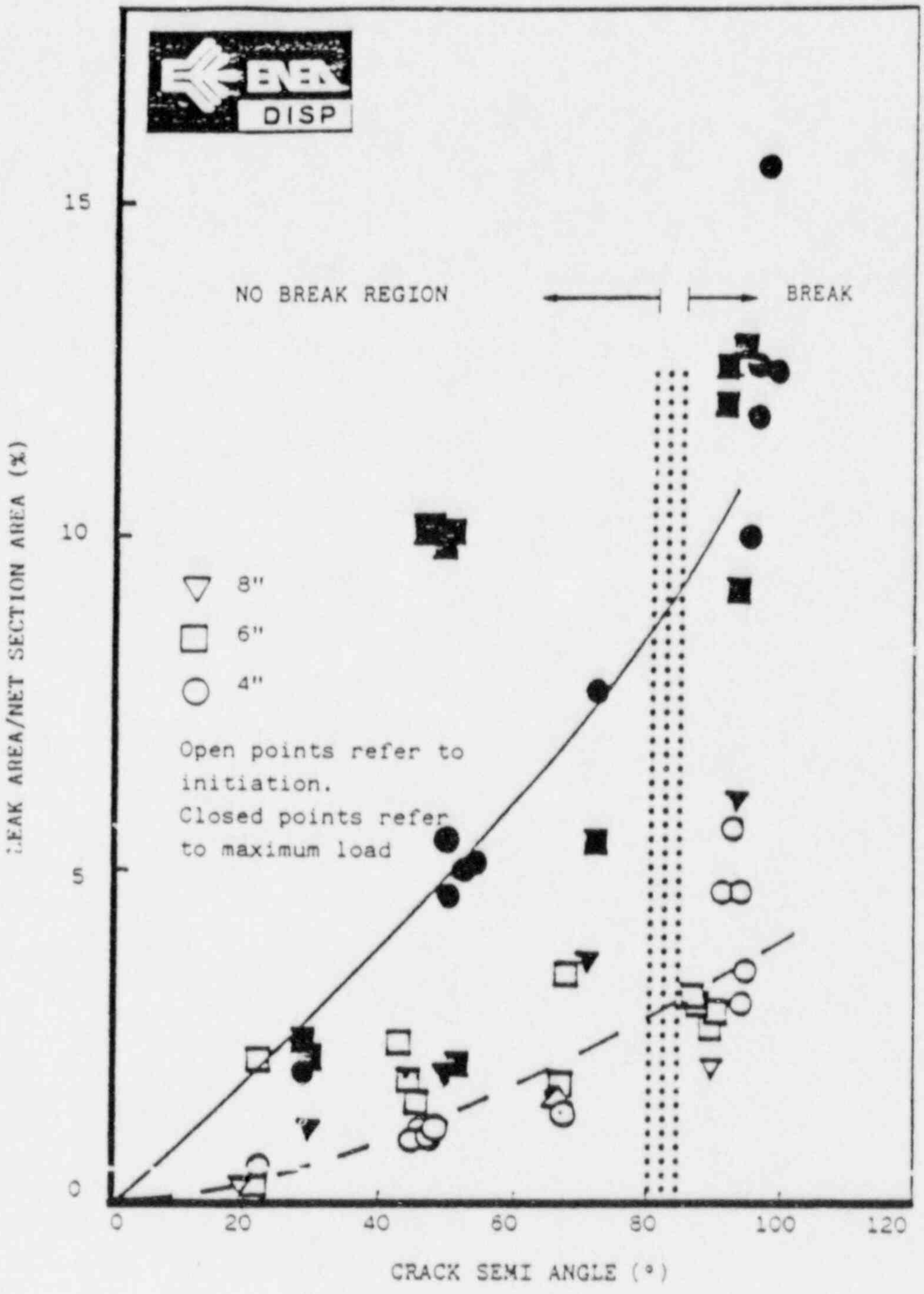


FIGURE 4 . PLOT OF LEAK AREA TO NET CROSS SECTION AREA AS FUNCTION OF CRACK SEMI ANGLE

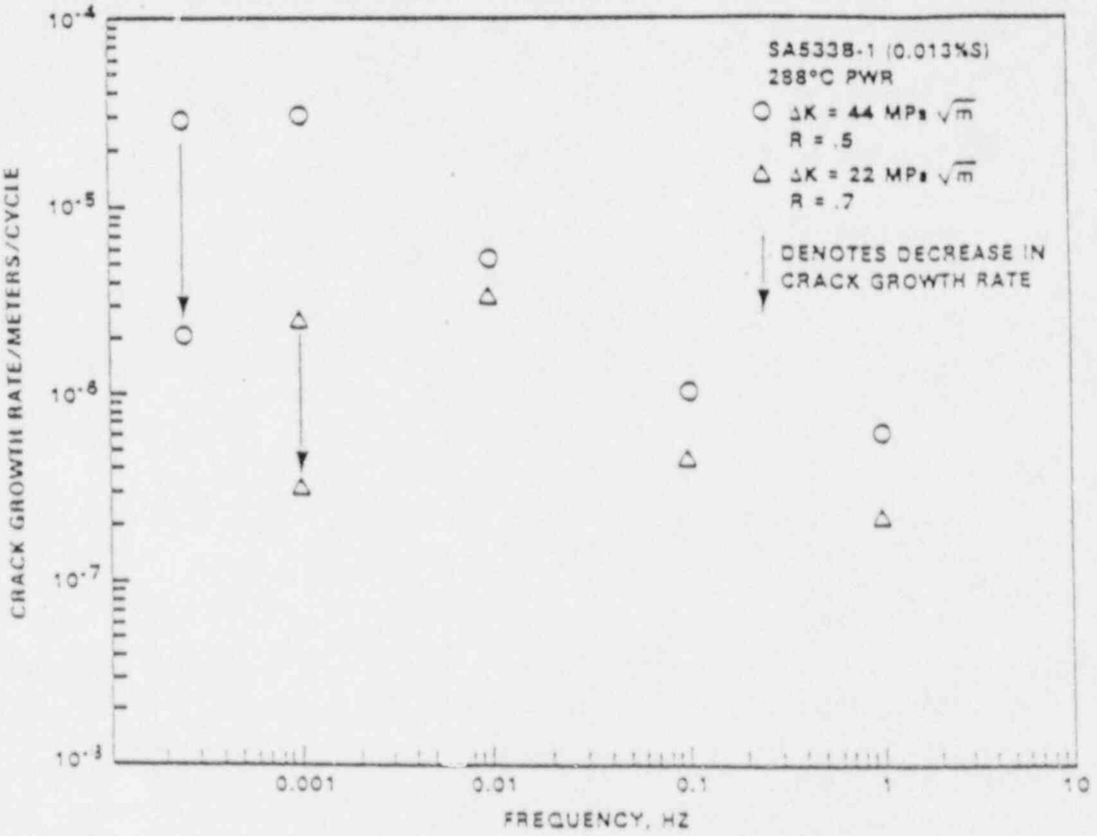


Figure 5. Effect of frequency on fatigue crack growth rate of A 533 B carbon steel.(Reference 3)

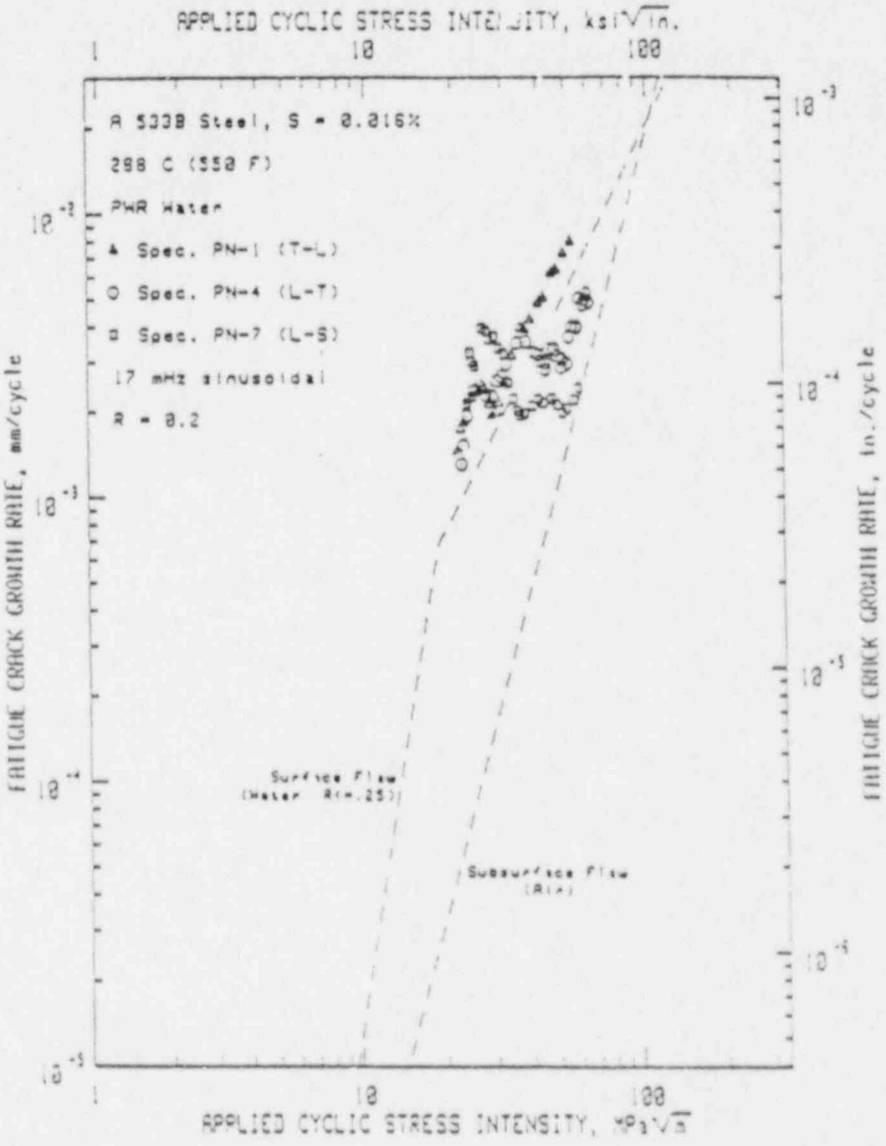


Figure 6. Effect of sulfur (medium content) on fatigue for A 533 B carbon steel.(Reference 3)

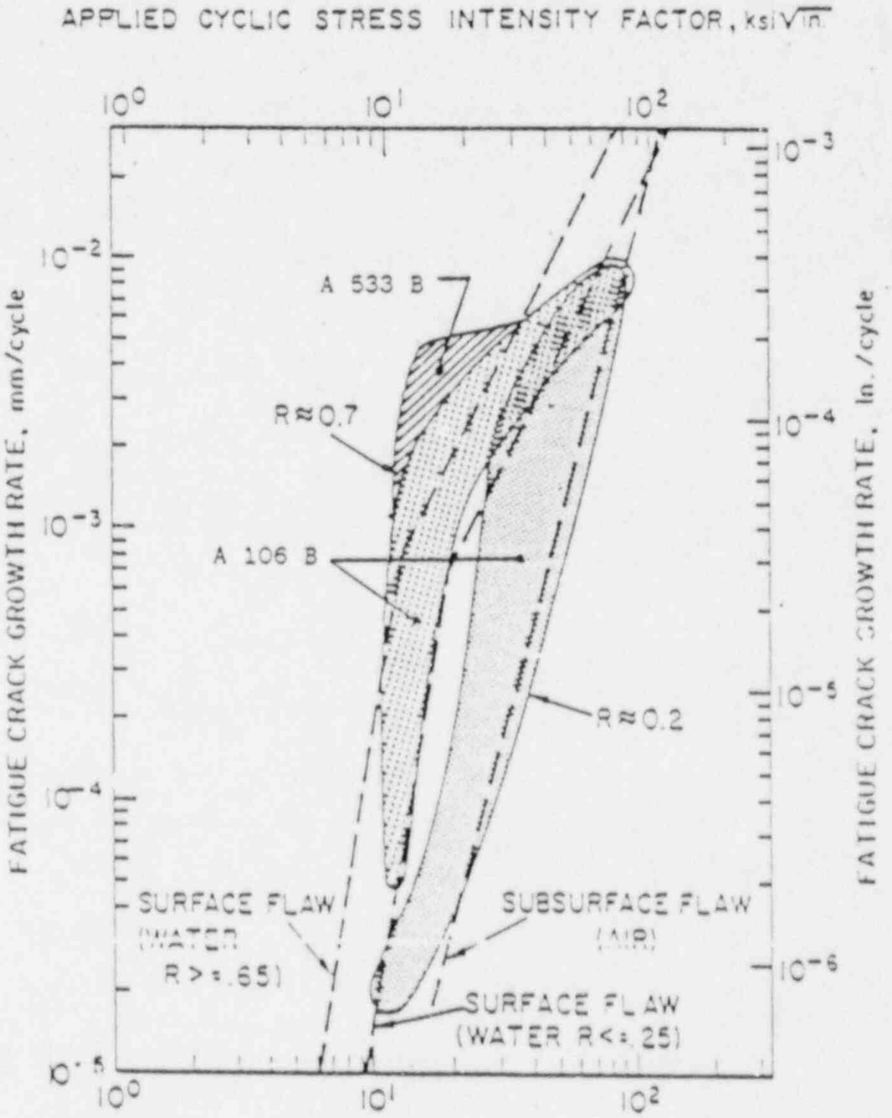


Figure 7. Effect of R ratio on fatigue crack growth rate of A 106 B and A 533 B carbon steel (Reference 3,4)

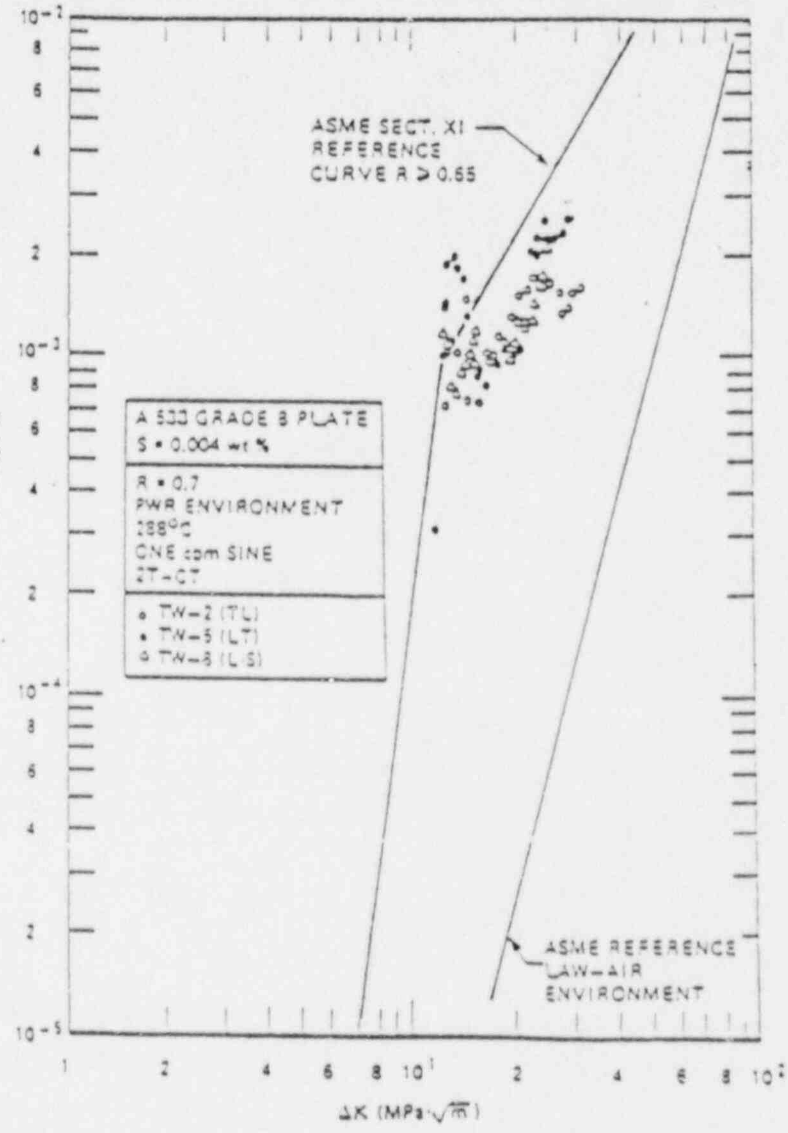


Figure 8. Effect of orientation on fatigue crack growth rate of A 533 B.(Reference 4)

CRACK GROWS ALTERING ITS
ORIGINAL ASPECT RATIO

CRACK GROWS KEEPING ITS
ORIGINAL ASPECT RATIO

Figure 9. The actual shape of a growing crack is not well known.

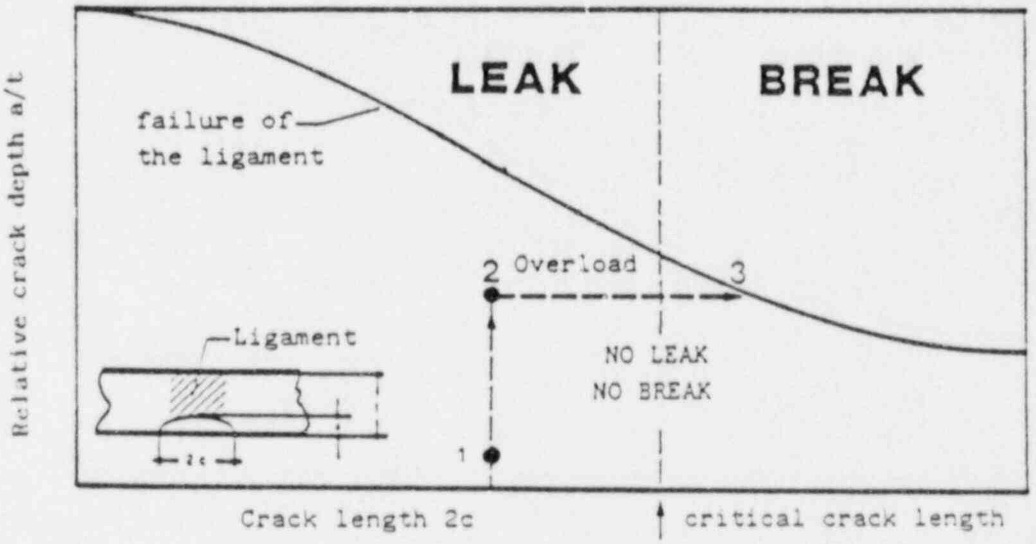


Figure 10 .Overload (Seismic) applied to a pipe containing a crack grown to 2 may cause failure

RECENT RESULTS AND FUTURE PROGRAMS
ON PIPE FRACTURE TESTS IN ITALY

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INTRODUCTION

The purpose of this paper is to present the continuation in 1986 of the ENEA research program on fracture behaviour of carbon and stainless steel pipes started in 1981.

Previous work, from 1981 to 1985, has already been outlined /1/.

In addition the paper will be also presenting future programs on the matter that represent a remarkable improvement over the past in that it is aimed to address the problem of fracture resistance of piping systems from a broader point of view that includes, in particular, dynamic loads, elbows and flanges behaviour, jet forces and leak detection systems.

The objective of the program is to improve elastic-plastic fracture mechanics knowledge to assess pipe fracture behaviour in any degraded condition and under any operating or accident condition that can be expected during plant life and develop reliable leak detection capabilities to pick up an early sign of failure.

The final goal is to further support new design criteria, such as leak before break (LBB) already introduced in Italy /2/, studying in depth the possible piping and system response to any adverse condition.

ENEA RESEARCH PROGRAM IN 1986. SIGNIFICANT RESULTS

Tests on pipes have been continued in 1986. Experiments have interested carbon steel pipes of 4", 6" and 8" diameter of A 106 B and austenitic stainless steel pipes, type 316, of 12" diameter.

As in the previous years, pipes containing through-wall cracks were loaded in pure bending as shown in figure 1.

A complete picture of all tests run in 1986 is shown in the test matrix of table 1.

This time we also checked the behaviour of carbon pipes containing small cracks or no crack at all in order to verify the Modified Net Section Collapse Load criterion proposed by ENEA (ENEA MNSCL).

Results from previous research programs were showing that for carbon steel with R/t (Radius over thickness) lower than 6, instability was reached under moments higher than those predicted by the NSCL.

This is presented in figure 2 where experimental results are shown in a diagram applied stress versus crack half angle. The applied stress represents nominal stress far from crack area. Solid line in figure 2 is the theoretical stress versus crack half angle according to NSCL criterion.

The ENEA MNSCL criterion was based on the observation that before instability the crack was growing tearing the material and therefore at the tip of the crack stress should have reached the ultimate value σ_u.

This maximum value, σ_u, dies out at a distance λ from the crack tip which is proportional to $\sqrt{\alpha Rt}$ were α is the crack half angle.

A schematic of ENEA MNSCL is shown in figure 3.

Obviously, since the increment of stress and therefore of resistance capability is function of λ, i.e. of crack length, by decreasing crack length its influence should diminish and completely disappear in a pipe without crack.

Tests have confirmed this prediction as shown in figure 4, where experimental results (open and closed points) are shown compared to theoretical NSCL curves (solid lines).

As it can be seen for 6" - 18 mm thick pipe ($R/t = 4$) and 4" - 18 mm pipe ($R/t = 2.6$) experimental results are over theoretical prediction for large cracks. As crack approaches zero length the upward experimental trend bends down matching the theoretical prediction of NSCL.

It can be also seen how experimental results for 8" - 11 mm thick pipes, with $R/t = 10$, agree very well with theoretical prediction for any crack lenght.

Another point of interest stems out of tests carried on carbon steel pipes at 300°C.

This time, as shown in figure 5, both 6" - 18 mm thick and 8" - 18 mm thick pipes perform below the NSCL prediction.

This is due to a dramatic toughness drop with the temperature probably due to a dynamic strain aging effect on the material.

Table 2 shows J_C values and tearing modulus obtained on A 106 B specimens at RT and 280°C.

For such low values of J_C the ENEA MNSCL criterion cannot be applied for any crack length since the value of λ is greater than the extension of the plastic zone itself ahead of the crack tip. This is physically impossible since λ should be a fraction of the plastic zone.

The probable dynamic strain aging effect can be better seen in figure 7 where experimental load versus displacement is shown for two 6" diameter carbon steel pipes containing a through wall crack of 140° and 150° tested at room temperature and 300°C respectively.

It can be seen the decrease of maximum load and, particularly, displacement for the pipe tested at 300°C indicative of a transition from ductile to semi-ductile behaviour.

Another important finding is related to the opening area of the crack during the loading phase up to the maximum load.

Measurements based on experimental COD have been compared to theoretical predictions making use of Tada equation /3/.

Results are shown in figure 8 for 8" diameter, 11 mm thick stainless steel pipes /4/.

It can be seen that Tada equation tends to overpredict the actual opening area in particular at higher loads. Linear elastic fracture mechanics is well applicable up to a bending moment equal to 40% of the theoretical NSCL.

Best prediction of the actual leak area can be obtained by using the Tada equation with the yield stress instead of the flow stress and a plastic constraint factor equal to 6.

FUTURE PROGRAM

A new research program has been prepared by the Division of Mechanical Analysis and Technology of ENEA-DISP.

The new three-year program will be presented for approval in june 1986 and start in 1988.

The program, that addresses the problem of piping integrity from a broader point of view, is broken down in the following 6 Actions:

ACTION 1: TESTS ON CARBON AND STAINLESS STEEL PIPES UNDER STATIC BENDING LOADS AND MATERIAL PROPERTY DATA BASE

ACTION 2: TESTS ON CARBON AND STAINLESS STEEL PIPES UNDER DYNAMIC LOADS AND MATERIAL PROPERTY DATA BASE

ACTION 3: TESTS ON CARBON AND STAINLESS STEEL PIPES UNDER

PRESSURE LOADS

ACTION 4: THEORETICAL ANALYSIS OF EXPERIMENTAL RESULTS OF ACTIONS 1, 2 AND 3 AND DEVELOPMENT OF LBB CRITERIA

ACTION 5: DEVELOPMENT OF LEAK DETECTION SYSTEMS

ACTION 6: DEVELOPMENT OF UT INSPECTION ON SS PIPES.

In particular:

ACTION 1 is a continuation of programs so far developed with new features. Pipes up to 30" diameter and 70 mm thickness will be tested under bending moments.

It will also study the fracture behaviour of full scale elbows, tees and flanges.

ACTION 2 is a new research focused to understand the real impact of dynamic and seismic loads on degraded pipes. It must be understood the actual action of seismic loads and whether or not they can be considered as dynamic. If pipe behaviour is ductile the yield strength should be playing a fundamental role: the higher the yield strength the better. Any increase in the yield strength due to a dynamic effect could possibly lead to an increase of fracture resistance if toughness continue to be sufficiently high.

REFERENCES

/1/ P.P. Milella, "Outline of Nuclear Piping Research Conducted in Italy", Nuclear Engineering and Design, 98 (1987), pp. 219-229.

/2/ P.P. Milella, "ENEA-DISP Approach to Leak Before Break. Latest Development and Future Subjects", ENEA-DISP Technical Report ACO/ATEM-4/DT(87).

/3/ US NRC, NUREG/CR-3464.

/4/ C. Maricchiolo, P.P. Milella, "Leak Area Evaluation on Circumferentially through-wall Cracked Pipe Tests under Pure Bending Conditions", ENEA-DISP Technical Report ACO/ATEM DT-4(86).

TABLE 1

ENEA TEST MATRIX - 1986

SA-106B

TEST CODE	OUTSIDE DIAMETER (MM)	THICKNESS (MM)	TYPE OF LOAD	CRACK TYPE	CRACK LENGTH DEG	CRACK DEPTH A/T	TEMP. DEG C
4A1IV30	114.3	17.12	B	C TW	30	1	RT
4A2IV60	114.3	17.12	B	C TW	60	1	RT
4A3IV135	114.3	17.12	B	C TW	135	1	RT
6B3IV	168.3	18.26	B	-	-	-	RT
6A1IV20	168.3	18.26	B	C TW	20	1	RT
6A2IV140	168.3	18.26	B	C TW	140	1	RT
6A3IV240	168.3	18.26	B	C TW	240	1	RT
6B1IV60	168.3	18.26	B	C TW	60	1	300
6B2IV90	168.3	18.26	B	C TW	90	1	300
6C1IV120	168.3	18.26	B	C TW	120	1	300
6C3IV150	168.3	18.26	B	C TW	150	1	300
6C2IV180	168.3	18.26	B	C TW	180	1	300
8A1IV60	219.1	18.26	B	C TW	60	1	300
8A2IV90	219.1	18.26	B	C TW	90	1	300
8A3IV120	219.1	18.26	B	C TW	120	1	300
8B1IV150	219.1	18.26	B	C TW	150	1	300
8B2IV180	219.1	18.26	B	C TW	180	1	300

SS-316

TEST CODE	OUTSIDE DIAMETER (MM)	THICKNESS (MM)	TYPE OF LOAD	CRACK TYPE	CRACK LENGTH DEG	CRACK DEPTH A/T	TEMP. DEG C
121I40	323.9	17.48	B	C TW	40	1	RT
122I50	323.9	17.48	B	C TW	50	1	RT
123I60	323.9	17.48	B	C TW	60	1	RT
124I90	323.9	17.48	B	C TW	90	1	RT
125I180	323.9	17.48	B	C TW	180	1	RT

B: BENDING

C TW: CIRCUMFERENTIAL THROUGH-WALL

C PT: CIRCUMFERENTIAL PART-THROUGH

TABLE 2

Campione	J_{Ic} (KN/m)	K_{Jc} (MN/m$^{3/2}$)	TEARING MODULUS	Temp. (°C)
A4	334.03	277.78	570.8	20
A6	200.15	215.02	322.1	20
B6	236.29	233.63	275.3	20
C6	259.12	244.66	281.0	20
B6T	40.40	93.07	139.8	280
C6T	50.67	104.23	187.0	280
A8T	52.68	106.28	219.4	280
B8T	65.28	118.30	247.9	280

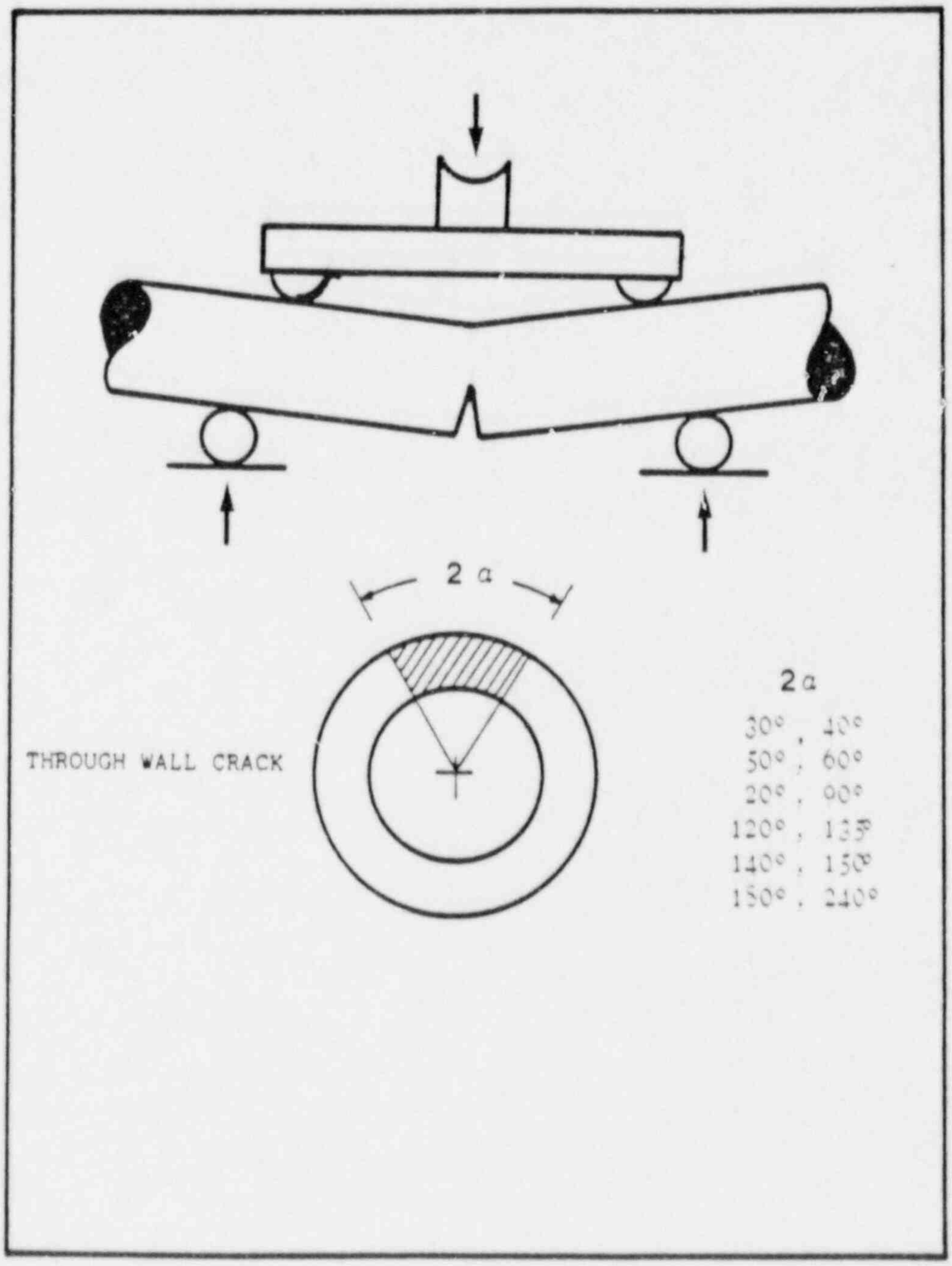


Figure 1 . Four point bent test method (pure bending), through crack

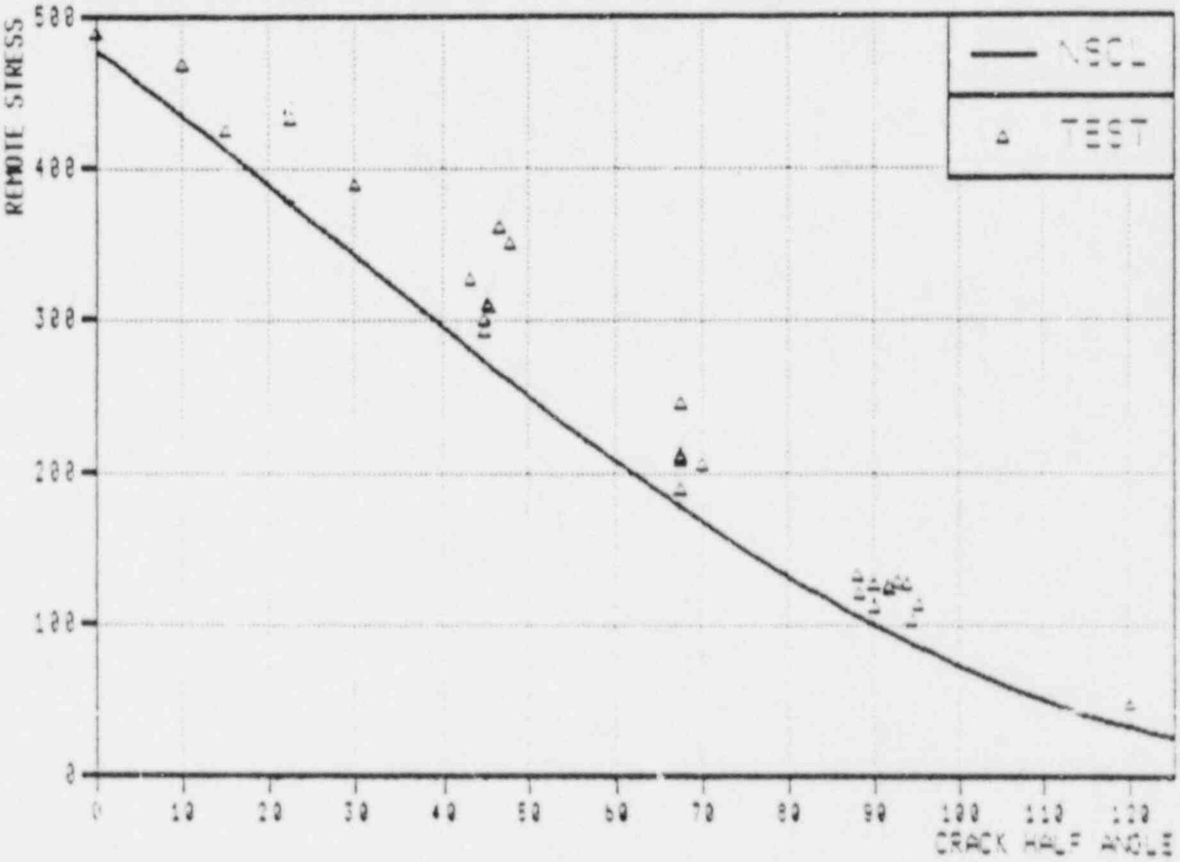
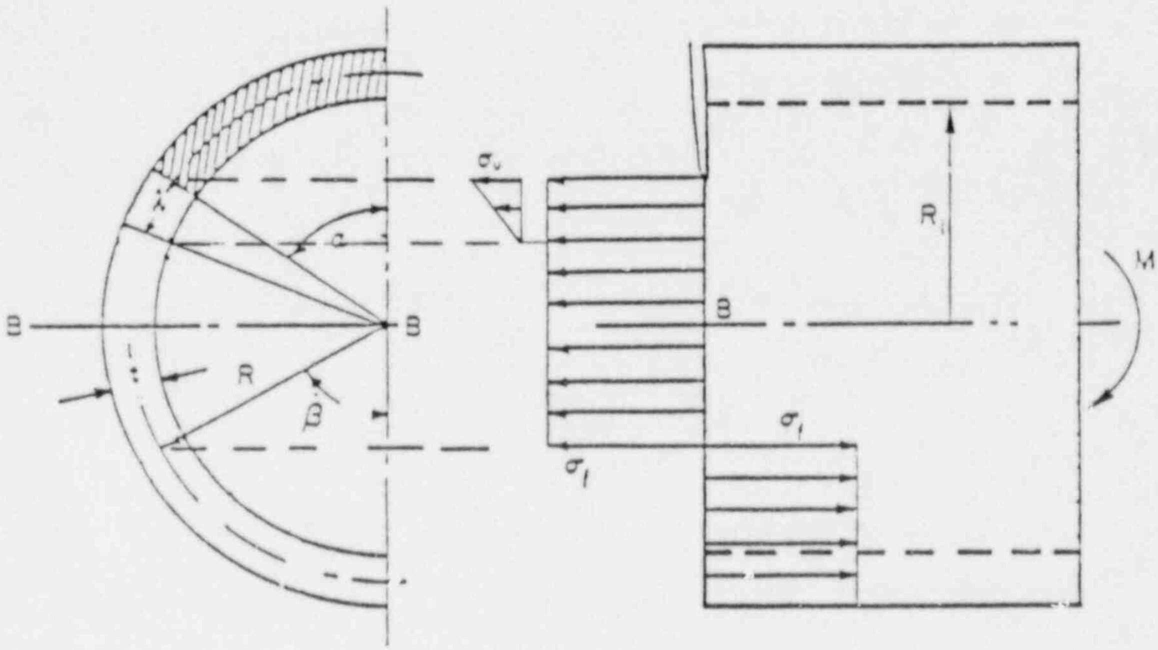

REMOTE STRESS
500
400
300
200
100
0
0
10
20
30
40
50
60
70
80
90
100
110
120
CRACK HALF ANGLE
NSCL
TEST

FIGURE 2



$$M_{max} = 2\sigma_f R^2 t \,(2 \sin\beta - \sin\alpha) + \sigma_c \lambda R t \left[\cos\left(\alpha + \frac{\lambda}{3R}\right) - \cos\beta\right]$$

$$\sigma_f = \frac{\sigma_{ys} + \sigma_u}{2}$$

$$\sigma_c = \sigma_u - \sigma_f$$

$$\lambda = \sqrt{a R t}$$

THE NET SECTION COLLAPSE LOAD CRITERION AND ITS MODIFIED VERSION

FIGURE 3

A 106 B - 25° C

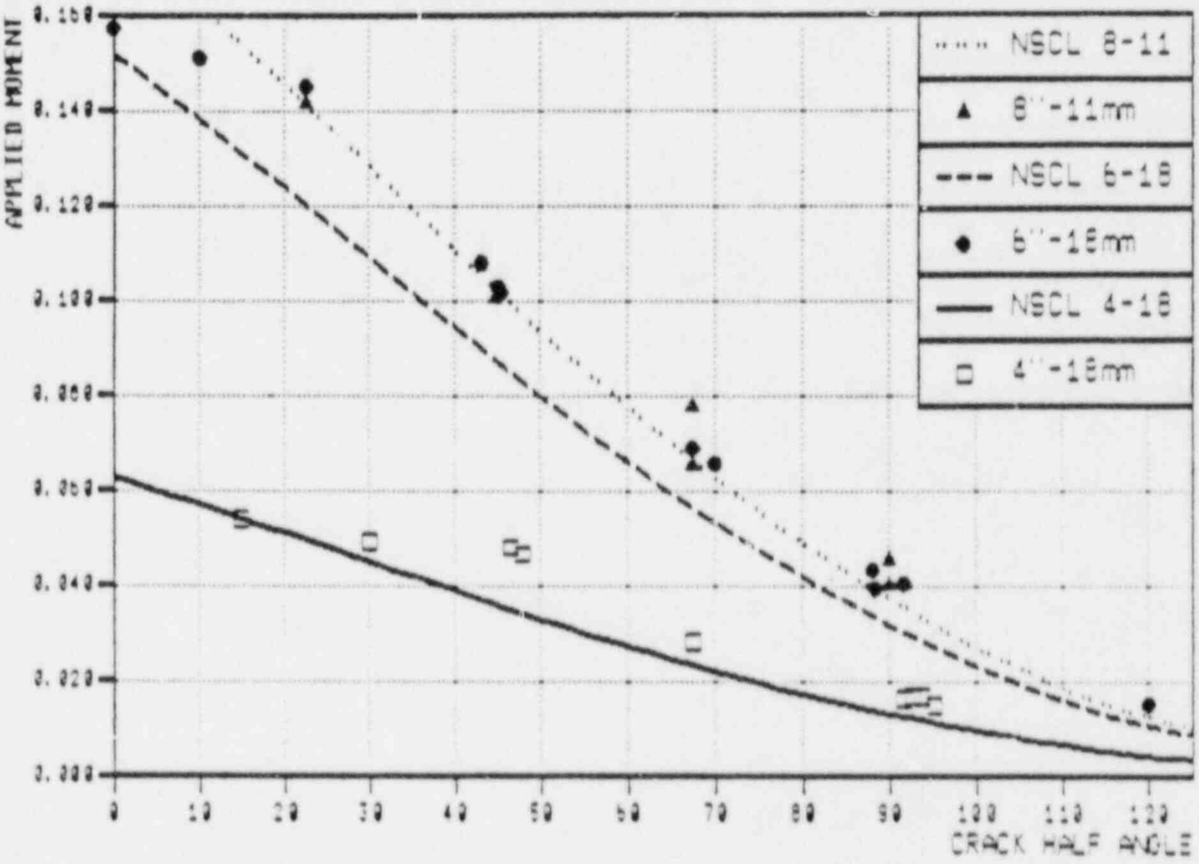


FIGURE 4

A 106 B - 300° C

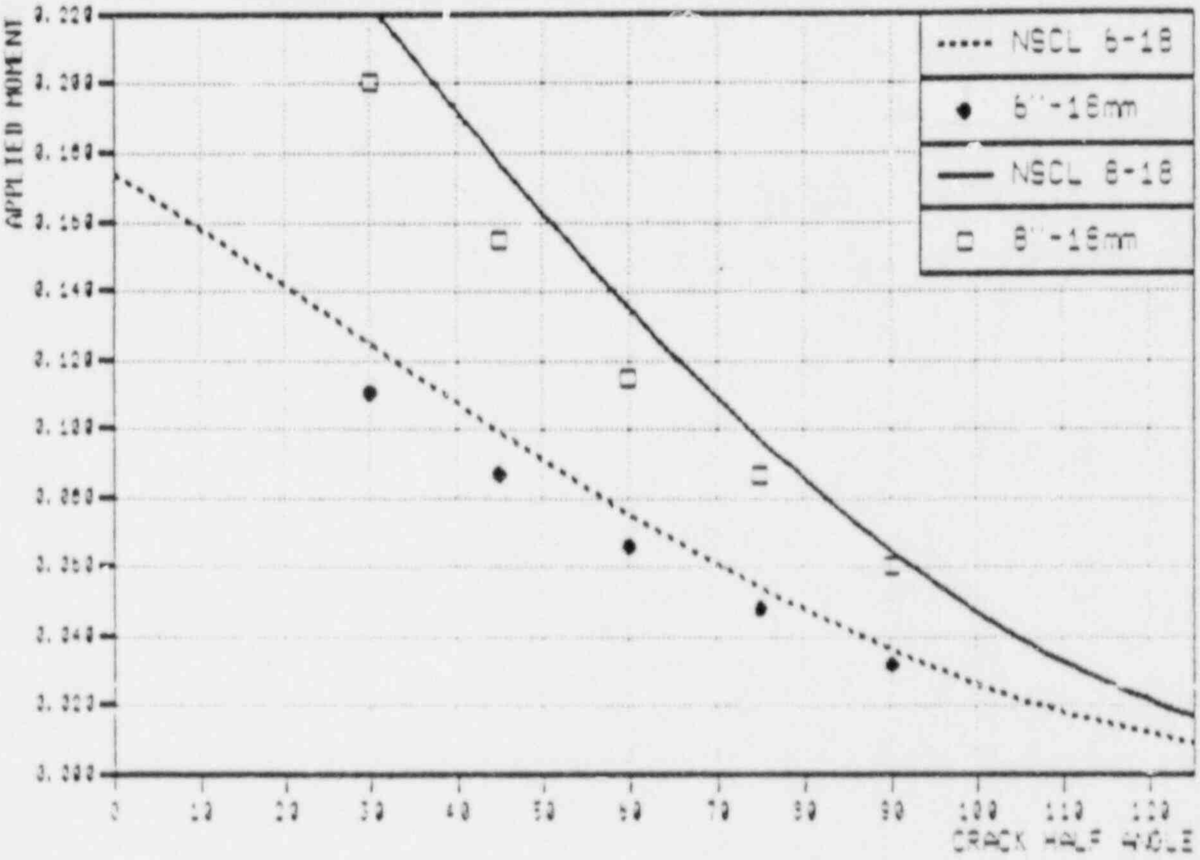


FIGURE 5

Figure 6 not used in text

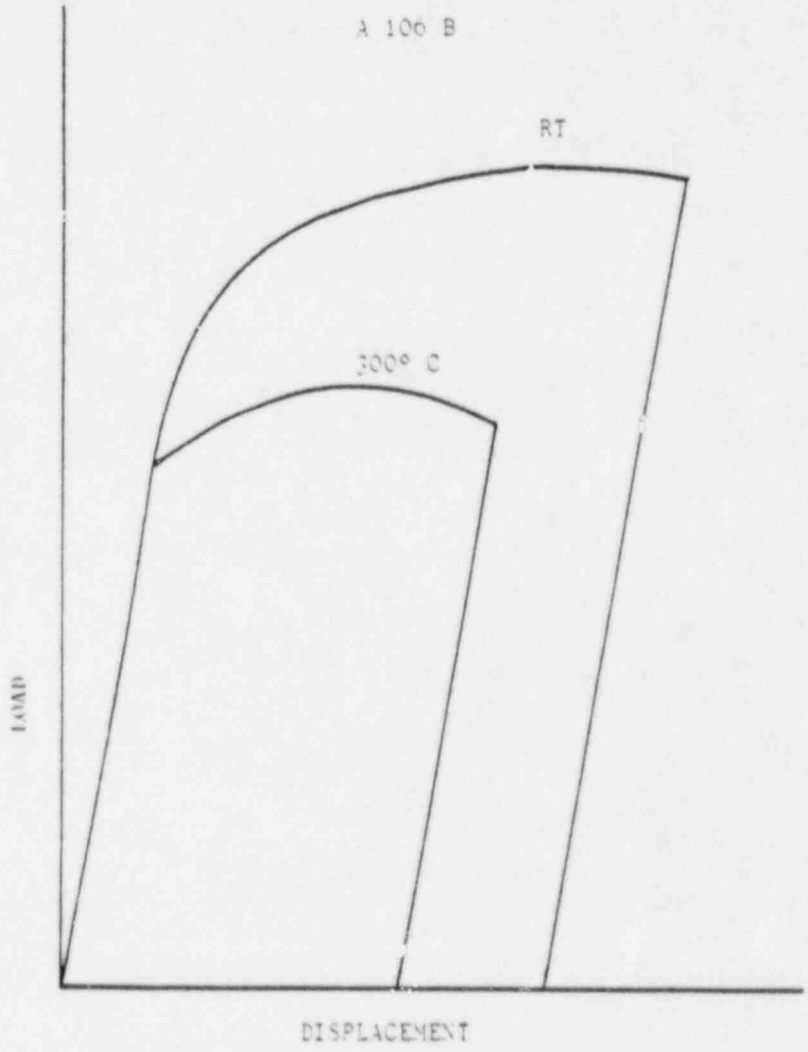
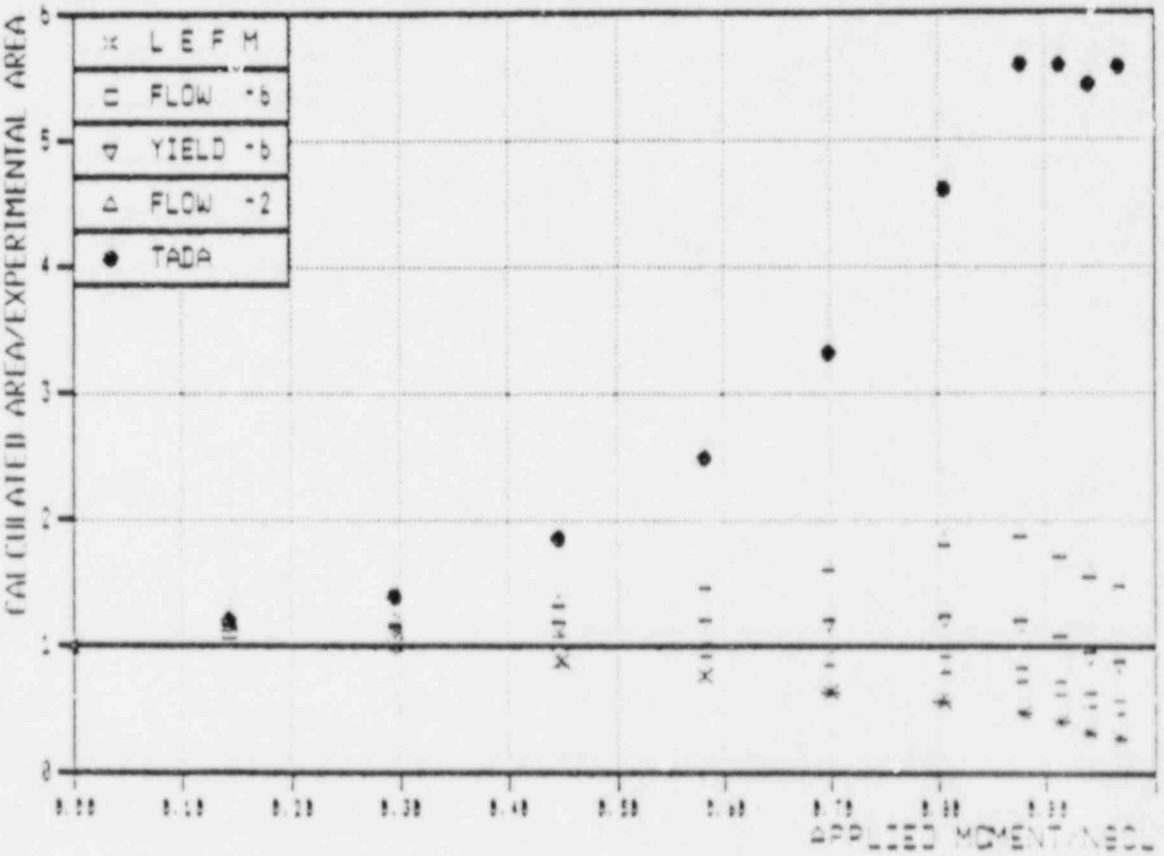


FIGURE 7



A 106 B - 8",18 mm

FIGURE 8

Progress of Ductile Pipe Fracture Test Program at JAERI

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June , 1987

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Abstract

At JAERI, a ductile pipe fracture test program has been conducted as a part of an extensive piping research program, and the ductile fracture behaviors of circumferentially cracked stainless steel and carbon steel piping have been investigated at room temperature, 300 °C and BWR environment. In this program, dynamic test and leak test are also included. The paper summarizes the outline and future plans of the program as well as the pipe bending test results conducted at room temperature and 300 °C using 3-, 6- and 12-inch diameter type 304 stainless steel and carbon steel pipes with a through-wall or a part-through crack in the circumferential direction.

Two pipe bending test machines which installed a high compliance disk spring device were constructed to perform 6-inch and 12-inch diameter pipe test under compliant bending load.

Pipe fracture data obtained were compared and discussed with regard to the validity of the net-section collapse criterion, safety margin of the flaw acceptance criteria for austenitic stainless steel piping in ASME Code Sec. XI, and the applicability of simple fracture parameter estimation schemes which were developed by EPRI-GE and NRC.

A reasonable agreement was observed between the predicted net-section collapse load and test results of through-wall cracked pipes. However, for part-through cracked pipes, agreement was less accurate than the case of through-wall cracked pipes in general. Especially the predicted load became unconservative when the crack depth was more than 90% of wall thickness.

Using the test results of 6-inch diameter stainless steel pipes of base metal and GTAW joint, the flaw acceptance criteria of the ASME Code Sec. XI, IWB 3640 was examined, and it was shown that the criteria satisfy the expected safety factor.

Test result of 6-inch diameter stainless steel and carbon steel pipes were also compared with the predicted J-value and COA(Crack Opening Area) obtained from estimation scheme developed by EPRI-GE and NRC. EPRI-GE estimation scheme gave larger J and COA values than those obtained from test results, while NRC procedure gave improved predicted values.

In addition to the above results, an evaluation procedure for critical pipe length and supporting conditions at the onset of instability is presented using an empirical expression obtained from pipe test results. It is shown, for instance, that the critical length of 24-inch diameter Sch. 80 stainless steel pipe is more than 140 m long under the displacement controlled 3-point bending load.

As a part of ductile pipe fracture test program, a leak test has been started from FY 1987, in which the influence of small jet through a through-wall cracked pipe including a leak rate measurement will be investigated from FY 1988.

1. Introduction

At JAERI, an extensive piping research program has been conducted, which is directed towards demonstrating the integrity of LWR piping during plant life and also the safety of nuclear plant even under the postulated pipe break accident. The ductile pipe fracture test program described in this paper has been conducted as a part of the piping research program in which Pipe Fatigue, Jet, and Pipe Whip test programs have been also involved

In the pipe fatigue test program, fatigue growth of multiple surface cracks in the inner surface of pipings were investigated.

In the Jet and Pipe Whip test program, Jet force, jet impingement and pipe whip behavior were investiguted under simulated pipe break accident. The above two tasks have almost been completed.

In the ductile pipe fracture test program, stable and unstable pipe fracture under bending load, and pipe fracture behavior under dynamic load are being investigated.1)2)3)4) A leak test through a through-wall cracked piping under bending load is also included in this program.

In this paper are summarized the progress and future plan of the ductile pipe fracture test program conducted at JAERI as well as the recent pipe bending test results performed at room temperature and 300 °C using 3-, 6-, and 12-inch diameter type 304 stainless steel and carbon steel pipes with a through-wall or a part-through cracked pipe in the circumferential direction. By the use of pipe test results, the validity of the net-section collapse criterion and flaw acceptance criteria for austenitic piping of ASME Code Sec.XI and, engineering fracture parameter estimation methods developed by EPRI-GE5) and NRC6) are discussed. Besides, a stability evaluation procedure of piping systems is presentd under displacement controlled bending load using empirical expressions obtained from the pipe test results.

2. Progress and outline of the ductile pipe fracture test program

In Table 1, time schedule of the ductile pipe fracture test program is shown. The program was started in 1982 by constructing a pipe bending test facility, and subsequently pipe tests have been carried out since 1983. In this program, following tasks have been carried out. First task is the bending test of [illegible], 6- and 12-inch diameter pipes under rigid or compliant displacement control loading at room temperature, 300 °C, and BWR environment. Tests have been performed using austenitic stainless steel and carbon steel pipes including welded joint with a circumferential through-wall or a part-through crack.

Second task is a dynamic test in which the pipe fracture behavior under a dynamic bending load has been investigated since FY 1986. As a part of the program, leak test was started from FY 198[illegible], and the future test plan will be described in the paper. Computer code development has been also performed in the program to calculate the fracture mechanics parameters of 3-dimensional geometry and to evaluate pipe fracture behaviors.

In order to perform the pipe bending test under compliant and rigid bending load, two test facilities which installed a compliant disk springs were constructed fcr 6-inch and 12-inch diameter pipes respectively. Fig. 1 shows the pipe bending facility for 12-inch diameter pipes. This facility has the maximum capacities of 1500 kN load, 100 mm stroke and 326 mm/MN compliance respectively. The pipe bending test facility can be used under either high compliance or rigid condition.

3. Results and discussions on pipe fracture tests of stainless steel and carbon steel pipes at room temperature and 300 °C

3.1 Validity of Net-section collapse approach

The net-section collapse criterion[7]) can be applicable when the fully ductile condition is reached in piping. A stress distribution model of the net-section collapse criterion in the cracked section of piping is shown in Fig. 2. Net-section collapse load can be evaluated using the stress distribution shown in Fig. 2. The flow stress was assumed to be the average of yield strength and ultimate tensile strength.

In Fig. 3, normalized collapse loads of through-wall cracked pipes obtained from 3-, 6-, and 12-inch diameter stainless steel pipe tests and 6-inch diameter carbon steel pipe tests are plotted and compared with the net-section collapse criterion. Generally a reasomable agreement can be seen between test results and the criterion though welded stainless steel pipes tend to show slightly lower load and carbon steel pipes show about 20% higher load than the load by the net-section collapse criterion.

On the other hand, some different trend from the through-wall cracked pipes were observed in case of part-through cracked pipes. Fig. 4 to Fig. 6 show the results of 6-inch diameter part-through cracked type 304 stainless steel pipes with and without welded joint and carbon steel pipes respectively.

As shown in Fig. 4, part-through cracked stainless steel pipes of base metal showed a conservative load compared to the criterion except for deep and short crack range. However, deeply cracked pipes of d/t=0.91 showed a considerably lower load than that of the criterion. On the other hand, welded stainless steel pipes showed slightly lower load than that of base metal. In this case, deeply cracked pipe also showed a considerably lower load than that of the net-section collapse criterion.

In the results of 6-inch diameter carbon steel pipes as shown in Fig. 6, a different trend that through-wall cracked pipes also show higher load than the criterion as well as part-through cracked pipes except for a deeply cracked pipe, if the flow stress is assumed to be the average of yield strength and ultimate tensile strength.

As mentioned above, the net-section collapse criterion gave a reasonable or a conservative load for stainless steel and carbon steel pipes tested except for deeply cracked pipes. Therefore the net-section collapse criterion can not be applied for predicting the fracture load of a deeply cracked pipe.

Using the test results of 6-inch diameter stainless steel pipes with and without welded joint, the validity of the flaw acceptance criteria of ASME Code Sec.XI, IWB 3640 was examined as shown in Fig. 7 and Fig. 8. In these figures, the maxmum allowable flaw size of the criteria for Sm load is indicated by the broken line and slolid lines were obtained from the original equation developed by EPRI-GE[8)9)10)], which indicate the onset of fracture by P_b=Sm, 2Sm, and 3Sm loading respectively, while pipe test results were plotted with the safety factor for Sm load, i.e., P_b/Sm value. It can be found in both figures that most data point satisfy the expected safety factor by comparingto solid curves.However the deeply cracked pipes of d/t=0.91 or 0.90 do not retain enough safety factor. Whereas the flaw acceptance criteria of IWB 3640 seems reasonable because the maximum allowable depth is limitted up to 75 % of wall thickness.

In Fig. 9, flow stresses of tested stainless steel pipes determined experimentally from the net-section collapse formula are plotted as the function of crack depth, and compared with 3Sm value. In ASME Code SecXI, it is assumend that flow stress of the austenitic stainless steel pipe satisfy 3Sm value, and it is seen in the figure that the most flow stresses obtained from pipe test results are larger than or nearly 3Sm. However, it is clearly observed that the flow stresses are considerably low for deeply cracked pipes. In Fig. 9, it is also observed that flow stress is dependent on crack angle as well as crack depth.

3.2 Comparison of J-integral and COA between 6-inch diameter pipe test results and current engineering estimation schemes

Recently some engneering estimation schemes for fracture mechanics parameters, such as J-integral, COD, and COA (crack opening area), have been developed.[5)6)11)12)]

The typical pipe test results of 6-inch diameter stainless steel and carbon steel pipes with a 90 degree through-wall crack are compared below with J-integral and COA obtained from EPRI-GE and NRC estination schemes.

In Fig. 10 and Fig. 11, J-integral values obtained by these estimation schemes are compared with the test results of stainless steel and carbon steel pipes. Experimental J values were determined by the equations developed by J. Pan et.al.[14)] using load vs. load-line displacement curve obtained from test results. In both figures, it is seen that EPRI-GE estimation scheme gives a higher J value than that of test results, while NRC scheme seems to give more improved J values. In Fig. 12 and Fig. 13, estimated COA values are also compared with the test results. As shown in Fig. 12 and Fig. 13, EPRI-GE estimation scheme gave larger COA value as well as J value. On the other hand the NRC scheme gives a lower COA than that of test results, which means that a conservative leak flow rate can be obtained by the use of NRC scheme in COA estimation.

In each case of the above estimations, shown in Fig. 10 ~ Fig. 13, EPRI-GE scheme gave higher values than those of test results. However it must be noted that the estimated values of these parameters by EPRI-GE scheme are also dependent on the choice of Remberg-Osgood prameters, n and α. In the present study, n and α were determined by the fitting of engineering stress-strain curve in the strain range of 2 % to 10 %.

3.3 Stability of piping system against unstable ductile fracture under bending load

The initiation of an unstable ductile fracture under the prescribed displacement is dependent on the moment-rotation relationship of the cracked section and loading system compliance including compliance of piping. In Fig. 14, the moment-rotation relationship of cracked section of a through-wall cracked pipe under bending is schematically illustrated. The minimum value of $(-d\alpha_C/dM)$ can be usualy found in the load-decreasing region after the maximum load.

On the other hand, the stability of a piping system under prescribed displacement bending load can be expressed as follows.

$$\begin{aligned} \frac{d\delta_T}{dP} &= \frac{d\delta_0}{dP} + \frac{d\delta_C}{dP} \\ &= C_T + \lambda^2 \cdot \frac{d\alpha_C}{dM} < 0 \end{aligned} \tag{1}$$

where C_T is total compliance of the piping system at loading point and λ^2 is the proportional coefficient between δ_C/P and α_C/M, i.e., $\delta_C/P = \lambda^2 \cdot \alpha_C/M$. δ_0 is the displacement without crack and is expressed by $\delta_0 = C_T P$.

Using the minimum value of $(-d\alpha_C/dM)$ mentioned above, the stability of the piping system for any amount of displacement can be expressed as follows.

$$\frac{C_T}{\lambda^2} < \left(- \frac{d\alpha_C}{dM} \right)_{min} \tag{2}$$

In Fig. 15 are shown stability conditions for specific cases, i.e., simply supported 3-point bending beam, uniform bending beam with fixed grips, and 4-point bending beam with a compliant loading system, which were obtained from eq.2 or using the same analogy for the case of prescribed rotation bending. It should be noted that the critical length of 3-point bending beam is three times as much as that of the uniform bending beam.

In the meanwhile, the minimum value of $(-d\alpha_C/dM)$ is necessary to evaluate the above critical conditions. A FEM analysis or simple estimation schemes might be applicable for some cases. However the validity of such methods is suspectable for the case of a fully ductile condition which seems to be the most cases at the onset of an unstable ductile fracture.

By the use of 6-inch diameter and 12-inch diameter stainless steel and carbon steel pipe test results, empirical expressions for $(-d\alpha_C/dM)_{min}$ were obtained as described below.

In Fig. 16, the normalized minimum $(-d\alpha_C/dM)$ values obtained from pipe test results are plotted as a function of crack angle. It is observed in the figure that $(-d\alpha_C/dM)_{min} \cdot M_C$ values plotted are approximately constant, 0.2 for stainless steel pipes and 0.09 for carbon steel pipes. The stability of any piping system under displacement control type bending can be evaluated using above empirical expressions of $(-d\alpha_C/dM)_{min}$, and two case studies were carried out with regard to critical pipe length of 3-, 6-, 12-, 24-inch diameter Sch. 80 pipes of stainless steel and carbon steel. Fig. 17 and Fig. 18 show the critical length obtained from the above procedure. Data point plotted were directly obtained from the minimam $(-d\alpha_C/dM)$ of test results using equation (2).

As shown in Fig. 17 and Fig. 18, the critical pipe length increases with pipe diameter and crack angle. The critical pipe length of carbon steel pipe is approximately half of that of stainless steel pipe. It was found that the critical length evaluated by the equation of Tada and Paris[13)] gave reasonable length comparing with test results.

4. Conclusions and future plan

In this paper, the progress of ductile pipe fracture test program conducted at JAERI was summarized and some recent pipe test results were presented.

Discussions were carried out with regared to the validity of the net-section collapse criterion and the flaw acceptance criteria of ASME Code Sec.XI, IWB 3640, and engineering fracture parameter estimation schemes developed by EPRI-GE and NRC. Based on an empirical formula, a procedure was also presented to evaluate the stability of piping system under displacement control type bending load with respect to pipe surpport, pipe length, and compliance conditions.

The JAERI's pipe fracture test program including dynamic test and leak test is being scheduled to continue by the end of FY 1989. Leak test will be performed from FY 1988 in order to investigate the flow induced phemomena, such as the effect of vibration or possibility of crack growth acceleration including leak rate study, caused by a fluid leakage through a through-wall cracked pipe under bending load. In Table 2 are shown the time schedule and task items of leak test.

Acknowledgement

This work was performed under a contract between the Science and Technology Agency of Japan and the JAERI to demonstrate the safety for pipe rupture of the primary coolant circuits in nuclear power plants. The authors would like to acknowledge Mr. K. Sato, Director of Department of Reactor Safety Research, Dr. T. Shimooke, Deputy Director and our collaborators Messrs. T. Ohba, T. Kawamura and R. Yagioka for their great support.

Nomenclature

COA: Crack opening area
C_T : Total compliance
d : Crack depth
M : Bending moment
M_C : Collapse moment
P : Axial force
R : Mean Radius
S : Span length
S_f : Flow stress obtained from pipe test
α : Load-point rotation caused by crack existence
α_T : Total load-point rotation
δ_C : Load-point displacement caused by crack existence
δ_T : Total load-point displacement
θ : Half crack angle
σ_f : Flow stress determined by mechanical properties

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(1) K. Shibata, et al.: Ductile Fracture Behavior of 6-inch Diameter Type 304 Stainless Steel and STS 42 Carbon Steel Piping Containing a Through-wall or Part-through Crack, JAERI-M 86-078, 1986
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(11) P.C. Paris and H. Tada: The Application of Fracture Proof Design Methods Using Tearing Instability Theory to Nuclear Piping Postulating Circumferential Through Wall Cracks, NUREG/CR-3464, 1983

(12) F.W.Brust: Approximate Method for Fracture Analysis of Through-Wall Cracked Pipes, NUREG/CR-4853, 1987
(13) H. Tada,et al. : A Stability Analysis of Circumferential Cracks for Reactor Piping Systems, ASTM STP 700, 1980
(14) J. Pan, et al.: Application of a Tearing Instability Analysis for Strain Hardening Materials to a Circumferentially Cracked Pipe in Bending, ASTM STP 833, 1984

Table 1 Time schedule of ductile pipe fracture test program

Items \ Fiscal year	1982	1983	1984	1985	1986	1987	1988	1989
6-inch diameter pipe test	Test machine → HC,400KN 750mm	Test (RT,300 C,BWR,SS,CS,WM) →	→	→	→	→		
12-inch diameter pipe test			Test machine → HC,1500KN 1000mm	Test(") →	→	→		
3-inch diameter pipe test					Test (RT,SS,CS) →	→		
Dynamic test				Test machine →	Test (RT,BWR,SS,CS) →	→	→	→
Leak test						Test apparatus →	Test →	→
Computer code	EPAS , ADINA (3-D Fracture mechanics parameters) →	→	→	→	→	→	→	→

Table 2 Leak test program

Items \ FY	1987	1988	1989
Leak test apparatus	→		
Fabrication of test pipes	4-D,6-D →	12-D →	
Leak test*		4-D,6-D →	12-D →
Slit specimen test*		→	→

* Test parameters : Material,Roughness,COD,temperature, pressure,applied stress

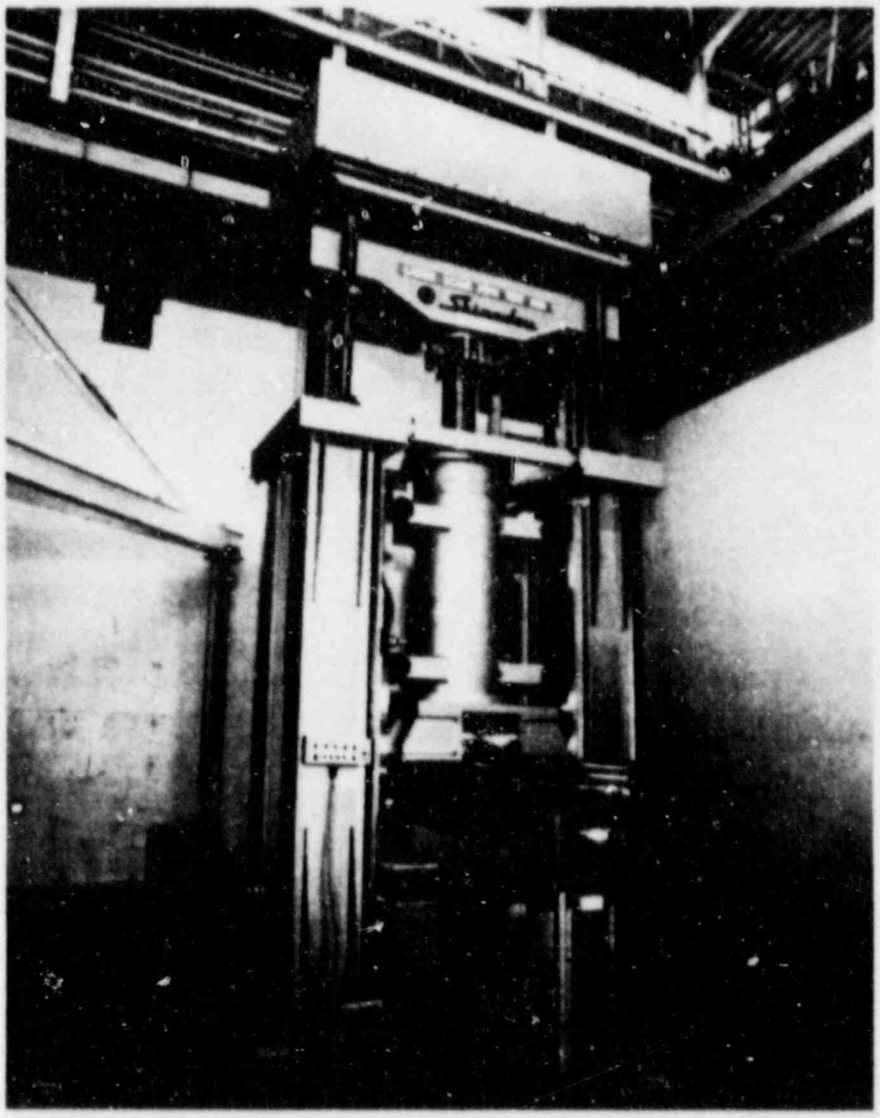
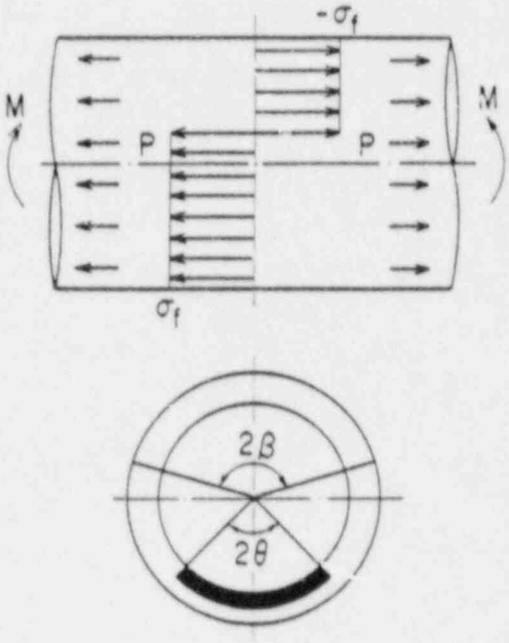


Fig. 1 1500kN high compliance pipe bending facility for 12-inch diameter pipe test



for $\beta \leqq \pi - \theta$;

$$Mc = 2\,\sigma_f R^2 t\left(2\sin\beta - \frac{d}{t}\sin\theta\right)$$

$$\beta = \frac{\pi - \frac{d}{t}\theta}{2} - \frac{\pi}{4}\,\frac{Rp}{t\sigma_f}$$

for $\beta > \pi - \theta$;

$$Mc = 2\,\sigma_f R^2 t\left\{2\left(1 - \frac{d}{t}\right)\sin\beta' + \frac{d}{t}\sin\theta\right\}$$

$$\beta' = \pi + \frac{1}{1-d/t}\left\{\frac{\frac{d}{t}\theta - \pi}{2} - \frac{\pi}{4}\,\frac{Rp}{t\sigma_f}\right\}$$

Fig. 2 Fracture evaluation by net-section collapse criterion

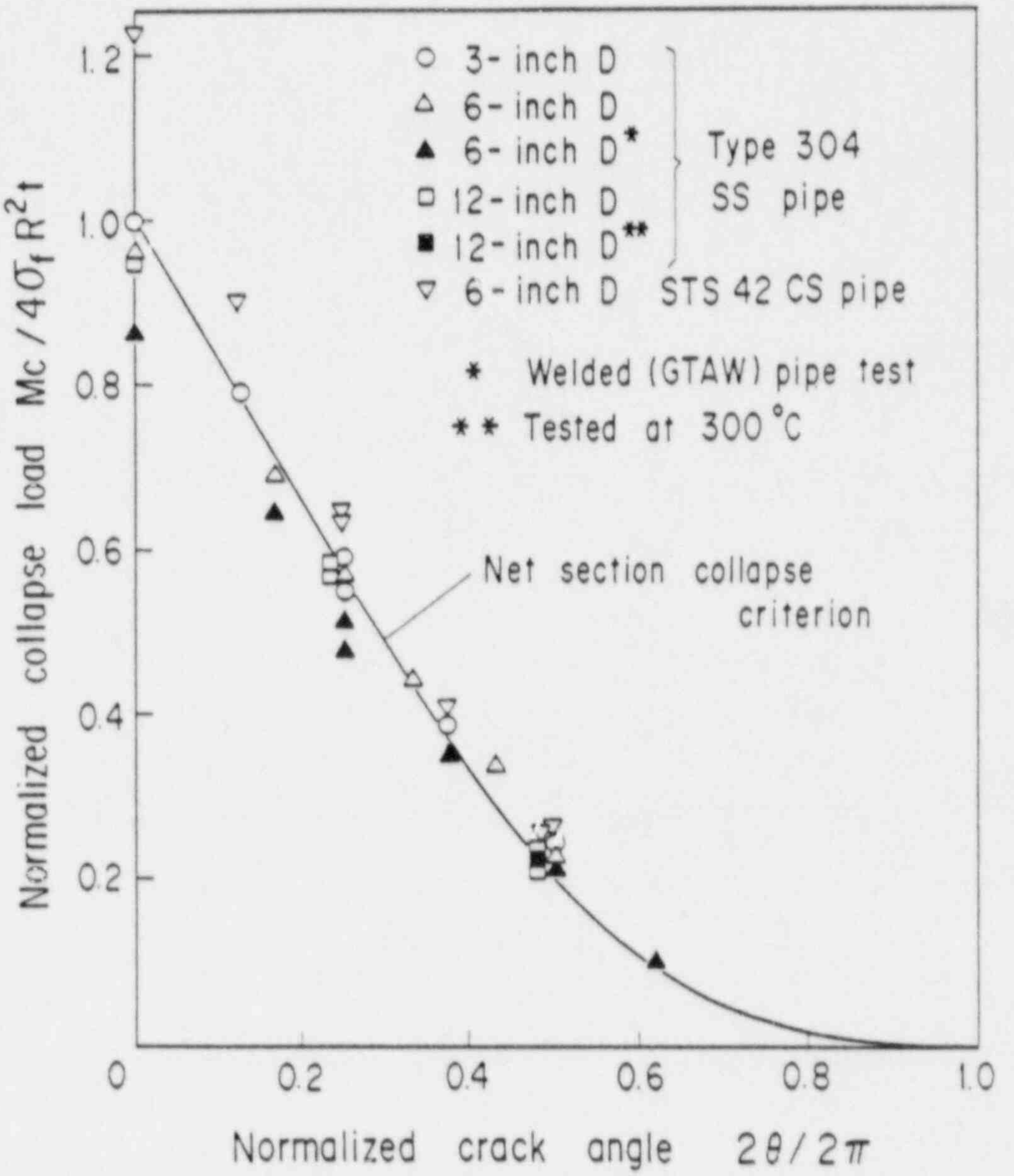


Fig. 3 Comparison between net-section collapse criterion and pipe test results (Through-wall cracked pipes)

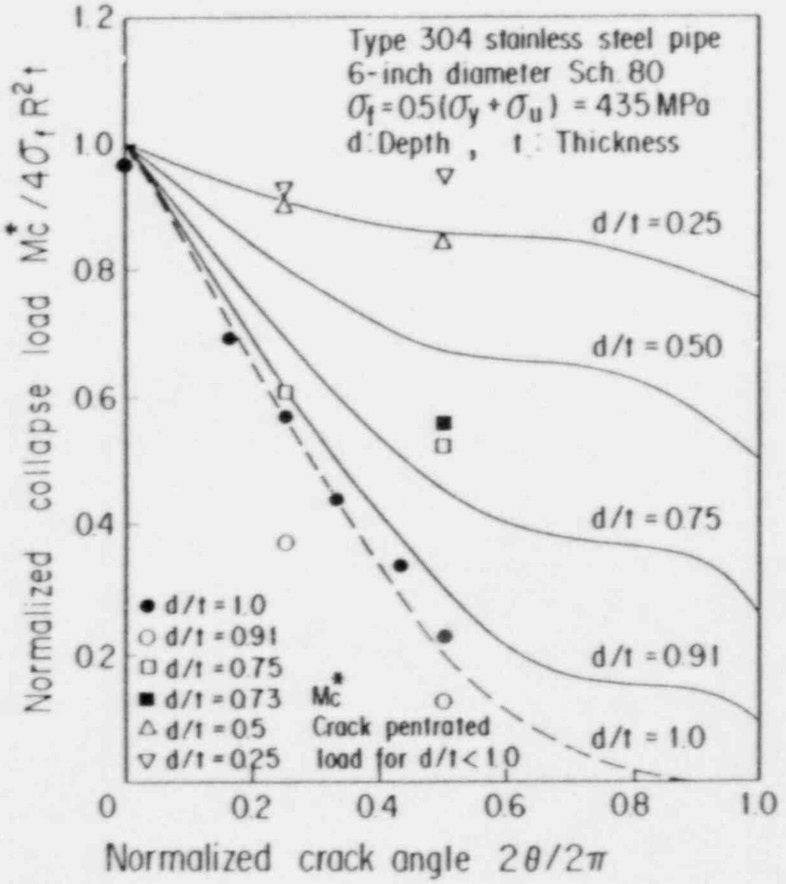


Fig. 4 Comparison between net-section collapse criterion and pipe test results

(6-inch diameter stainless steel pipe, Base metal)

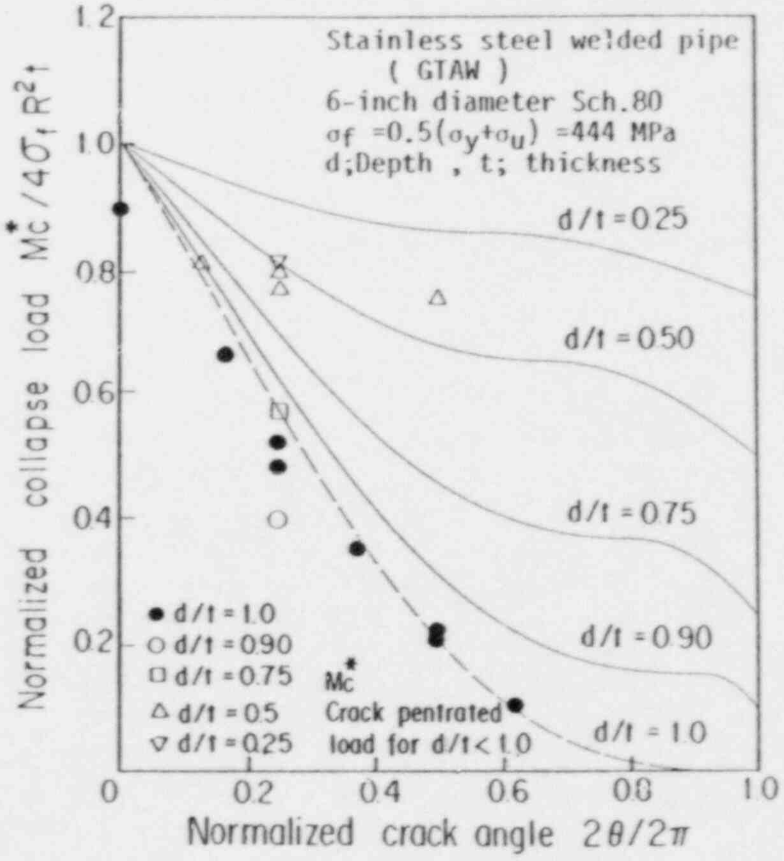


Fig. 5 Comparison between net-section collapse criterion and pipe test results

(6-inch diameter stainless steel pipe, GTAW joint)

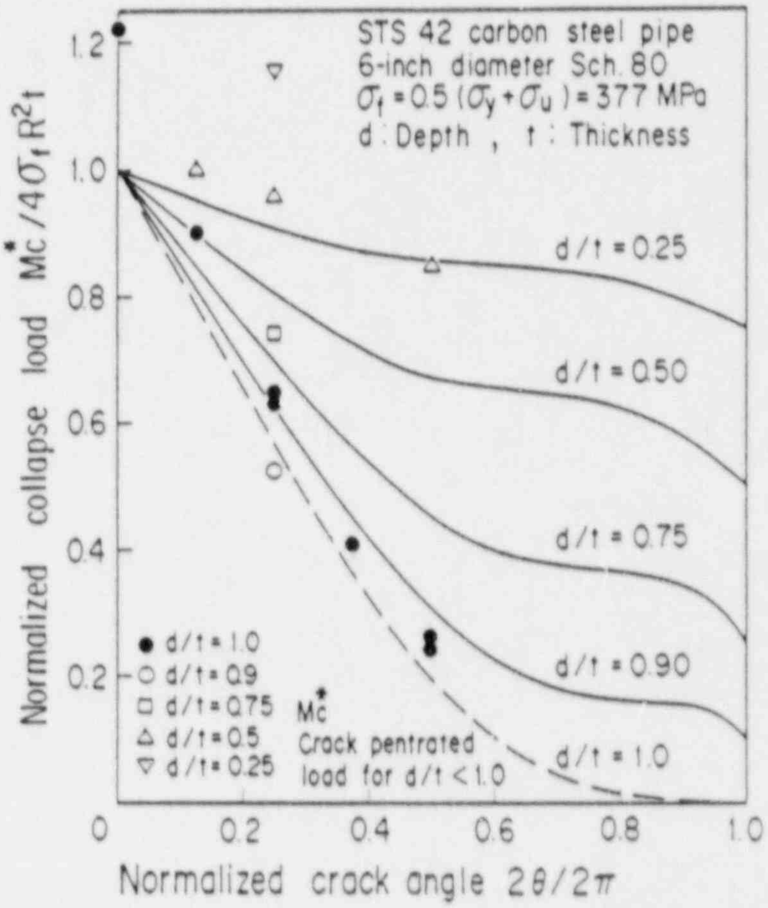
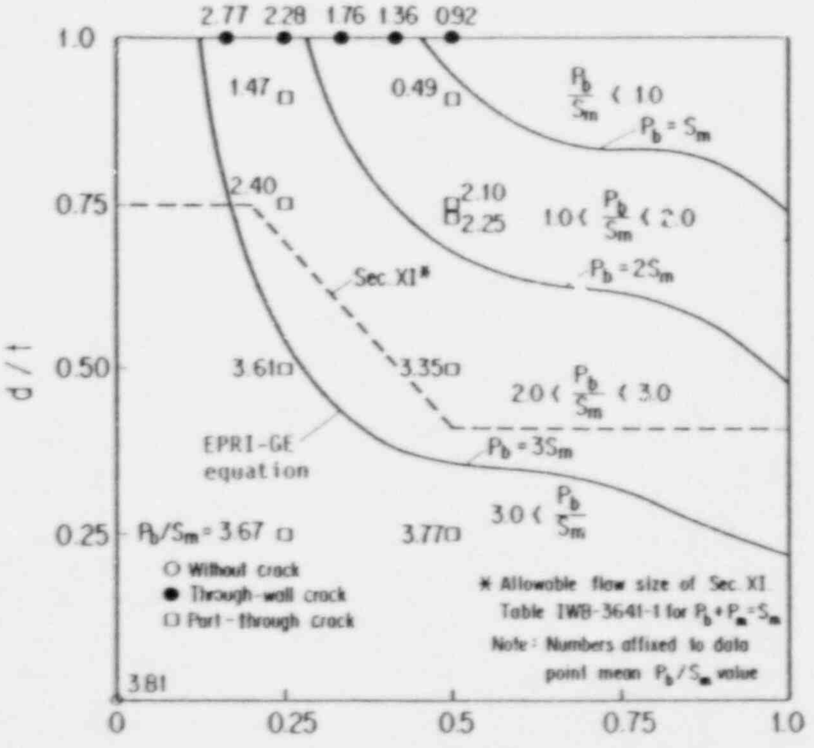
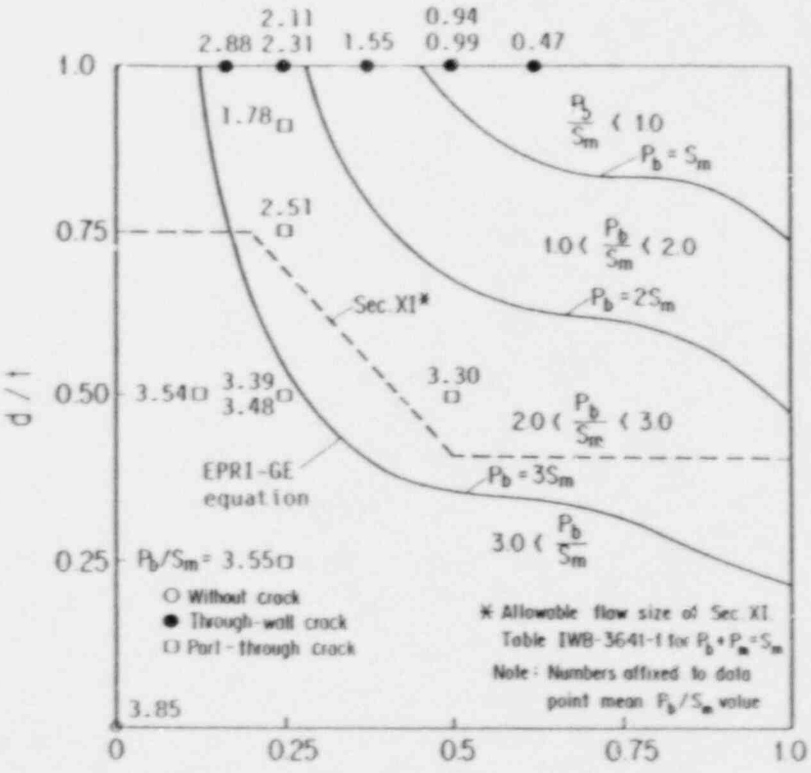


Fig. 6 Comparison between net-section collapse criterion and pipe test results (6-inch diameter carbon steel pipe)



(Base metal)

Fig. 7 Comparison between test results of 6 inch diameter stainless steel pipes and flaw acceptance criteria of ASME Code Sec.XI (Base metal)



(Welded joint:GTAW)

Fig. 8 Comparison between test results of 6 inch diameter stainless steel pipes and flaw acceptance criteria of ASME Code Sec.XI (Welded joint, GTAW)

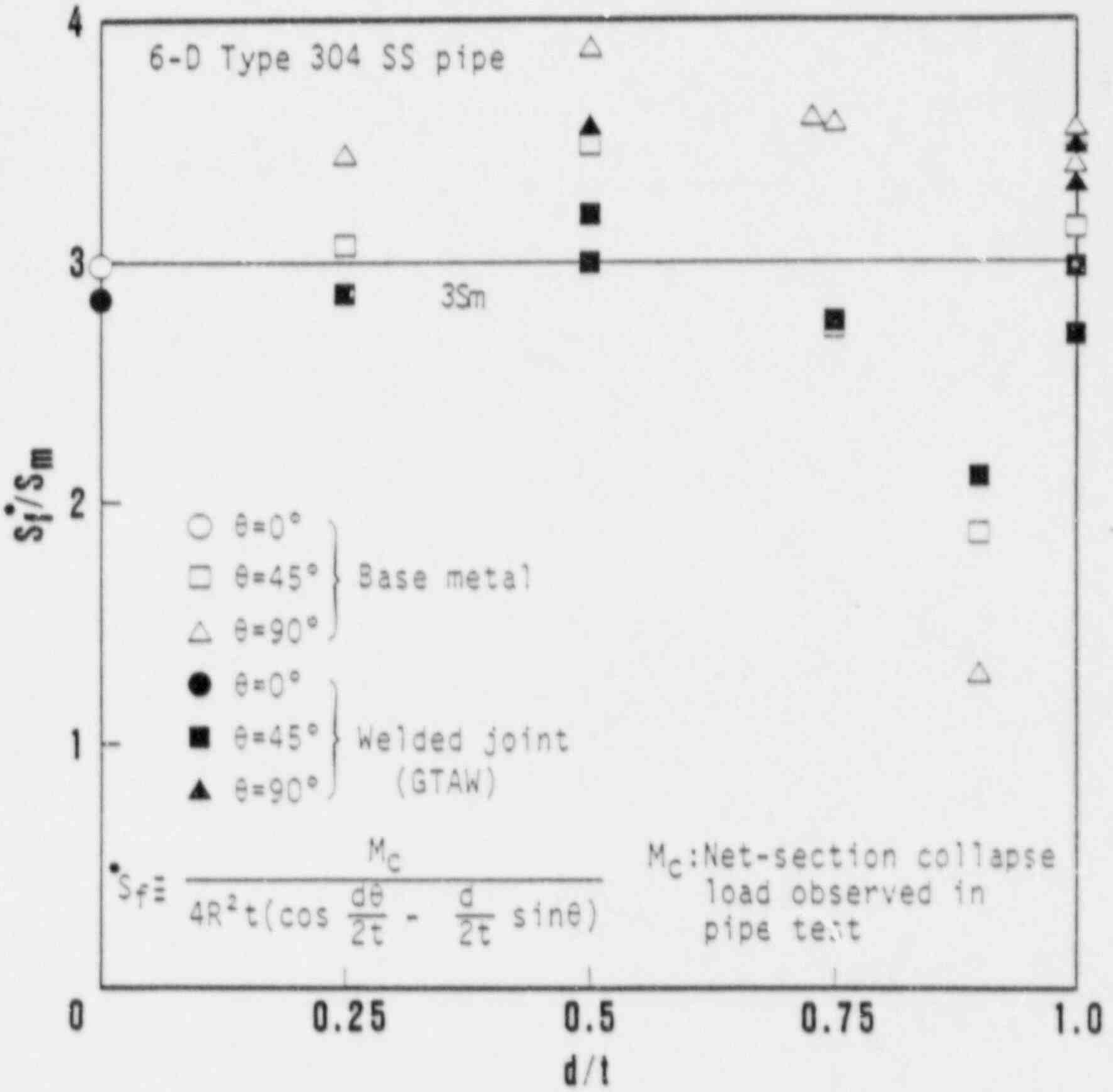


Fig. 9 Measured flow stresses in 6-inch diameter stainless steel pipe test

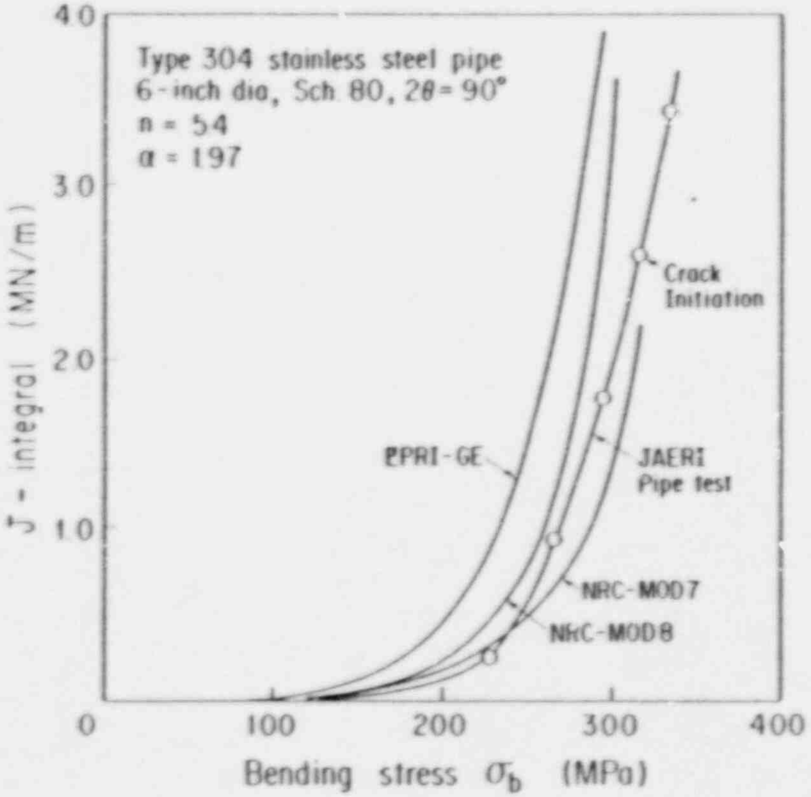


Fig. 10 Comparison of J-value between pipe test result and current engineering estimation methods

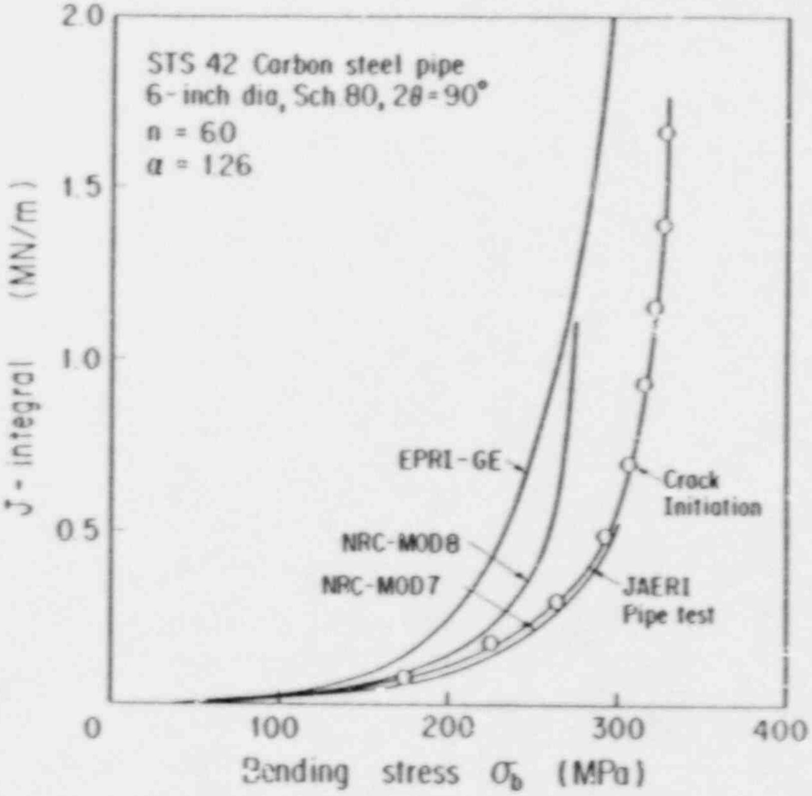


Fig. 11 Comparison of J-value between pipe test result and current engineering estimation methods

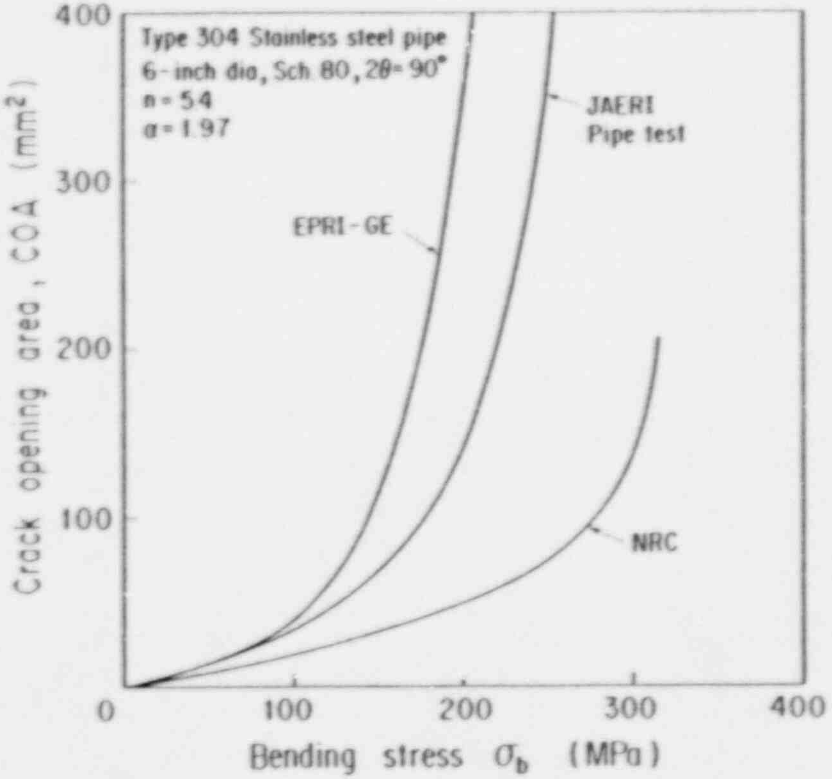


Fig. 12 Comparison of Crack Opening Area between pipe test result and current engineering estimation methods

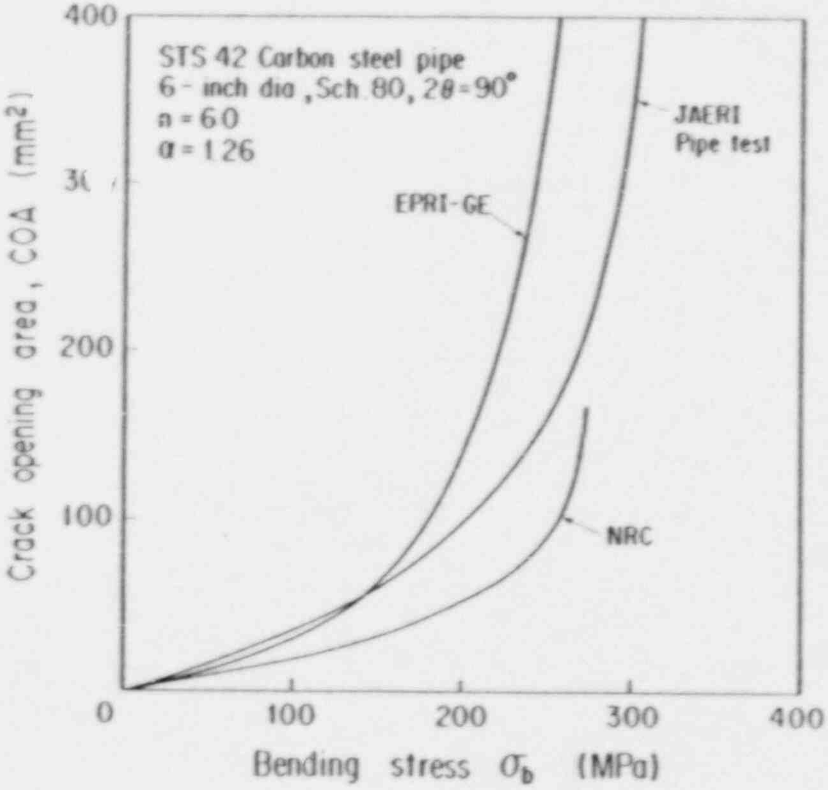


Fig. 13 Comparison of Crack Opening Area between pipe test result and current engineering estimation methods

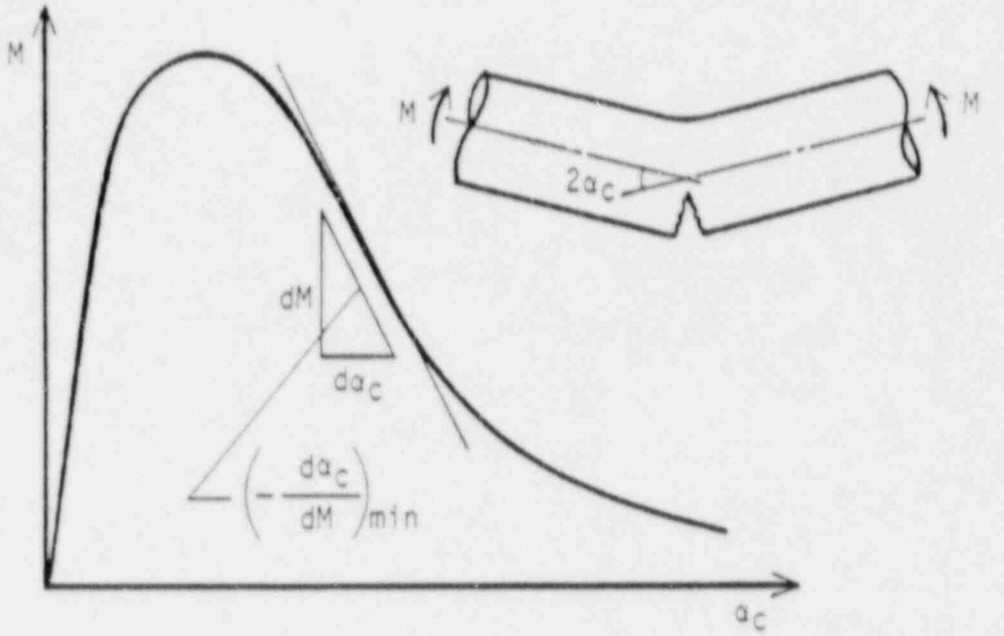


Fig. 14 Load-Rotation relationship of cracked pipe under bending

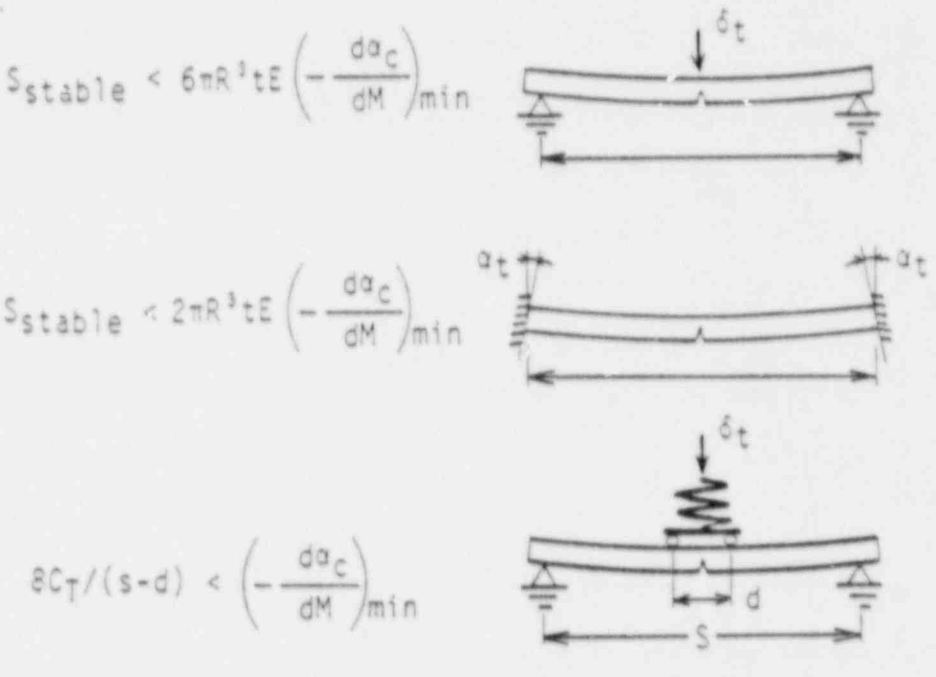


Fig. 15 Stability condition of piping systems under displacement control bending

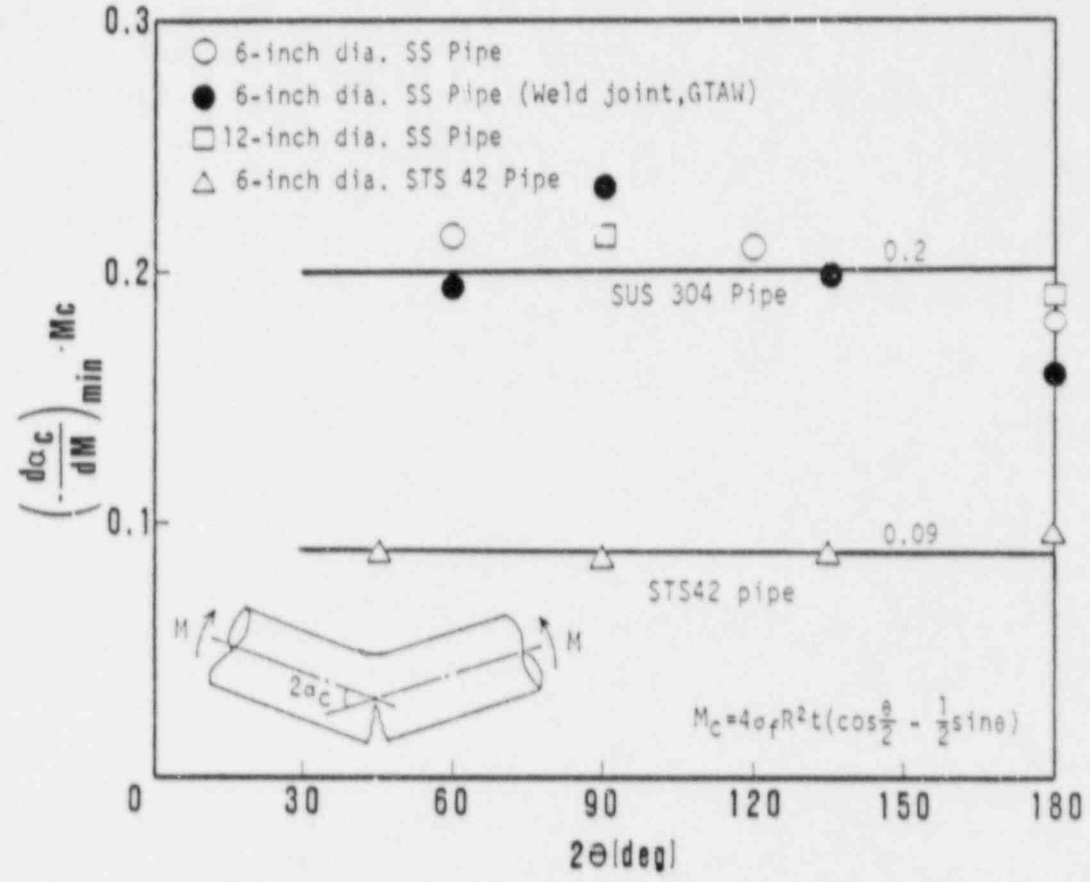


Fig. 16 Empirical expressions for minimum ($-\alpha_c/dM$) value obtained from pipe tests

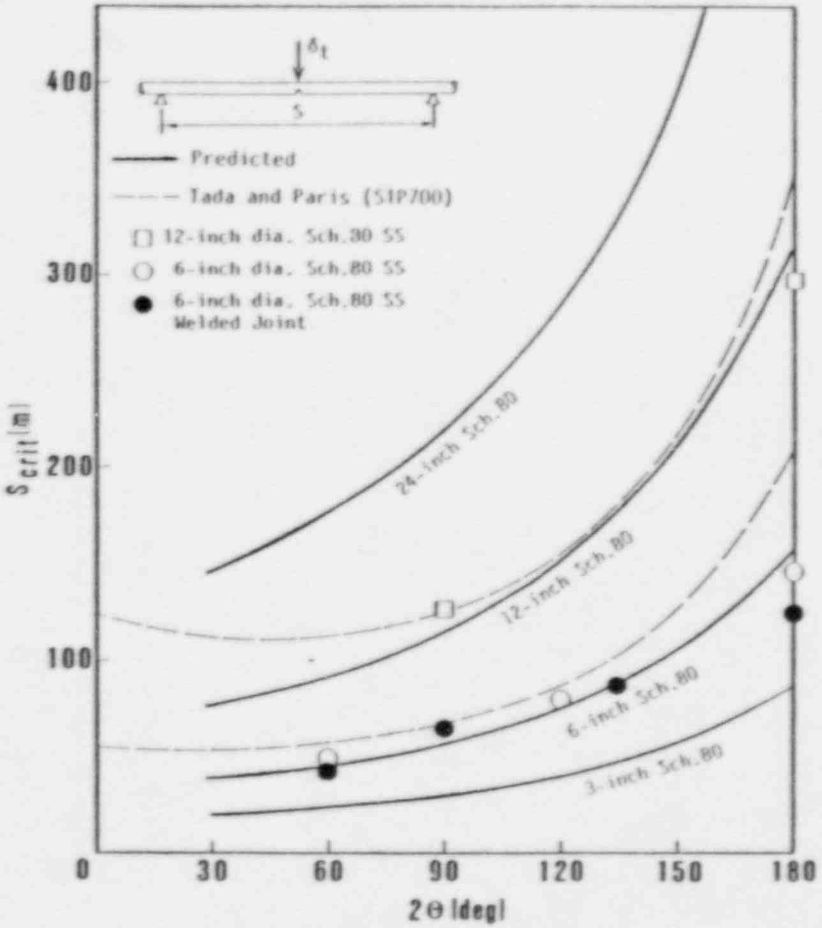


Fig. 17 Critical span length of stainless steel piping under 3-point bending against unstable ductile fracture

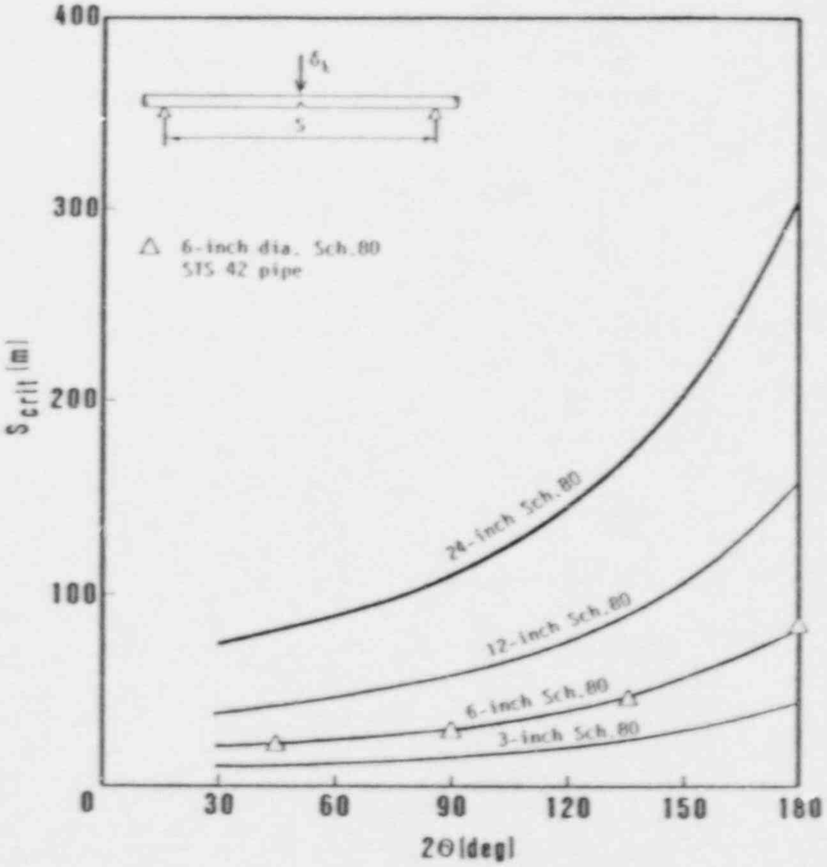


Fig. 18 Critical span length of carbon steel piping under 3-point bending against unstable ductile fracture

SESSION 6: FRACTURE MECHANICS (PART 1)

Chairman: H. Schulz, GRS, Federal Republic of Germany

APPLICATION OF LBB IN THE USA

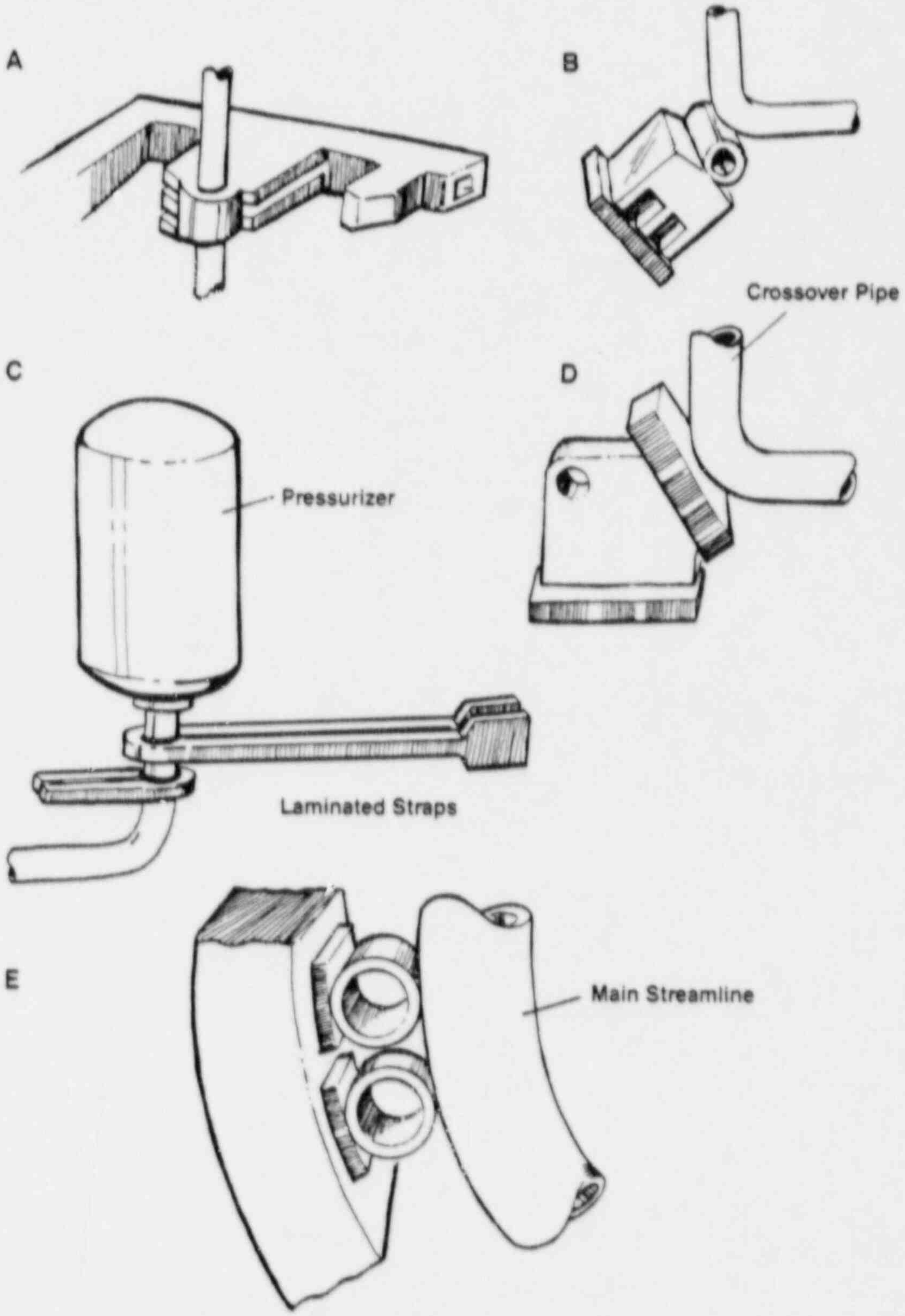
A FIRST APPLICATION TO
DUQUESNE LIGHT COMPANY
BEAVER VALLEY UNIT 2

A PRESENTATION TO THE
LBB TOKYO SEMINAR

DOUGLAS M. NORRIS
KAZUO KISHIDA
BINDI CHEXAL

MAY 15, 1987

TOKYO, JAPAN



Pipe Whip Devices

HISTORY OF PIPE WHIP HARDWARE REQUIREMENTS

- DESIGN BASIS ACCIDENT TO SIZE CONTAINMENT
- DESIGN BASIS ACCIDENT BECOMES REAL ACCIDENT... MAJOR BREAK AS DESIGN BASIS
- REQUIREMENTS FOR PIPE WHIP HARDWARE 10CFR PART 50/GDC4; RG 1.46 (1974)
- GENERIC ISSUE A-2 ASYMMETRIC BLOWDOWN LOADS - NUREG-0609
- WOG; LLNL: FAILURE PROBABILITY = 10^{-12} (1982)
- GENERIC LTR 8404, GDC4 EXEMPTIONS FOR PWR RCS (NARROW SCOPE RULE)
- NUREG 1061-VOL 3 PROPOSED EXTENSION TO BOP (1984)
- NRC ELIMINATES ARBITRARY INTERMEDIATE BREAKS
- BROAD SCOPE RULE FOR BOP PIPING, REQUEST FOR COMMENT (1986)

EPRI/DLC HISTORY

SOFTWARE DISCUSSIONS WITH RLCA	EARLY 85
PHASE I (1) RLCA PROPOSAL TO DLC	MAR 85
DLC OKS PHASE I	MAY 85
PHASE II (2) PROPOSAL TO DLC	JAN 85
EPRI PRESENTATION TO DLC VP	FEB 86
DLC OKS PHASE II	MAR 86
DLC/EPRI COOP PROGRAM	MAR 86
FINAL REPORT TO NRC	JAN 87
NRC OKS SER	MAR 87
NRC OKS ENVIR IMPACT	MAR 87

---

(1) PREPARE NRC SCOPE DOCUMENT/CRITERIA

(2) DO THE WORK

LBB CRITERIA

- SCREEN OUT PROBLEM LINES - WATER HAMMER, IGSCC, EROSION
- SELECT LINE LOCATION WITH HIGHEST STRESS/LOWEST TOUGHNESS
- DETERMINE MIN DETECTABLE LEAK RATE Q (SAY 0.5 GPM)
- CALCULATE FLAW LENGTH L FOR 10Q AT LEVEL A LOAD
- SHOW $L_{CRIT} > 2L$ AT LOAD LEVEL A + SSE
- SHOW L IS STABLE AT $\sqrt{2}$ (LEVEL A + SSE)
- SHOW EOL FATIGUE CRACK GROWTH SATISFIES IWB 3640
- ARCHIVAL MATERIAL PROPERTIES VS. LOWER BOUND

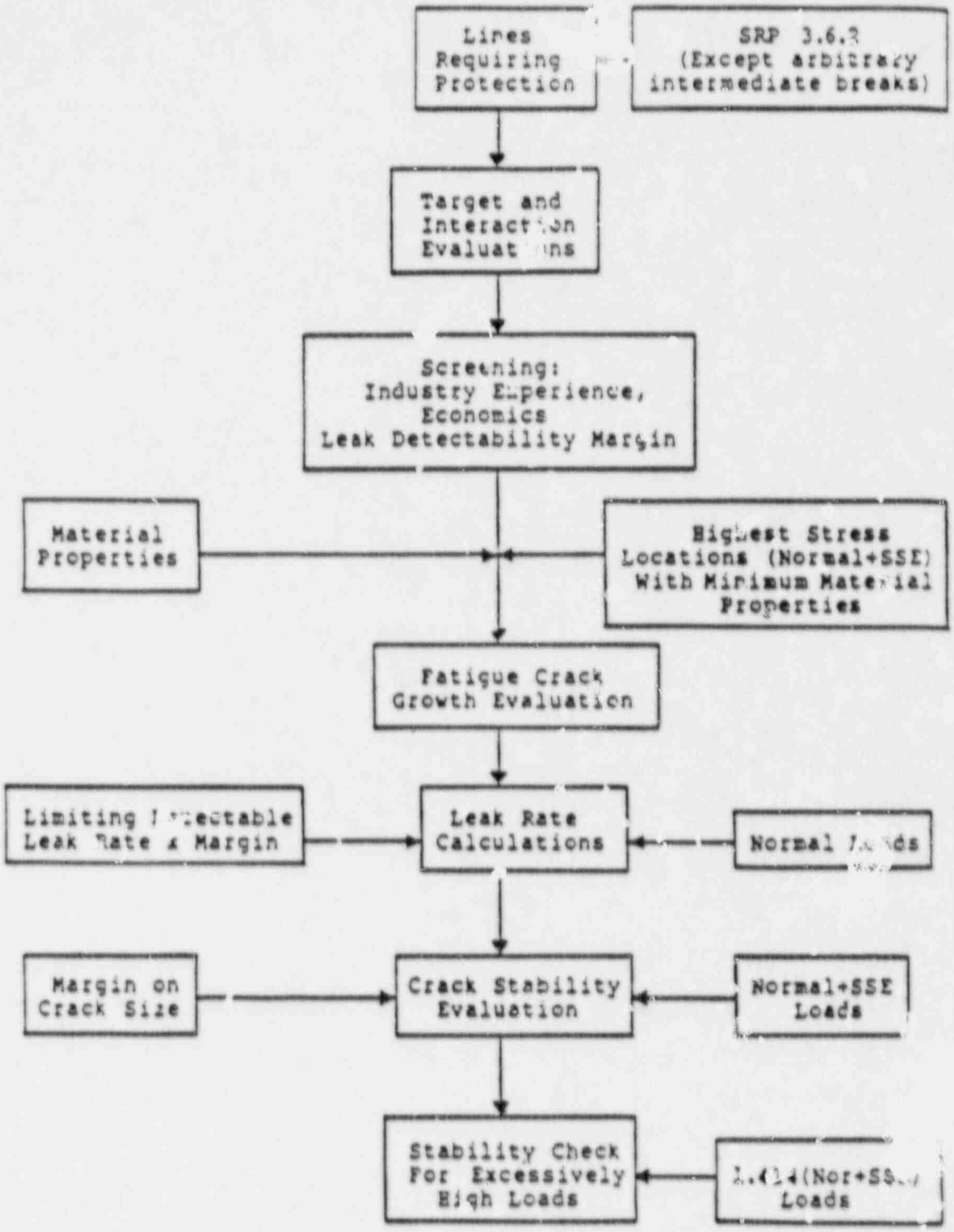


FIGURE 1.1 WHIPJET PROGRAM

EPRI LBB RELEVANT EXPERIENCE

TECHNOLOGY IN PLACE FROM EARLIER PROGRAMS

- DUCTILE STEEL FRACTURE MECHANICS (1977)
- STAINLESS/CS PIPING DATA BASE (1982)
- CRACK LEAKAGE TESTS/PREDICTIVE SOFTWARE (1983)
- FRACTURE TESTS/PREDICTIVE SOFTWARE (1984)
- ASME SECTION XI IWB-3640/3650 (1985)

EPRI WHIPJET CONTRIBUTIONS

- SOFTWARE

PICEP	CRACK OPENING AREA FLOW THROUGH PIPE CRACK BOUNDING CRITICAL CRACK SIZE
FLET	CRACK INSTABILITY CRITICAL LOAD/CRITICAL CRACK SIZE

- FLOW RATE TESTING

  WYLE TESTS: MINIMUM DETECTABLE FLOW RATE

- PIPE FRACTURE TOUGHNESS/STRENGTH

  DATA BASE ON SS & CS

- REPORTING AND ANALYSIS REVIEW
- CONSULTING WITH DLC/SWEC/DLC
- PRESENTATIONS TO NRC & ACRS

HARDWARE ELIMINATION

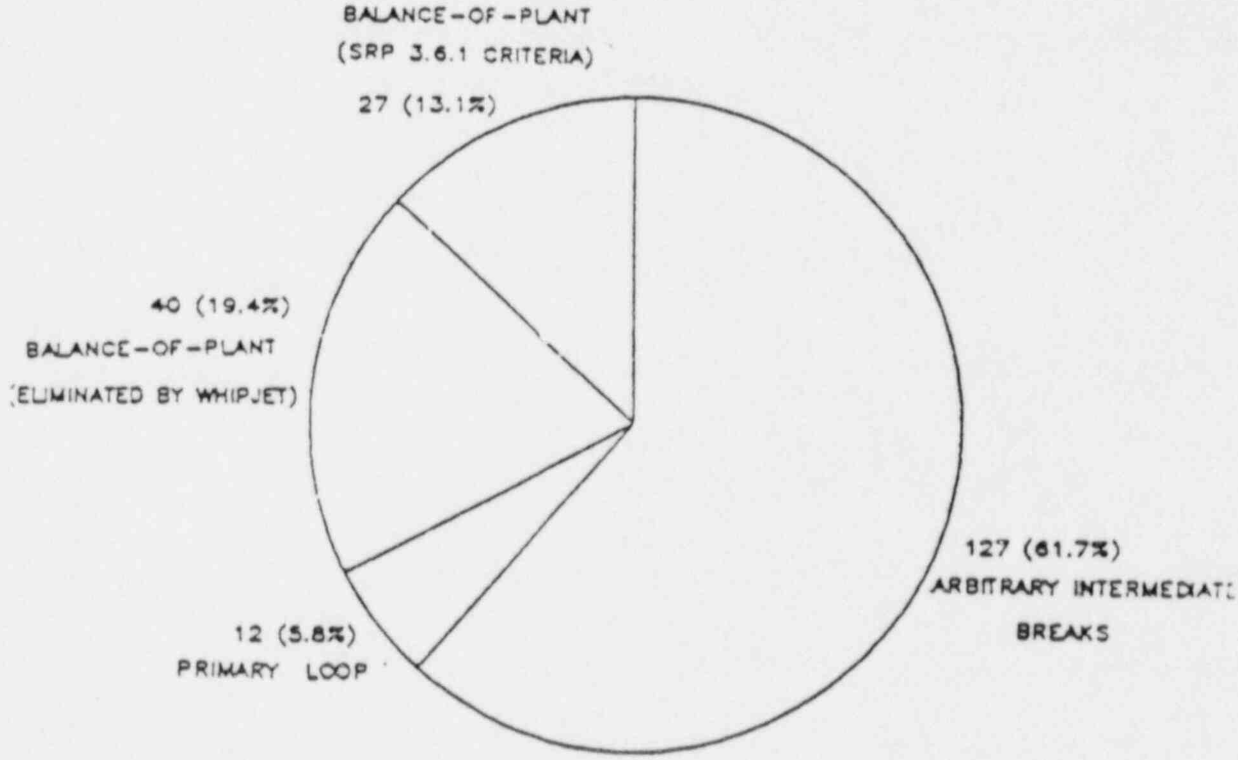
PIPING SYSTEM	PIPE SIZE (IN)	MATERIAL	BREAKS	HARDWARE PRR (1)	JIS (2)
SIS	6	TYPE 316	20	7	5
RCS	8	TYPE 304	6	6	0
RHS	10	TYPE 316	1	0 (3)	0
RHS	12	TYPE 316	1	1	0
SIS	12	TYPE 316	28	10	3
RCS	14	TYPE 304	13	8	0
		TOTAL	69	32	8
			TOTAL	40	

NOTES:

(1) PRR = PIPE RUPTURE RESTRAINT
(2) JIS = JET IMPINGEMENT SHIELD
(3) BREAK REQUIRES SIS RESTRAINT

BVPS-2 HARDWARE REQUIREMENTS

Pipe Rupture Restraints and Jet Shields



NRC QUOTE FROM SER (NUREG 1057, (4), MARCH 1987) AND ENVIRONMENTAL IMPACT STATEMENT (P. S. TAM TO J. J. CAREY, 13 MARCH 1987)

"...THE STAFF CONCLUDES THAT DUQUESNE LIGHT COMPANY HAS PROVIDED TECHNICAL JUSTIFICATION FOR NOT INSTALLING PROTECTIVE DEVICES..."

"THE APPLICANT'S APPLICATION IS IN LINE WITH THE RULEMAKING BUT IS AHEAD OF IT."

BENEFITS

DUQUESNE LIGHT COMPANY (~2 YRS TO COMPLETION)

CAPITAL COSTS	1,000,000
RAD EXP SAVING	1,800,000
	$2,800,000

REMAINING VS UNFINISHED PLANTS (13)

INDUSTRY	$29,000,000

"Pipe Rupture Hardware Minimization in Pressurized Water Reactor Systems"

S.M. Mukherjee, J.J. Szyslowski, N.A. Goldstein, V. Cexal, D.M. Norris, B. Beaudoin, D. Quiñones, and W. Server

ASME Piping and Pressure Vessel Conference
San Diego, Ca. July, 1987

Crack Opening Area for Leak-Before-Break Evaluation

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Abstract

Assessment of the quantitative leak flow rate of coolant from a crack is essential for the evaluation of leak-before-break behavior in primary piping systems. The leak rate is directly related to the crack opening area and depends on pipe size, applied stress and crack angle. The prediction method for analyzing crack opening area for a pipe is important for leak-before-break evaluation.

Several theoretical approaches for predicting crack opening areas are proposed on the basis of elastic or elastic-plastic fracture mechanics. One approach is the Tada and Paris formula which has been developed based on linear elastic fracture mechanics. Another approach is the method proposed by Kumar and German who have developed an engineering approach for crack opening displacement. The estimation is based on elastic and fully plastic deformation theory.

Round robin analyses for crack opening areas are performed using the above methods of Tada and Paris, and Kumar and German. The participants are four Japanese organizations. The pipe analyzed is 6-inch diameter Type 304 stainless steel with a circumferential through-wall crack. The applied load is bending moment without internal pressure.

The round robin analyses are classified into three cases. Case I is the analysis of crack opening area calculated by the Tada and Paris formula. The calculation results, conducted separately, coincide among the four participants.

Case II is the calculation example using the Kumar and German method. The values of material constants determined from the stress-strain curve are fixed to confirm the calculation procedures utilized by the four participants. Case II crack opening areas are also coincident among the four participants.

Case III is the main analysis in this study. The crack opening areas are calculated from the Kumar and German method using the stress-strain curve obtained from a smooth round specimen. The values of material constants are deterimined from the stress-strain curve at each organization. The results of the crack opening areas calculated by each participant are quite different.

It is concluded that for verification of leak-before-break concept the Tada and Paris method is suitable as the engineering prediction method for crack opening area in the present situation.

1. Introduction

Several types of leak detection systems such as temperature, pressure and flow sensors with associated instrumentation and alarms are mounted to detect coolant leakage from pressure boundary in nuclear power plants. These leak detectors have the ability to detect a leaking pipe before the cracks exceed critical values. However, assessment of the quantitative leak flow rate of coolant from a crack is essential for the evaluation of leak-before-break behavior in primary piping systems.

The leak rate is directly related to the crack opening area and depends on pipe size, applied stress, crack angle, etc. The prediction method for analyzing crack opening area for a pipe is important for leak-before-break evaluation.

Several theoretical approaches for predicting crack opening areas are proposed on the basis of elastic or elastic-plastic fracture mechanics[1-5]. One approach is the Tada and Paris formula which has been developed based on linear elastic fracture mechanics, including the effect of yielding near crack tip[4]. Kumar and German have developed an engineering approach for crack opening displacement[5]. The estimation is based on elastic and fully plastic deformation theory.

This paper describes the round robin anaysis results of crack opening area for Type 304 stainless steel pipe and recommends a method that can be used for leak-before-break evaluation.

2. Prediction Methods of Crack Opening Area

2.1 The Tada and Paris Method

Formulas for estimating the crack opening area for circumferentially and longitudinally through-wall cracks in pipes were recently developed by Tada and Paris. Their estimations are based on linear elastic fracture mechanics, including the effect of shell corrections.

The crack opening area A_t for a circumferential crack in a pipe receiving tensile stress σ_t is given by

$$A_t = \frac{\sigma_t}{E}(\pi R^2)\ I_t(\theta) \tag{1}$$

where θ is the half crack angle, E is Young's modulus , R is the mean radius and $I_t(\theta)$ is the nondimensional function.

The crack opening area A_b for a pipe subjected to bending stress σ_b is expressed as

$$A_b = \frac{\sigma_b}{E}(\pi R^2)\ I_b(\theta) \tag{2}$$

for a circumferentially through-wall crack. $I_t(\theta)$ in Eq.(1) and $I_b(\theta)$ in Eq.(2) are given as follows;

$$\begin{aligned} I_t(\theta) = 2\theta^2\Big[1+\left(\tfrac{\theta}{\pi}\right)^{3/2}\Big\{8.6-13.3\left(\tfrac{\theta}{\pi}\right)+24\left(\tfrac{\theta}{\pi}\right)^2\Big\} \\ +\left(\tfrac{\theta}{\pi}\right)^3\Big\{22.5-75\left(\tfrac{\theta}{\pi}\right)+205.7\left(\tfrac{\theta}{\pi}\right)^2-247.5\left(\tfrac{\theta}{\pi}\right)^3+242\left(\tfrac{\theta}{\pi}\right)^4\Big\}\Big] \end{aligned} \tag{3}$$

$$(0 < \theta < 100^\circ)$$

and

$$\begin{aligned} I_b(\theta) = 2\theta^2\Big[1+\left(\tfrac{\theta}{\pi}\right)^{3/2}\Big\{8.2-12.7\left(\tfrac{\theta}{\pi}\right)+19.3\left(\tfrac{\theta}{\pi}\right)^2\Big\} \\ +\left(\tfrac{\theta}{\pi}\right)^3\Big\{20.4-68\left(\tfrac{\theta}{\pi}\right)+165.2\left(\tfrac{\theta}{\pi}\right)^2-187.2\left(\tfrac{\theta}{\pi}\right)^3+146.7\left(\tfrac{\theta}{\pi}\right)^4\Big\}\Big] \end{aligned} \tag{4}$$

$$(0 < \theta < 100^\circ)$$

The effect of yielding near crack tip is incorporated using the plastic zone correction which is calculated from the relation

$$\theta_{eff} = \theta + \frac{K_{total}^2}{2\pi R\,\sigma_y^2} \tag{5}$$

where σ_y is the reference yield stress and K_{total} is the total stress intensity factor obtained simply by superposition of seperate factors of the stress intensity factor due to tension K_t and due to bending K_b. The formulas for K_t and K_b are given in Reference 4). Equations (1) and (2) were made of the typical value of mean radius to wall-thickness of R/t=10.

2.2 The Kumar and German Method

Crack opening displacement (COD) δ is obtained from analysis using the deformation theory of plasticity. The material behavior was modeled by the Ramberg-Osgood stress-strain representaion of the form

$$\frac{\varepsilon}{\varepsilon_o} = \frac{\sigma}{\sigma_o} + \alpha\left(\frac{\sigma}{\sigma_o}\right)^n \qquad (6)$$

$$\sigma_o = E\,\varepsilon_o$$

where α is a material constant, σ_o is a reference stress usually taken to be the yield stress, and n is the strain hardening exponent.

Consider a pipe containing a through-wall crack and subjected to bending moment. R denotes the mean radius, t is the wall thickness, 2θ is the total angle of the crack, $2a=2\theta R$ is the total length of the crack in the circumferential direction, and M is the applied bending moment. The elastic-plastic solution for COD δ is given by

$$\delta = f_2\left(a_e, \frac{R}{t}\right)\frac{M}{E} + \alpha\,\varepsilon_o\,a\,h_2\left(\frac{\theta}{\pi}, n, \frac{R}{t}\right)\left(\frac{M}{M_o}\right)^n \qquad (7)$$

where a_e is the effective crack length adjusted to include Irwin's small-scale yielding correction, and M_o is the limit moment for a cracked pipe under bending moment, and can be written as

$$M_o = 4\,\sigma_o\,R^2\,t\left(\cos\frac{\theta}{2} - \frac{1}{2}\sin\theta\right) \qquad (8)$$

In Eq.(7), function f_2 can be obtained from the elastic solutions given in Reference 5), and function h_2 represents the fully plastic solutions and is obtained from finite element shell analysis as was indicated in Reference 5).

It is shown that crack opening profile is elliptical from the linear elastic fracture mechanics and the crack opening area A is given by the area of the ellipsi as

$$A = \frac{\pi\,a\,\delta}{2} \qquad (9)$$

where $a(=R\theta)$ is half crack length.

3 Conditions for Round Robin Analyses

3.1 Cracked Pipe

Round robin analyses for crack opening areas were performed using the Tada and Paris, and Kumar and German methods previously. The participants in the analyses are the four Japanese organizations shown in Table 1.

A 6-inch diameter Type 304 stainless steel pipe was analysed under the pure bending moment without internal pressure at ambient temperature. The crack in the pipe was a circumferential through-wall crack at a 90° angle. These conditions are summarized in Table 2.

A pipe test conducted at Japan Atomic Energy Research Institute (JAERI)$^{6)}$ was refered in oder to compare the round robin calculations with a experimental result. The same conditions of the pipe test were used in the round robin analyses.

3.2 Cases of Analyses

The round robin anaylses are classified into three cases as shown in Table 3. Case I is the analysis of crack opening area calculated by the Tada and Paris formula. Case II is the calculation example using the Kumar and German method. The values of the material constants expressed by the Ramberg-Osgood representation are fixed to confirm the calculation procedures utilized by the four participants. Case III is the main analysis in this study. The crack opening areas are calculated from the Kumar and German method determining the material constants of Ramberg-Osgood representation using the given stress-strain curve of Type 304 stainless steel.

4 Results of Round Robin Analyses

4.1 Case I

The Tada and Paris method gives the crack opening area directly as expressed by Eq.(1). Assuming the crack opening profile to be elliptical, COD is obtained from the area using Eq.(9). Calculation results of the CODs are shown in Fig.1 as a function of the applied stress. The material constants used in this analysis are the same values obtained from the tensile test of the pipe material.

Although the calculated CODs are higher than the experimental data, they coincide among the four participants.

4.2 Case II

To confirm the calculation program of the Kumar and German method used by each participant, the CODs are calculated using the same material constant values. The CODs show no difference among the four participants as illustrated in Fig.2. Although the CODs are in good agreement with the experimental data, the number of strain hardening n and coefficient α fixed in the calculations are arbitary. This means that the calculation procedures do not differ among the participants.

4.3 Case III

The CODs are calculated by the Kumar and German method using the stress-strain curve of Type 304 stainless steel. Figure 3 shows the engineering stress-strain curves of the small and large range strains used in the calculations. The curves were obtained from a round smooth specimen at ambient temperature$^{6)}$. The curves of both small and large strain range were supplied to the participants to use arbitrarily.

Each participant individually decided on the values of material constants represented by the Ramberg-Osgood relation to fit the stress-strain curve of Fig. 3. The material constants of each participant for Case III are shown in Table 4.

Participants A and B calculated two cases of CODs using the small and large engineering stress-strain range respectively. Participant C decided on the material constants considering the small and large engineering stress-strain curve. Participant D used only the large range curve of Fig. 3(2) and obtained the CODs from the true and engineering stress-strain curves.

The Case III CODs using the stress-strain curves are quite different among the four participants.

5 Discussion

The Tada and Paris method consists of simple formulas and the crack opening area is determined by only σ_0 and E. This method would be expected to yield the same results among the calculators.

Calculated COD derived from the Tada and Paris method is higher than the experimental COD as shown in Fig. 1. This means a unconservative estimation for coolant leakage detectability. When the σ_0 is used as the flow stress instead of the yield stress in Eq. (5), where the flow stress is the average of yield stress and ultimate tensile strength, calculated COD is in good agreement with although slightly less than the experimental data. This tendency was observed in 6-inch diameter pipe with a crack angle other than 90°. The crack opening area calculated by the Tada and Paris method gives a conservative estimation for leakage detectability when the σ_0 is used as the flow stress instead of the yield stress.

On the other hand, for the Kumar and German method, α and n are determined from the stress-strain curve to fit the Ramberg-Osgood representation, where the strain range of the suitable curve in the calculation can not be precisely determined. From α and n, h_2 and f_2-functions are determined for each crack aspect ratio from the Table of Reference 5). When the α and n are decimals, f_2 and h_2 are predicted by extrapolation or interpolation of α and n. Caluculated crack opening area for leak-before-break evaluation is prone to diversify because of the difficulty in obtaining a unique solution.

6 Conclusion

Round robin analyses of crack opening area for circumferential cracked pipe were performed using the Tada and Paris, and Kumar and German methods. The COD calculated by the Kumar and German method using the stress-strain curve varied among the four participants. A unified technology commonly agreed upon is necessary for leak rate evaluation. The Tada and Paris method is appropriate for assessing the crack opening area for leak-before-break acceptance in the present situation.

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Table 1 Participant organizations for round robin analysis

Hitachi Ltd.
Ishikawajima Harima Heavy Industries Co., Ltd. (IHI)
Mitsubishi Atomic Power Industries, Inc. (MAPI)
Japan Atomic Energy Research Institue (JAERI)

Table 2 Cracked pipe experiment for round robin analysis

material		Type 304 stainless steel
pipe	diameter	165 mm (6-inch)
	thickeness	10.8 mm
crack	geometry	circumferential through wall crack
	angle	$2\theta = 90^{\circ}$
load		pure bending without internal pressure

Table 3 Cases for round robin cracked pipe

Test case	Scheme	Material constants (Room Temperature)	
Case I	Tada & Paris (NUREG CR3464)	fixed	E=194,000 MPa σ_0=243 MPa
Case II	German & Kumar (EPRI NP3607)	fixed	E=194,000MPa σ_0=243 MPa α=1.0 n=5.0
Case III	German & Kumar (EPRI NP3607)	determine from $\sigma-\varepsilon$ curve of Fig.3 (1) and (2)	

σ_0=yield stress, E=Young's Modulus

Table 4 Calcutating conditions for Case III conducted by each organiztion

Organization		$\sigma-\varepsilon$ curve used	α	n	σ_0 (MPa)	E (GPa)	ε_0	Remarks
A	A - 1	Fig.3(1)	2.270	10.218	243	194	0.00124	engineering $\sigma-\varepsilon$
	A - 2	Fig.3(2)	1.173	7.874	243	194	0.00124	
B	B - 1	Fig.3(1)	0.0368	9.046	110	219.5	0.0005	
	B - 2	Fig.3(2)	0.7193	5.995	176	194	0.0009	
C	C - 1	Figs.3	1.0	7.0	243	194	0.00124	
	C - 2	(1) & (2)	2.0	7.0	243	194	0.00124	
D	D - 1	Fig.3(2)	6.10	4.33	243	194	0.00125	true $\sigma-\varepsilon$ curve
	D - 2	Fig.3(2)	5.16	5.42	243	194	0.00125	engineering $\sigma-\varepsilon$

σ_0=yield stress, E=Young's Modulus, ε_0=yield strain

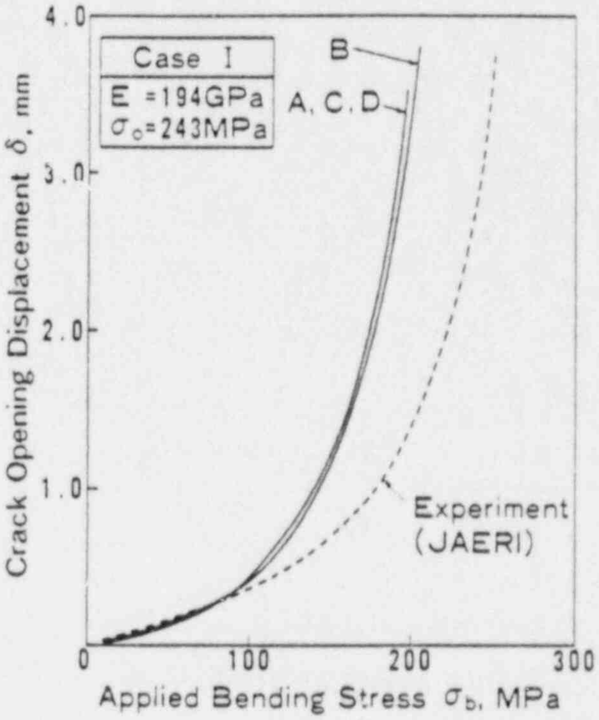


Figure 1 Round robin analysis result of crack opening displacement of 6-inch diameter Type 304 stainless steel pipe calculated by the Tada and Paris method.

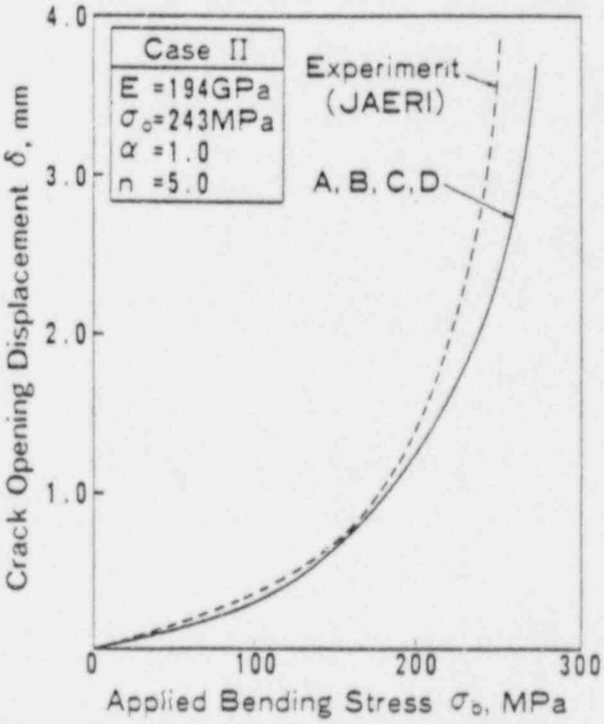
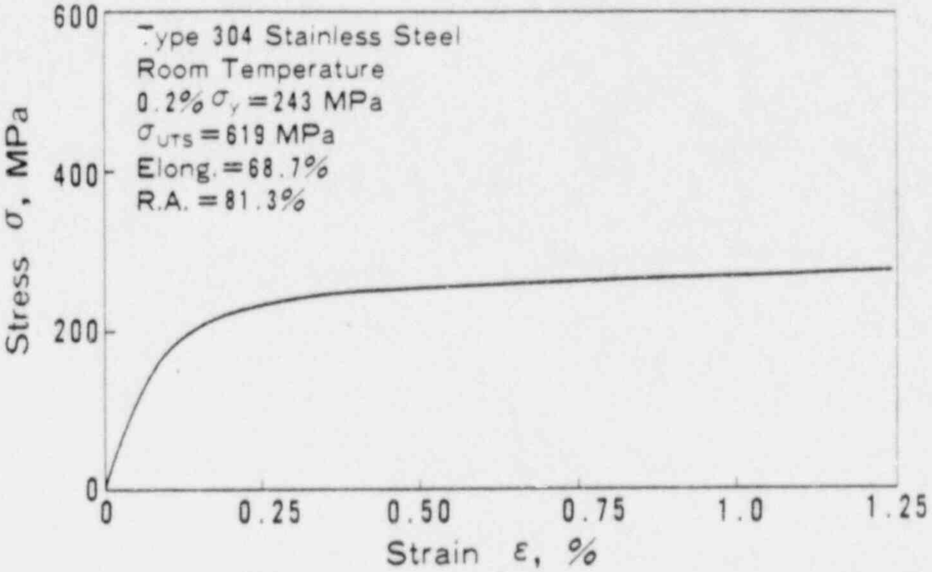
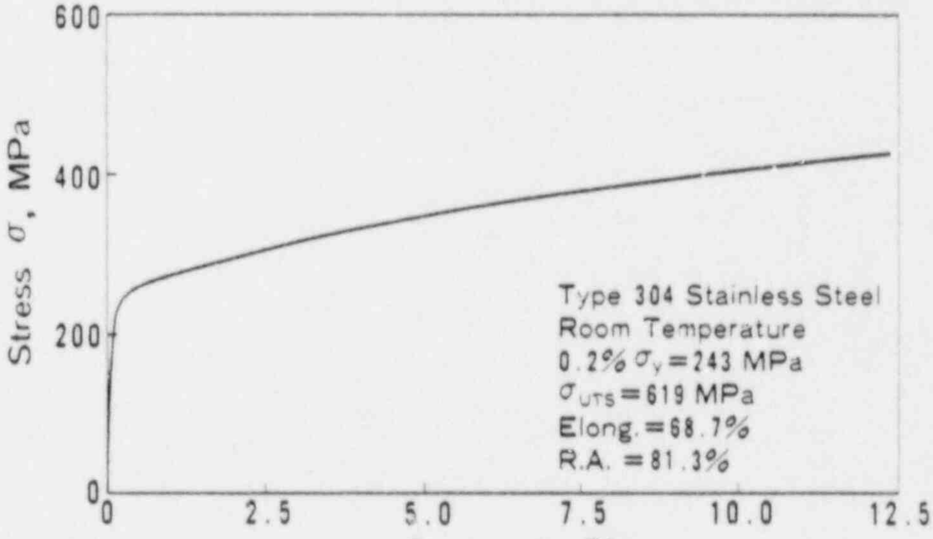


Figure 2 Crack opening displacement to confirm the calculation procedure of the Kumar and German method among the four participants.



(1) Stress-strain curve of Type 304 stainless steel in small strain range



(2) Stress-strain curve of Type 304 stainless steel in large strain range

Figure 3 Stress-strain curve obtained from a smooth round specimen[6)]

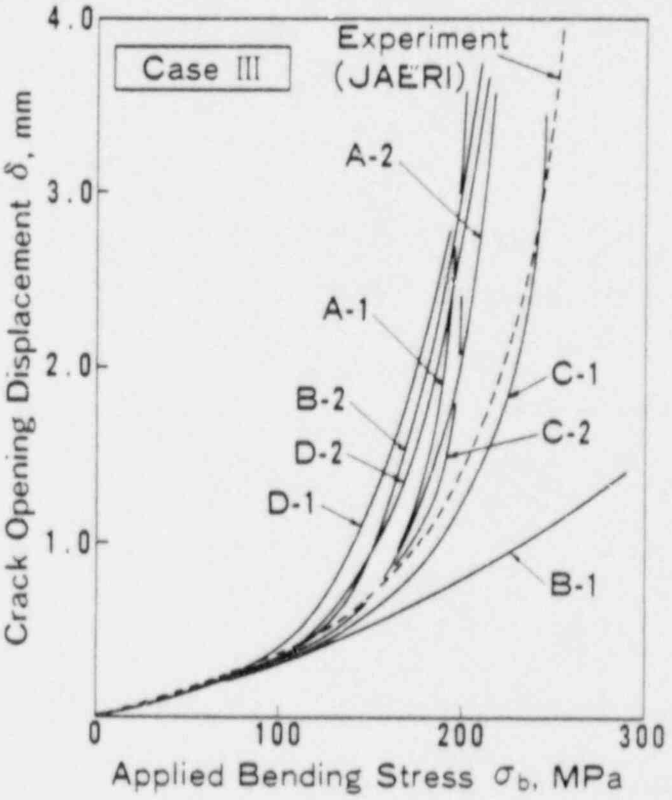


Figure 4 Round robin result of crack opening displacement calculated by the Kumar and German method using the stress-strain curve.

Crack Growth Study on Carbon Steel in Simulated BWR Environments

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1 INTRODUCTION

For further advancement of BWR structural integrity, life time estimation and prediction of final failure mode for BWR components are indispensable. These analyses require quantitative characterization of subcritical flaw growth during plant operations.

This study focuses on crack propagation for BWR piping systems of carbon steels. Fatigue crack growth data were generated in simulated BWR water environments using compact tension specimens. Metallurgical, environmental and stress factor effects on the crack growth behavior were investigated.

A surface crack study relative to this was described elsewhere (Hasegawa 1987).

2 EXPERIMENTAL

The 1T CT specimens with side grooves were machined from 20B Sch. 100 pipe of JIS G 3455 STS42 carbon steel and from 26B Sch. 80 pipe of STS49 carbon steel with the crack extension directions being parallel to the circumference of these pipes. The chemical composition of these steel pipes are shown in Table 1. A few CT specimens made of weld metal were fabricated from a butt weld joint of STS42 Sch. 100 pipes.

Prior to the experiments, CT specimens were pre-cracked in air at room temperature by fatigue at a stress intensity factor not beyond an initial value for the experiments.

Corrosion fatigue crack growth test as basic data was accomplished under 2×10^{-2} Hz triangular waveform with stress ratio 0.5 in 288°C pure water containing 8 ppm dissolved oxygen. Then, the test conditions were changed independently in the basic crack growth test condition in order to examine their effects on the crack growth rates, from 2×10^{-2} Hz to 2×10^{-3} Hz, from 0.5 stress ratio to 0.2 stress ratio and from 288°C to 150°C. Trapezoidal waveform tests were also carried out to identify stress corrosion cracking contribution to crack growth rates during corrosion fatigue tests. Furthermore, some specimens were tested in 288°C air-saturated steam environment. Fatigue crack growth rates in room temperature air were obtained for each specimen as the reference to those in water and steam environment as above. The test conditions are summarized in Table 2.

Crack extension was monitored by a compliance method and crack growth rates were determined by seven points incremental polynomial method in most of the tests. All the rates were plotted against applied cyclic stress intensity factor range, ΔK, in double logarithm scales.

3 RESULTS AND DISCUSSION

Fatigue crack growth rates obtained in room temperature air condition are shown in Fig.1. There was not any noticeable difference in crack growth rates between STS42 and STS49 pipes. No difference was also found in crack growth rates between the base material and the weld metal. These crack growth rates in low ΔK range exceeded the reference curve for carbon and low alloy steels in air of ASME Code Sec. XI (ASME 1983) as shown in Fig.1.

Crack propagation rates obtained in 288°C pure water under triangular waveform at 2×10^{-2} Hz and 2×10^{-3} Hz are also shown in Fig. 1. ΔK dependency of the crack growth rate was complicated. It seems that only the rates in low ΔK range followed Paris law relationship and the rates in high ΔK range were retarded or remained unchanged In some cases, the crack growth rate decreased followed by another increase. All the obtained crack growth rates except those under 2×10^{-3} Hz triangular waveform were located within the ASME code reference curve in water at stress ratio $R \geq 0.65$. In 2×10^{-3} Hz triangular waveform test, the rates were several times higher than the rates at 2×10^{-2} Hz at high stress intensity factor range and the rates were beyond the ASME code reference curve.

Crack growth rates in 288°C 8 ppm DO water under 2×10^{-3} Hz and 2×10^{-4} Hz trapezoidal waveform are shown in Fig.2. Note that no detectable crack growth was obtained under 2×10^{-4} Hz trapezoidal waveform until stress intensity factor range ΔK exceeding about 30 $MPa.m^{1/2}$ for both pipe steels.

The 2×10^{-3} Hz trapezoidal waveform test and the 2×10^{-2} Hz triangular waveform test did not give any noticeable differences in crack growth rates, however, the 2×10^{-4} Hz trapezoidal waveform test showed slightly higher crack growth rates at around ΔK=30 $MPa.m^{1/2}$. In the trapezoidal waveform tests, the waveforms at 2×10^{-3} Hz and 2×10^{-4} Hz were composed by holding for 450 seconds and 4950 seconds at the top load in the 2×10^{-2} Hz triangular waveform test, respectively. Therefore, crack growth rate acceleration in the 2×10^{-4} Hz trapezoidal waveform test was due to stress corrosion cracking for top load holding periods of 4950 seconds. Subtraction of crack growth rates under 2×10^{-2} Hz triangular waveform from those under 2×10^{-4} Hz trapezoidal waveform could give a stress corrosion cracking propagation rate (Kawakubo 1980). The rates from 3×10^{-10} m/s to 8×10^{-10} m/s are calculated at around K=60 $MPa.m^{1/2}$ as stress corrosion cracking propagation rates of carbon steel in 288°C 8 ppm DO water.

As for stress ratio, decrease from 0.5 to 0.2 shifted the crack growth rate curve to a high ΔK direction as shown in Fig.3. An effective cyclic stress intensity factor, K_{eff}, instead of ΔK, seemed to be available for plotting different sets of rates at stress ratios of 0.5 and 0.2.

Temperature change from 288°C to 150°C under 2×10^{-2} Hz triangular waveform apparently lowered the crack growth rate as shown in Fig.4. temperature air.

Environmental change from 8 ppm dissolved oxygen containing water to air-saturated steam lowered the crack growth rate as shown in Fig.5. Changing stress waveform from 10^{-2} Hz triangular to 10^{-3} Hz trapezoidal did not give any noticeable increase in growth rate in steam environment as well as that in 288°C 8 ppm DO water. And the rate in steam environment is generally lower than that in 288°C 8 ppm DO water.

Therefore, crack growth in steam environment can be evaluated conservatively by the rate in 288°C 8 ppm DO water.

Crack growth rate of weld metal in 288°C 8 ppm DO water under 2×10^{-2} Hz triangular waveform are shown in Fig.6. The growth rate is almost equal to or lower than those of the base materials.

4 CONCLUSION

Fatigue crack growth rates of carbon steels are obtained using compact tension specimen in high temperature water and in steam environments and the results are summarized as follows.

(1) Crack growth rates strongly depended on the frequency in triangular waveform. The rates became higher as the frequency lowered from 2×10^{-2} Hz to 2×10^{-3} Hz.
(2) Crack growth rate are almost equal under 2×10^{-3} Hz Trapezoidal waveform and under 2×10^{-2} Hz triangular waveform.
(3) Crack growth rate are lowered as stress ratio decreased from 0.5 to 0.2.
(4) Temperature change from 288°c to 150°C apparently lowered the crack growth rate.
(5) 288°C 8 ppm DO water environment gave much higher acceleration on crack growth rate than 288°C air-saturated steam.
(6) Crack growth rate for weld metal in 288°C pure water are almost equal to or lower than that of base metal.

ACKNOWLEDGEMENTS

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Table 1. Chemical Composition of STS 42 and 49 Carbon Steel

(Wt%)

	C	Si	Mn	P	S
STS 42	0.22	0.30	1.20	0.025	0.012
STS 49	0.20	0.33	1.16	0.026	0.012

Table 2. Test Conditions of Corrosion Fatigue Tests

Material	Environment			Temperature(C)			Dissolved Oxygen	Stress Waveform		Stress ratio		Frequency (Hz)		
	Water	Steam	Air	288	150	RT	8(ppm)	Triangular	Trapezoidal	0.5	0.2	2×10^{-2}	2×10^{-3}	2×10^{-4}
STS 42 20B Sch 100	○			○			○	○		○		○		
	○			○			○	○		○			○	
	○			○			○	○			○	○		
	○			○			○		○	○			○	
	○			○			○		○	○				○
	○				○		○	○		○		○		
		○		○			○	○		○		○		
		○		○			○		○	○			○	
			○			○		○						
STS 49 26B Sch 80		○		○			○	○		○		○		
			○			○		○		○				
Weld Metal	○			○			○	○		○		○		
			○			○		○		○				

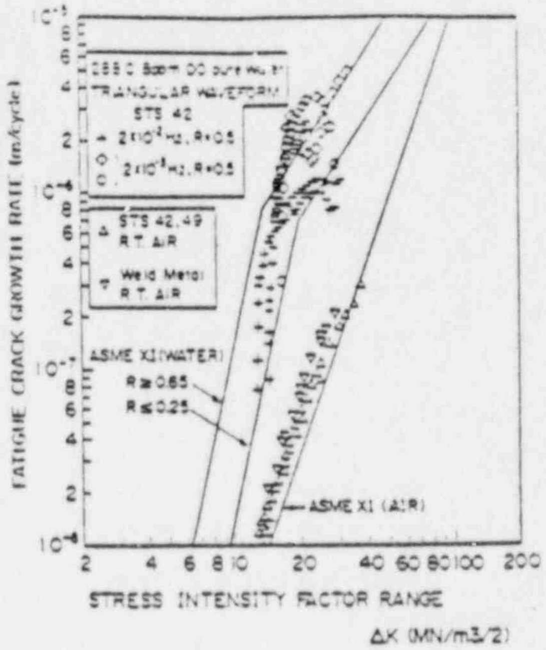

Fig. 1 Crack growth rates for STS 42 carbon steel obtained in triangular waveform tests

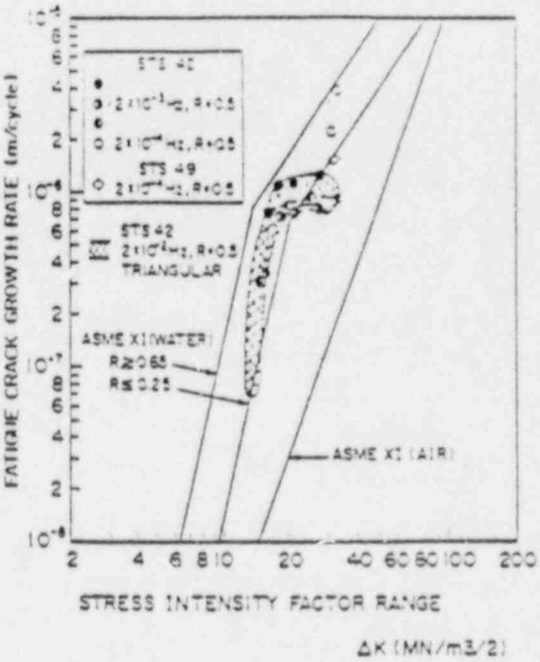

Fig 2 Crack growth rates for STS 42 and STS 49 carbon steels in trapezoidal waveform tests (288°C 8ppm DO, pure water)

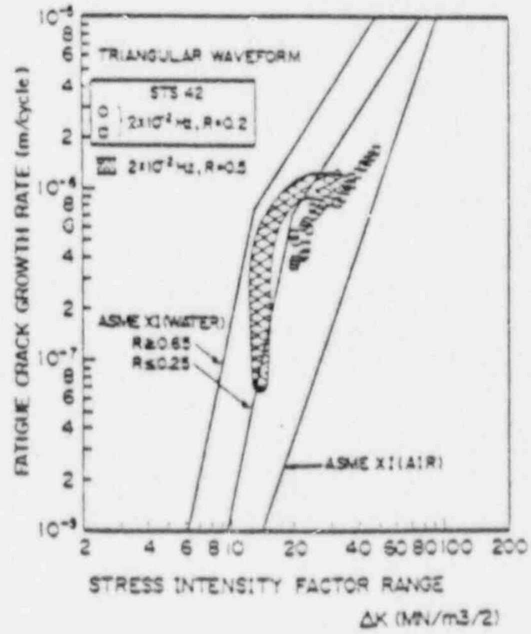

Fig 3 Crack growth rates for STS 42 carbon steel under stress ratio R=0.2 in 288°C 8ppm DO water

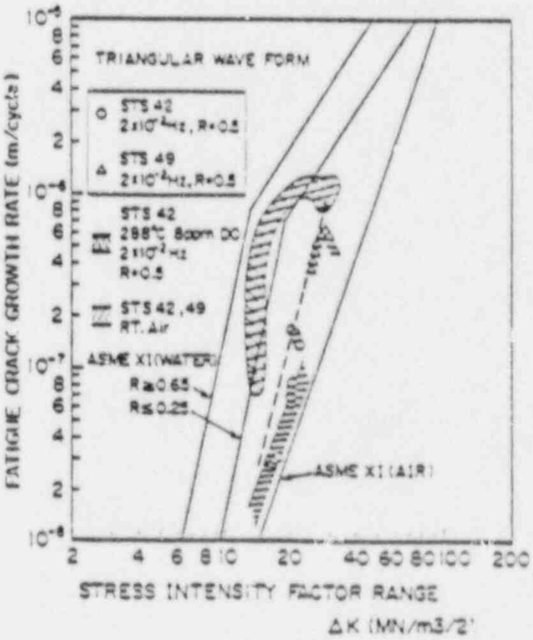

Fig 4 Crack growth rates for STS 42 and STS 49 carbon steels in 150°C 8ppm DO water

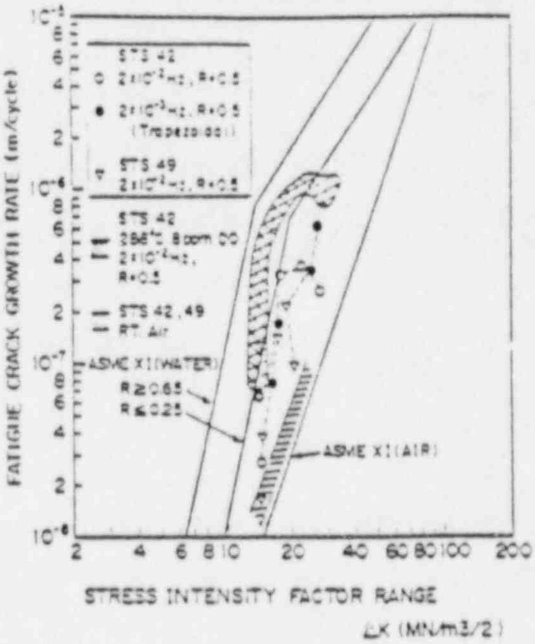

Fig 5 Crack growth rates for STS 42 and STS 49 carbon steels in 288°C air-satulated steam

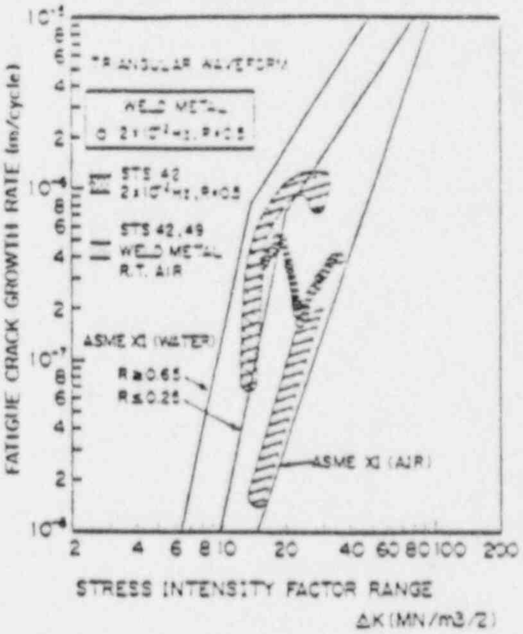

Fig. 6 Crack growth rates for weld metal in 288°C 8ppm DO water

Evaluation of Flaws in Nuclear Piping

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1 INTRODUCTION

We describe the development of flaw evaluation procedures by the ASME Task Group on Piping Flaw Evaluation for Section XI of the Boiler and Pressure Vessel Code (1). The application is to light water reactor piping. Existing procedures are reviewed for austenitic stainless steel and new procedures proposed for ferritic steels. We also summarize development of elastic-plastic fracture analysis methods needed in the development of the code evaluation procedures.

The objective is to determine an acceptable flaw size for a given load based on code safety margins. Our goal here is to present the philosophy, describe in general terms the technical basis, describe the current status, and give references for the details. The paper assumes the reader is familiar with tearing instability theory (2).

2 FLAWS IN STAINLESS STEELS

Fracture testing of austenitic stainless steel (3, 4) showed that failure occurs by ductile plastic collapse of the pipe cross section reduced by the cracked area. Based on these test results and load equilibrium considerations, equations and tables were developed in simple form amenable to code use. The material property required for this analysis is the flow stress, taken as three times the ASME code allowable design stress, S_m.

Limited specimen toughness data (5) and analysis showed that some stainless steel flux weldments failed at loads below the limit load. To predict this behavior we used elastic-plastic fracture mechanics to calculate the ratio of the load to produce unstable tearing, to the limit load for different pipe diameters. The reciprocal of this ratio was used to define a factor Z that would multiply the limit load associated with a given flaw size in code tables. The result was to reduce the allowable flaw size for flux welds relative to the base material for a given load. A general formula is given in Appendix A.

The flaw acceptance standards, the evaluation procedures and acceptance criteria, and supporting information is given in the ASME code in Section XI IWB-3500, IWB-3640, and Appendix C respectively. Code Case N-436 gives the formulas of Appendix A. More detailed information is given in a technical support document (6).

3 FLAWS IN FERRITIC STEELS

Development of evaluation procedures in ferritic steels is complicated by the strong dependence of toughness on temperature and the variety of piping, weld materials, and welding processes. These difficulties and earlier absence of toughness data have delayed recommendations for code evaluation procedures, however, recent proposals have been made that are discussed here.

The evaluation procedures parallel the methods discussed above for austenitic steels. For flawed ferritic materials that fail by ductile tearing, Z factors are provided. For materials that fail by plastic collapse, limit-load tables and equations (see Appendix A) are provided that give reasonable bounds to the database using a flow stress equal to 2.4 S_m. Equations are provided for brittle fracture. Both axial and circumferential flaws are considered but we discuss only the circumferential work here.

We have obtained fracture and strength data (7,8) on the most common piping steels used in U.S. plants. These are A516-70, A106B, A106C, A155, 7000 and 8000 series weldments, and submerged arc welds. Recommended bounding material data are given in Table 1 with the associated Z factors, which are discussed in more detail below. Table 2 shows Z factors for user-specified material data.

Table 1. Material Data (7) and Z Factors (9) for Carbon Steel Base Metals and Weldments

Material	T > 200F		T $\leq$ 200F		Z Constants[1]	
	σ_y ksi	J_{IC} in-lb/in^2	σ_y ksi	J_{IC} in-lb/in^2	C_1	C_2
Base Metal	27.1	600.	27.3	45.	1.20	0.0210
70XX Weld	27.1	600.	27.3	45.	1.20	0.0210
80XX Weld	27.1	350.	27.3	45.	1.35	0.0184
SAW	27.1	350.	27.3	45.	1.35	0.0184
High Mn Mo	27.1	350.	27.3	45.	1.35	0.0184

[1] $Z = C_1 * [1 + C_2 * A * (OD - 4)$ where OD is the nominal pipe diameter in inches, $A = [0.125\ (R/t) - 0.25]^{0.25}$ for $5 \leq R/t \leq 10$, and $A = [0.4(R/t) - 3.0]^{0.25}$ for $10 < R/t \leq 20$.

Table 2. Z Factors (9) for User-Specified Material Data for Carbon Steel Base Metals and Weldments.

Material	σ_y (ksi) Region	J_{IC} (in-lb/in^2) Region	Z Constants[1]	
			C_1	C_2
Base Metal &	27.1 ≤ ≤ 40.0	600 ≤ 1050	5.475	0.0210
70XX Weld	27.1 ≤ ≤ 40.0	≥ 1050	4.699	0.0152
80XX Weld	27.1 ≤ ≤ 40.0	350 ≤ < 600	6.159	0.0184
SAW &	27.1 ≤ ≤ 40.0	600 ≤ < 1050	5.475	0.0210
High Mn Mo	27.1 ≤ ≤ 40.0	≥ 1050	4.699	0.0152

[1] $Z = C_1 \cdot [1 + C_2 \cdot A \cdot (OD-4)]/\sigma_y^{0.46}$, where

OD is the nominal pipe diameter in inches,

$A = [0.125\ (R/t) - 0.25]^{0.25}$ for $5 \le R/t \le 10$,
and $A = [0.4(R/t) - 3.0]^{0.25}$ for $10 < R/t \le 20$.

It is necessary to first determine the failure mechanism appropriate to the flaw geometry, material, and temperature. This is based on a modified R6 (10, 11) flaw evaluation procedure (see Figure 1). This procedure uses an interaction diagram in a space of normalized brittle fracture toughness to plastic limit load that spans plastic collapse, ductile tearing, or brittle fracture. A bounding toughness-collapse line is derived (8) and the space is divided into the three failure regions. An assessment point plotted in this space (using specified formulas for stress intensity factor and limit load) determines the failure mechanism.

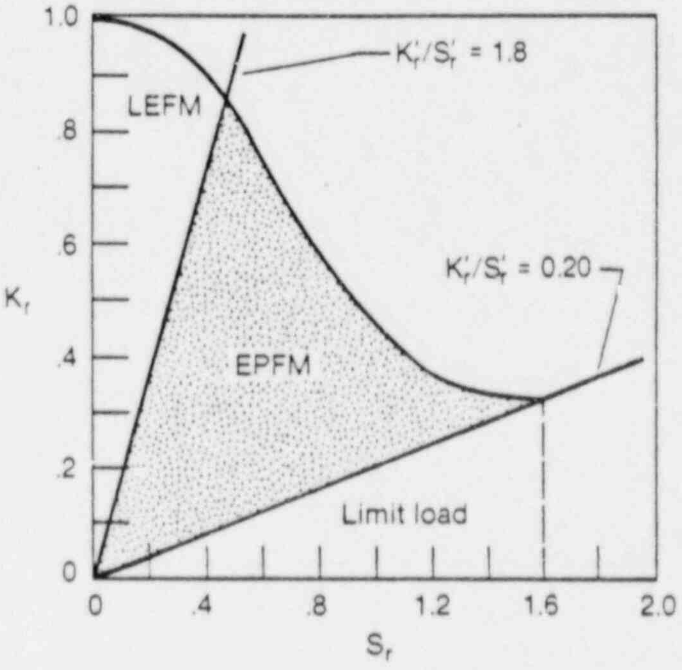

Figure 1. Ductile Fracture Failure Analysis Diagram to Determine Failure Mechanism

The evaluation sequence is shown in Figure 2. The upper part of the sequence addresses material properties. Bounding properties are provided in the absence specific application data. The middle section then determines the failure mechanism , and the bottom section provides the appropriate evaluation tables or equations.

4 DUCTILE FRACTURE MECHANICS

Tests (12) have shown that flawed piping can fail at loads below those associated with plastic collapse. The development of procedures describing these failures becomes expensive if finite element analysis is used because of the nonlinearity introduced by the plasticity and the element detail necessary at the crack tip. For these reasons we have developed elastic-plastic estimation formulas (13,14,15) based on the J integral that have wide application and that have been applied here to compute the limit load correction factor Z. Recent comparisons with pipe experiments (16) show the accuracy of these solutions.

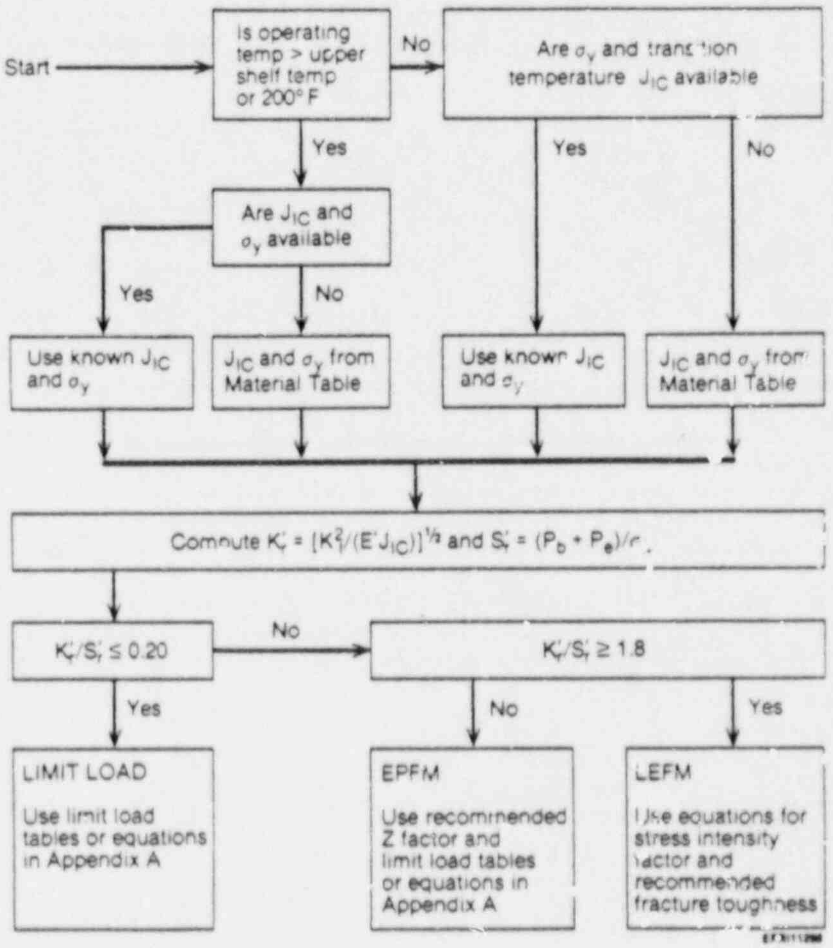

Figure 2. Flaw Evaluation Sequence for Carbon Steel Pipes. P_e is the Thermal Expansion Bending Stress.

The Z calculation uses pure bending solutions (13, 15) for a circumferential through-the-wall crack. These solutions relate the crack-driving force J to the geometry, material properties, and applied loading. We used these solutions to compute the applied J (and hence the applied load) at which the applied tearing modulus T exceeds the material tearing modulus. The process is repeated for different crack angles, pipe diameters, and R/t ratios. The calculations use code minimum yield strength and bounding J resistance curves. Our results for ferritic steels are given in Tables 1 and 2.

5 COMPARISON WITH PIPE TESTS

We have applied the procedures described above to tests on pipes containing circumferential surface flaws. The objective was to compare the test failure loads with the predicted failure loads. Test geometries and results are given in Table 3.

Case 1 uses known pipe properties to compute K'_r/S'_r and the equations of Appendix A to compute the failure loads. For those pipes failing by ductile tearing, we used recommended Z constants shown in Table 2. The S_r calculation uses the equations of Appendix A with the flow stress replaced by the code minimum yield stress.

Case 2 uses the bounding properties from Table 1 for the base material and the Z factor from that table.

Table 3. Application of Methodology to Carbon Steel Pipe Tests.

				$\frac{\text{Test Failure Load}}{\text{Predicted Failure Load}}$	
Data Source	Experimental Number	$\frac{a}{t}^{(1)}$	$\frac{\theta}{\pi}^{(2)}$	Case 1 Known Data & Eqs.	Case 2 Bounding Data & Eqs.
NRC/BCL (12)	4112-5	0.63	0.51	1.18	1.49
	4112-6	0.68	0.50	1.51	1.87
	4112-7	0.63	0.53	1.65	1.92
	4112-8	0.66	0.53	1.65	1.84
	4112-9	0.66	0.54	1.61	1.88
	4115-1	0.70	0.39	1.42	1.59
	4131-8	0.68	0.48	1.49	1.67
	4131-4	0.66	0.53	1.73	1.94
JAERI (17)	CS-11	0.25	0.25	1.45	1.80
	CS-12	0.50	0.25	1.35	1.68
	CS-13	0.75	0.25	1.22	1.51
	CS-15	0.50	0.125	1.27	1.58
	CS-16	0.50	0.50	1.42	1.77

(1) Ratio of Crack Depth to Wall Thickness

(2) Ratio of Crack Length to Pipe Circumference

5 SUMMARY

The procedures described above are appealing in that they represent a simple fracture modification to the limit load of the flawed pipe and unify procedures for stainless and carbon steel. The simplifications used in the development introduce conservatisms that may be avoided by more exact analysis proposed for the code.

The conservatisms arise from the use of bounding material properties, use of compact specimen data for pipe fracture toughness, use of through-the-wall flaws for the Z factor calculation, use of the lower bound Z for a given crack length, use of pure bending for the Z determination, and the assumption in the proposed limit-load tables that $P_m = 0.5S_m$.

The development has identified uncertainties that require more effort before these conservatisms can be reduced. In the experimental area, pipe and specimen fracture data on weldments in the as-welded and heat-treated states are needed. The difference between fracture toughness from compact specimens and pipes needs to be established and more definitive pipe toughness calibration functions developed. The assumptions of the plastic fracture analysis methodology (13) were justified using laboratory specimen results; more experience is required with these solutions applied to real structures. New solutions are needed especially for surface flaws and axial flaws and the interpolation functions for crack-opening displacement.

The task group is proceeding with this development for carbon steels with the objective of having the procedures in place by the end of 1987.

APPENDIX A

The code procedures may be closely approximated for both carbon and stainless steel using equilibrium relationships between load, geometry, and material strength at plastic collapse. For $\theta + \beta \leq \pi$,

$$P'_b = (2/\pi)\sigma_f\ [2\sin\beta - (a/t)\sin\theta]\ \text{(A-1)}$$

where $\beta = [(\pi - \theta\frac{a}{t}) - (P'_m/\sigma_f)\pi]/2$.

If $\theta + \beta > \pi$, then

$$P'_b = (2/\pi)\ \sigma_f\ [(2 - \frac{a}{t})\sin\beta],\ \text{(A-2)}$$

where $\beta = \pi(1 - \frac{a}{t} - P'_m/\sigma_f)/(2 - \frac{a}{t})$

from which the loads at failure may be calculated. The pipe fails when the average flow stress in the flawed section, σ_f, reaches a critical value. The nomenclature is defined in Figure A-1.

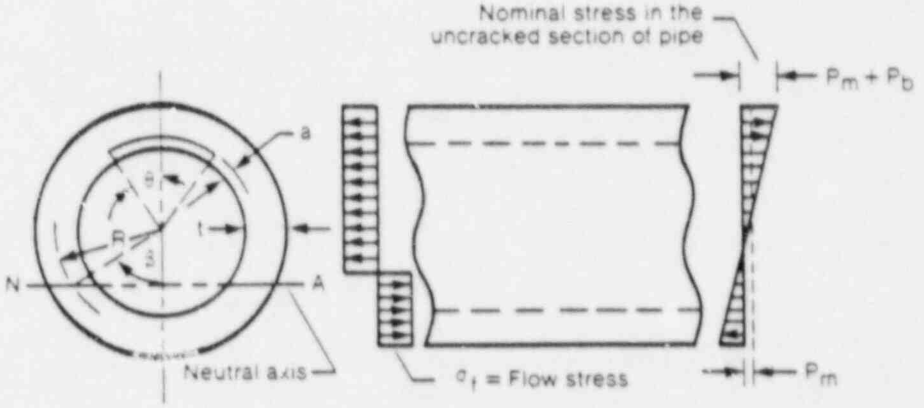

Figure A-1. Circumferential Surface Flaw Geometry and Assumed Plastic Collapse Stress Distribution

It is more useful to specify allowable loads with some factor of safety, SF, than the collapse loads. We have elected to apply this safety factor to the sum of the bending and membrane loads. The relation between collapse loads (primed quantities) and allowable loads is

$$P'_b = Z \cdot SF \quad (P_m + P_b) - P_m \qquad (A-3)$$

In this equation P_m' is taken equal to P_m and we have introduced the Z factor discussed earlier.

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NEW SWEDISH REGULATIONS FOR SAFETY OF PRESSURIZED COMPONENTS

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The Swedish Nuclear Power Inpectorate (SKI) has recently issued new regulations for safety of pressurized components in nuclear power plants. Since these rules represent to some extent a new thinking in the Swedish licensing policy it is believed that it may be of some interest to the international nuclear community to obtain information about it.

Of many novel items in FTK only two aspects will be briefly discussed in this note. The first and perhaps most important deals with the principles of selecting objects for inservice inspections.

Earlier this has been based the conventional safety class division much as in the ASME Code. Now, excepting the reactor pressure vessel and its internal parts, a new classification into three control groups A-C is to be made. In control group A 75 percent of the control objects (welds in piping, T-joints, pumps etc.) shall be subject to at least one inservice inspection within each six-year period. The corresponding percentage for control group B is 10. For control group C rules for the inspection of non-nuclear equipment are prescribed.

Compared to the earlier system this means a drastic concentration of the control resources to the high risk systems. Depending on the type of reactor the total control volume may be either increased or decreased.

The selection of objects into the control groups is based on the estimated failure risk of the component and on its importance for the nuclear safety. A leading thought is that these two factors should first be considered separately. Thus each each object is assigned a fracture index I-III, where I represents the highest estimated failure probability and III the lowest. The component is also given a consequence index 1-4 where 1 represents the highest importance for the nuclear safety and 4 the lowest. Although the consequence index is analogous to the safety class concept these two are quite different in application. When assigning the consequence index to a component all pertinent information about its importance for the total safety should be considered such as e.g performed probabilistic risk assessments (PRA). Since such studies have not been performed for all plants and all systems a type classification has been given which is based partly on PRAs performed up to date and partly on engineering judgements. For example piping directly connected to the pressure vessel in a boiler reactor is assigned a higher consequence index if the connection is above the waterlevel than if it is below. It is intended that the consequence classification should be revised continously on a plant to plant basis as new information about the safety implications comes to light.

The failure index is a qualitative measure of the failure risk. This index is assigned to a particular piece of equipment according to the following general principles.

- Equipment that is expected to bear high loads or be subjected to damage is assigned fracture index I.

- Equipment with normal margins against failure or subjected to normal wear processes is assigned fracture index II.

- Equipment with very good margins against failure or is subjected to insignificant wear processes is assigned fracture index III.

In order to perform this assigment of the fracture index a set of factors that may altar the fracture index to a higher or lower value is given in the rules. These factors have been determined by engineering judgement of research results and service experience. For example the value the fracture index should be lowered where there is a risk for stress-corrosion cracking. This is judged to be the case in austenitic steel when the temperature exceeds 150 C and the carbon content exceeds 0.04 percent. The fracture indexing should be continously revised as new information is made available. The values of the fracture and the consequence indices determine the control group for the object according to the following table.

Consequence index

Fracture index	1	2	3	4
I	A	A	B	C
II	A	B	C	C
III	B	C	C	C

The final classifications has to be approved by SKI.
The second important item regards the rules for continued use of damaged equipment e.g. components where cracking has occured. The official inspecting agency (The Swedish Plant Inspectorate) may allow continued service during a specified service period of objects where cracks have been detected provided that the following conditions are satisfied.

i) The component satisfies the requirements of ASME XI regarding the acceptance of components with cracks and other defects.

ii) It shall be shown by use of the R6-method the ample margins exist against fracture mechanisms not covered by ASME.

iii) For equipment in control group A and B it shall be shown that leak before break is highly probable.

The inspecting agency shall also consider if following up inspections are needed.

SESSION 7: FRACTURE MECHANICS (PART 2)

Chairman: P. Jamet, CEA, France

Application of Probabilistic Fracture Mechanics to Leak-Before-Break

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1. Introduction

A leak-before-break (LBB) analysis of a pressurized component has to be performed by application of fracture mechanics principles in order to describe the extension of cracks, mainly welding cracks. This requires special material data and the loadings of the component to be known as well as information about the size and location of possible cracks in the component. If these data are known exactly and the fracture mechanics failure model is accurate, then it can be decided whether a component can fail by leak or by a complete rupture.

Because of the uncertainty of the input data and the lack of accuracy of failure models, safety factors have to be introduced in a deterministic leak-before-break analysis. The selection of appropriate safety factors is always a problem in component design. In a leak-before-break analysis it is even more complicated /1/.

Instead of applying safety factors in the deterministic assessment of leak-before-break it may be more appropriate to use a probabilistic approach, where the fixed values of the input data are replaced by probability distributions /2,3/. The results of such an analysis are probabilities for leak and for break which increase with time. These probabilities can be the basis for a leak-before-break assessment.

In this paper first the deterministic approach for LBB will be described, then the principles for a probabilistic analysis will be indicated. As an example, results for a specific component will be presented.

2. Deterministic leak-before-break analysis

A deterministic LBB analysis starts from the most dangerous flaw in the component. For simplicity, we are considering here a semi-elliptical surface flaw of depth a_i and length $2c_i$. This crack extends subcritically in depth and in length due to fatigue or creep or stress corrosion. Fatigue is the most important cause of the subcritical extension of cracks and considered here exclusively. The crack growth rate and the developing shape of the crack depend on the type of loading - cyclic tension or bending, cyclic thermal shock - and on the material properties. In case of fatigue crack growth the material properties are described in terms of the relation between crack growth rate per load cycle (da/dN or dc/dN) and the range of the stress intensity factor ΔK. The simplest relation is the Paris-law:

$$\frac{da}{dN} = C \cdot (\Delta K)^m \qquad (1)$$

with the material parameters C and m; however, more complicated relations can also be applied. The calculation of crack extension for surface cracks has to take into account the variation of ΔK along the crack front.

For a given maximum stress σ_{max} the crack will grow suddenly through the wall at a critical combination of depth and length a_{cL} and c_{cL}. This event is called local instability. The relation between critical stress for local instability σ_{cL} and a_{cL}, c_{cL} has to be determined using linear-elastic or elasto-plastic relations. After wall penetration the crack may be arrested at a length $2c_a$, which for simplicity here is set equal to the length at local instability $2c_{cL}$. For a through-wall crack a relation exists between the critical length $2c_{cG}$ and the critical stress σ_{cG} for unstable extension in length (global instability).

Leak-before-break occurs if for the applied maximum stress the critical crack size c_{cG} is larger than c_a. In a complete leak-before-break analysis it has also to be shown that a leak can be detected /1/. Here it is assumed for simplicity that all leaks created can be detected.

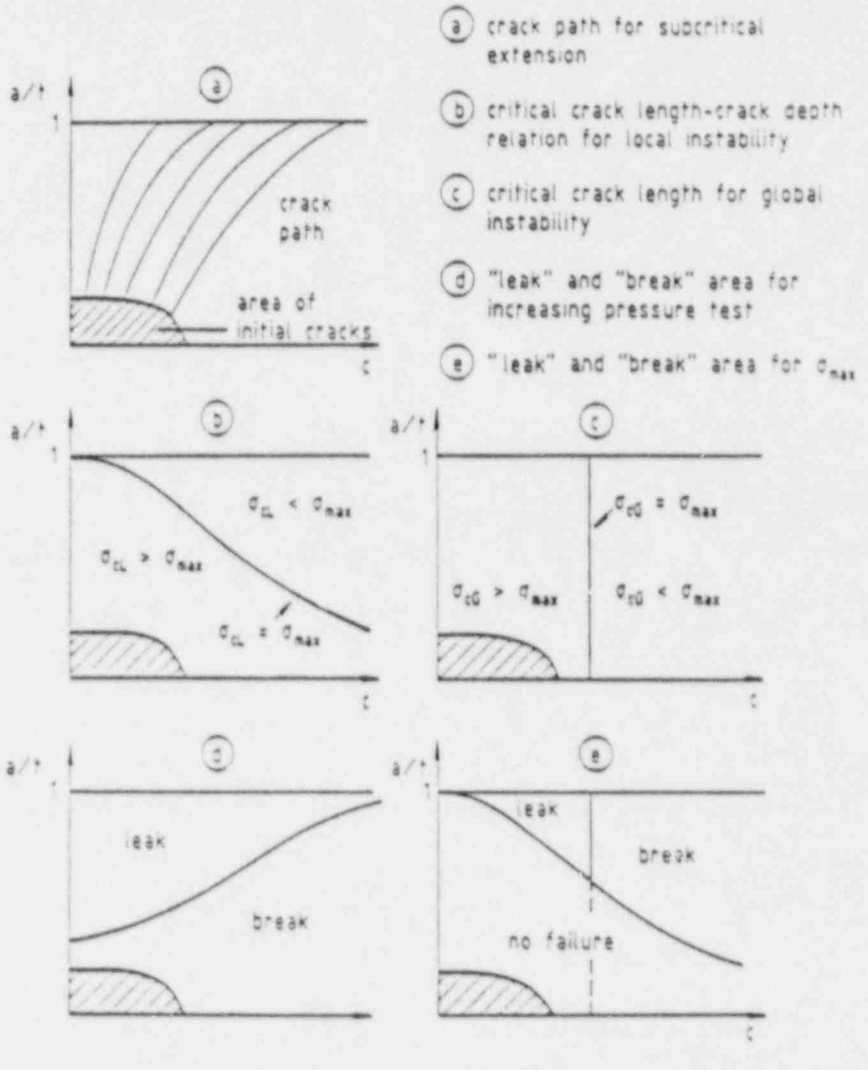

Figure 1: Leak-before-break diagrams

The results of a leak-before-break analysis can be presented in a leak-before-break diagram (fig. 1). In this diagram the crack depth a related to the wall thickness t is plotted versus the crack length.

The line in the left corner is the boundary line for cracks which may be present in a component after inspection. These cracks are growing in length and in depth along the lines shown in fig. 1. The form of these lines depends mainly on the type of loading, e.g. cyclic tension or cyclic bending.

For a given maximum stress two additional curves can be drawn in fig. 1. One curve determines the critical crack sizes for local instability. All cracks above this curve have a critical stress which is less than the maximum stress, and therefore they grow unstably through the wall. All cracks below this curve are stable. The vertical line characterizes the critical crack length c_{cG} for global instability at the given maximum stress. All through-wall cracks larger than c_{cG} lead to global instability. The diagram in fig. 1 can be divided into the three regions "no failure", "leak" and "break". Under the assumption that a surface crack of length 2c will create a through-wall crack of the same length after local instability, the boundary between the leak and the break areas is given by the intersection of the curves for local and global instabilities. Two corrections can be made to this assumption. The through-wall crack may be somewhat larger than the surface crack length. In addition, leak detectability has to be taken into account. The through-wall crack opens under load. The leak rate depends on the leak area, which is a function of the crack length and the stress, and on the surface roughness. From this dependency and the leak rate detectability a critical through-wall crack length for leak detection c_{LR} can be obtained. If c_{LR} is larger than c_{cG} then the leak cannot be detected.

In fig. 2 the leak-before-break diagram is shown again with an additional curve (b-b) which determines the maximum crack sizes at the end of the life of the component according to a conservative deterministic design. A leak-before-break assessment is possible on two levels.

On level I it is sufficient to show that there is enough safety margin between the end-of-life cracks and the curves delimiting the break area. This is the usual deterministic design of a component.

On level II it is assumed that all cracks have penetrated the wall. Leak-before-break is ensured if the safety margin between the maximum possible through-wall crack and the critical crack for global instability is large enough. This is a more restrictive safety barrier than the level I argument. Level II requires that information is available about the possible development of the shape of a crack.

Figure 2 b shows an example where leak-before-break is ensured for level I but not for level II.

The information necessary for a deterministic leak-before-break assessment is listed below:

- Maximum flaw size after fabrication and non-destructive inspection and the shape and the location of the maximum flaw.
- Information about subcritical crack extension. For fatigue crack growth the relation between crack growth for one cycle da/dN and range of the stress

intensity factor ΔK has to be known.

- Load cases to obtain the range in stress $\Delta\sigma$ and the maximum stress σ_{max}.
- A procedure to calculate the changing shape of the fatigue crack taking into account the variation of ΔK along the crack front.
- Relation between the maximum stress and the critical crack size for local instability a_{cL}, c_{cL}.
- Relation between maximum stress and critical crack size c_{cG} for global instability.
- Leak rate as a function of crack size and stress.
- Critical leak rate for detection.

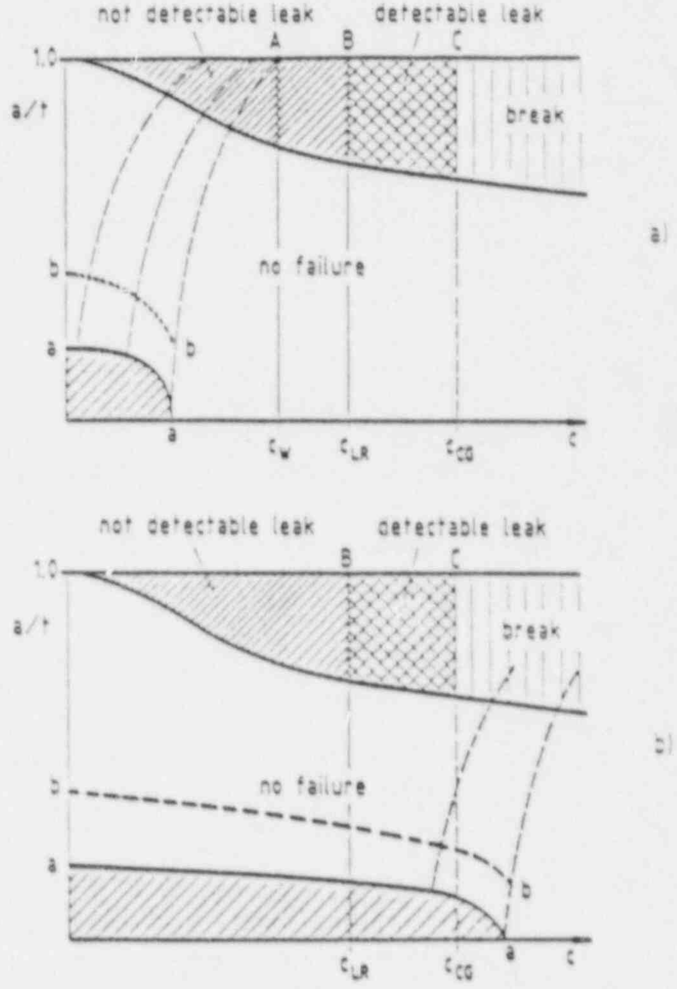

Figure 2: Leak-before-break diagrams for different initial flaw sizes

3. Probabilistic assessment

It is obvious that long cracks can lead to break at least for level II considerations where it is assumed that all existing cracks are growing through the wall. Therefore, the probability of break is related to the probability of having long cracks in the component. This calls for a probabilistic assessment of the leak-before-break behaviour.

In a probabilistic fracture mechanics analysis, the maximum flaw, the maximum load and the worst possible material parameters are replaced by their respective probability distributions. The failure probability of a crack containing structure is then equal to the probability of finding a crack with a length exceeding the critical crack length determined by the specific values of the random material properties and applied loads present at the location of the crack. A calculation scheme is shown in fig. 3.

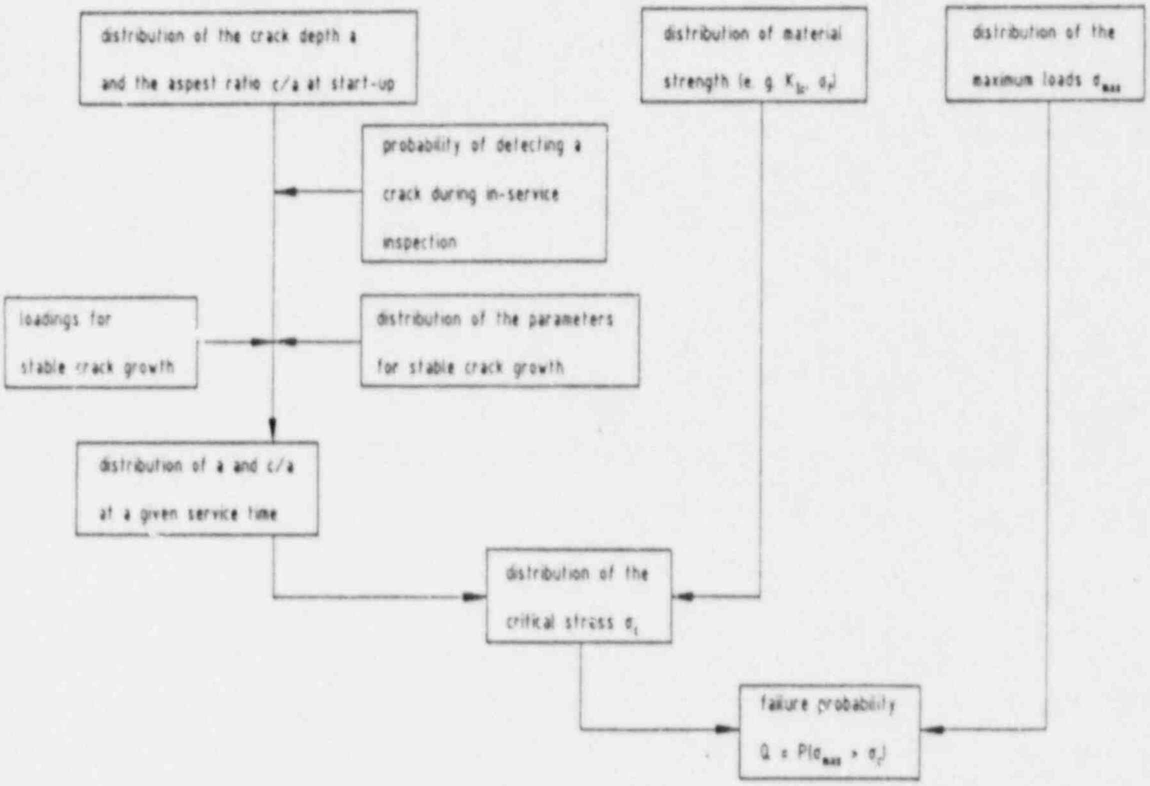

Figure 3: Calculation scheme for a probabilistic fracture mechanics analysis

In order to simplify the formulae in the following sections, the critical stress σ_c will be used to determine the onset of unstable crack extension instead of the critical crack length c_c. If, for example, an elasto-plastic analysis using the two-criteria-approach of CEGB /5/ is performed, the critical stress is given by:

$$\sigma_c = \frac{2}{\pi}\cdot\sigma_L\cdot\arccos\,(\exp\,(-\left(\frac{\pi}{8}\left(\frac{K_{Ic}\cdot\sigma_c}{K\cdot\sigma_L}\right)^2\right) \qquad (2)$$

where K_{Ic} denotes the fracture toughness, σ_L the plastic limit load and K the stress intensity factor.

Describing the scatter in the maximum applied stress by the probability density $f(\sigma_{max})$ we obtain for the probability of failure caused by a crack of the specific length c, depth a at a given location in the component with the flow stress σ_f and the fracture toughness K_{Ic}:

$$Q(a,\, c,\, \sigma_f,\, K_{Ic}) = \int_{\sigma_c}^{\infty} f(\sigma_{max})\, d\sigma_{max} \qquad (3)$$

Integration over all possible crack lengths and depths yields the probability that one crack of arbitrary size leads to failure if the material properties are kept constant:

$$Q(\sigma_f,\, K_{Ic}) = \int_0^t f(a) \int_0^{\infty} f(c/a) \int_{\sigma_c}^{\infty} f(\sigma_{max})\, d\sigma_{max}\, d(c/a)\, da \qquad (4)$$

where t denotes the wall thickness of the component under consideration. In eq. (4) it is assumed that the aspect ratio c/a and the crack depth a are independent random variables describing the scatter in a and c. Including the probabilities $f(K_{Ic})$ and $f(\sigma_f)$ for the fracture toughness K_{Ic} and the flow stress σ_f, we finally get the failure integral:

$$Q = \int_0^{\infty} f(\sigma_f) \int_0^{\infty} f(K_{Ic}) \int_0^t f(a) \int_0^{\infty} f(c/a) \int_{\sigma_c}^{\infty} f(\sigma_{max})\, d\sigma_{max}\, d(c/a)\, da\, dK_{Ic}\, d\sigma_f \qquad (5)$$

The failure probability for a component containing an average of M independent cracks is equal to:

$$P_f = 1 - \exp(-MQ) \qquad (6)$$

Stable crack growth shifts the distributions of a and c/a, and the failure integral Q and, consequently, P_f increase continuously with the number of load cycles.

In a probabilistic leak-before-break analysis the failure integral eq. (5) is split into two parts, the leak probability Q_L and the break probability Q_B:

$$Q = Q_L + Q_B \qquad (7)$$

If σ_{cL} and σ_{cG} denote the critical stresses for local and global instabilities, respectively, the break probability follows from eq. (5) with the critical stress

$$\sigma_c = \max(\sigma_{cL}, \sigma_{cG}) \tag{8}$$

because break occurs if the maximum applied stress is larger than the critical stress for both local and global instability. The leak probability is given by

$$Q_L = Q - Q_B \tag{9}$$

where the critical stress for the failure integral Q eq. (5) is equal to σ_{cL}.

Because of stable crack growth Q_L and Q_B change with time. Eqs. (5) - (9) can still be used with f(a), f(c/a) replaced by the corresponding time-dependent distributions if all leaks are detected, i.e. if

$$c_{LR} < c_{cG} \tag{10}$$

holds for all possible configurations. Otherwise the non-detected leaks continue to grow and the leak probability has to be modified accordingly /4/.

In a deterministic analysis the maximum crack sizes at the end of life are given by the limit curves in fig. 2. Leak-before-break behaviour at level I is ensured if there is a sufficiently large safety margin against failure. In a probabilistic evaluation there is always the possibility of failure because large cracks are not totally excluded. These cracks can reach the failure domain and cause a leak or a break. In this sense a probabilistic leak-before-break analysis corresponds to the deterministic level II procedure. A component is considered to be leak-before-break safe if the break probability remains sufficiently small throughout the design life.

4. Example

In this section, the main results are given of a probabilistic leak-before-break analysis performed for a pipe elbow of the German fast breeder reactor. More details are contained in /3/. The data base available provided sufficient information about the material properties and the applied stresses but none about the distributions of the crack size a and the aspect ratio c/a. Therefore several distributions f(a), f(c/a) were taken from previous probabilistic investigations (see figs. 4-5) and a probabilistic leak-before-break analysis was performed for the different statistical models thus obtained. The results for the leak probability Q_L, the break probability Q_B, and the ratio Q_B/Q_L are compared in figs. 6-8. The break probability is virtually independent of the crack depth distribution, and the leak probability varies little with the c/a distribution. The absolute value of the leak probability at start-up depends strongly on the probability of finding very deep cracks, whereas the break probability is mainly influenced by the probability of finding very long shallow cracks. If the occurrence of these cracks can be excluded by performing non-destructive examinations before start-up (which is difficult because of the coarse grains of the austenitic steel used for the pipe elbow), the reliability of the component can be improved considerably. The ratio of the break and the leak probabilities Q_B/Q_L is very small when the component enters service but increases considerably due to cyclic crack growth. If it is possible to conduct a non-destructive in-service inspection with a reasonable probability of detection for long shallow cracks this ratio could be lowered considerably.

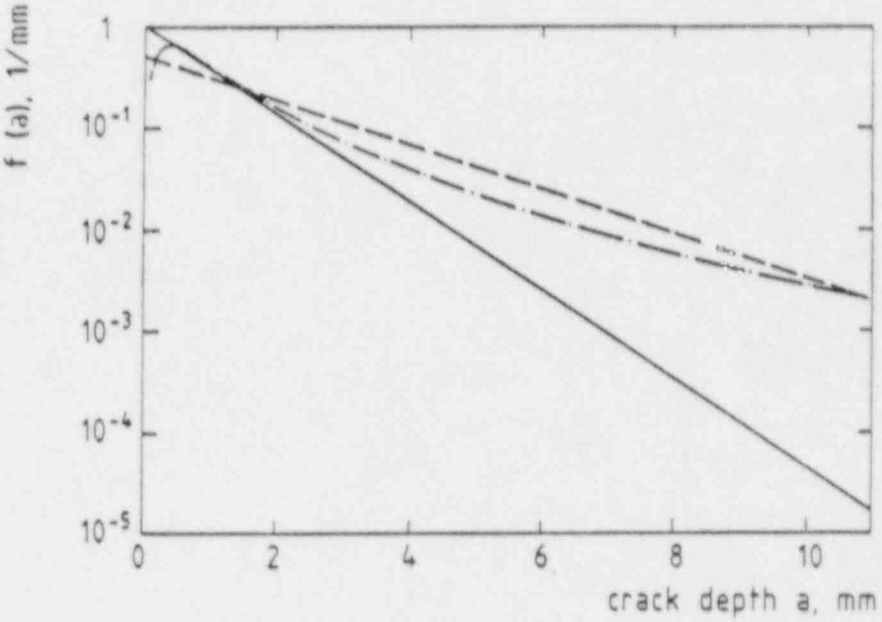

Figure 4: Distributions of the crack depth a /3/

—— —— exponential, λ = 1/mm

— — — exponential, λ = 0.5/mm

— —— lognormal, μ = 1 mm, σ = 1

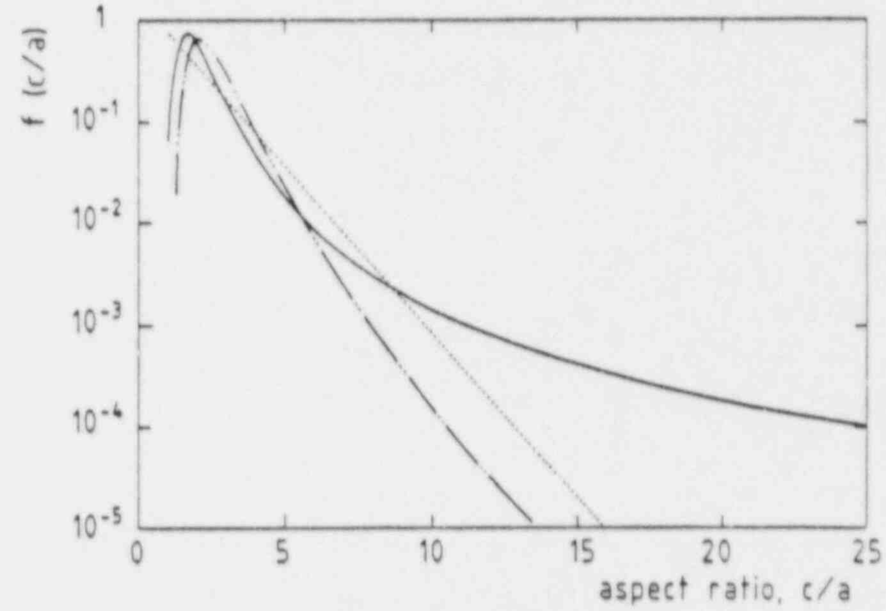

Figure 5: Distributions of the aspect ratio c/a

——— a/c normal, μ = 0.52, σ = 0.18 /3/

······ c/a exponential, λ = 0.75 /6/

— · — c/a lognormal, μ = 1.34, σ = 0.54 /2/

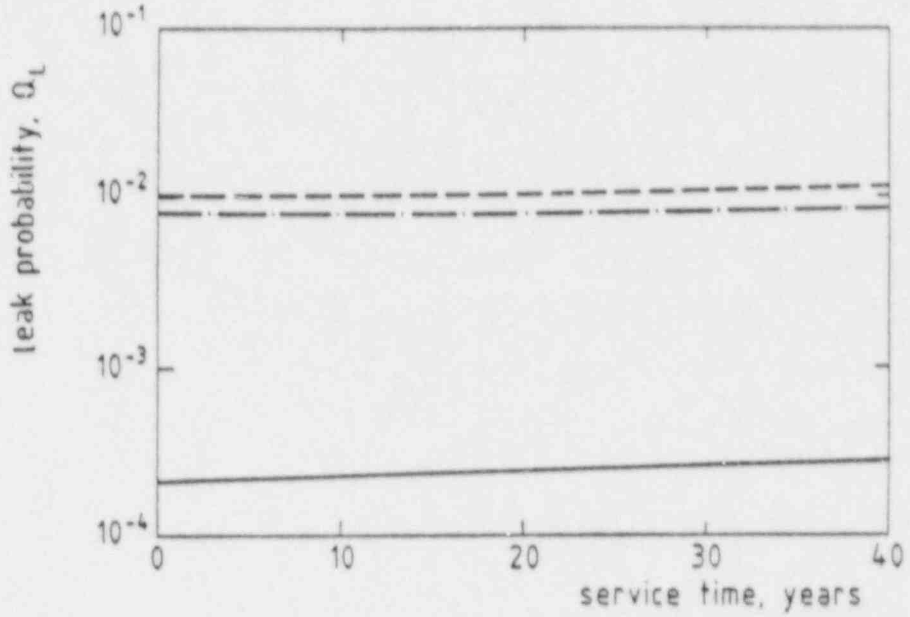

Figure 6: Dependence of the leak probability Q_L of the pipe elbow on f(a) (f(a/c) normal), notation as in fig. 4

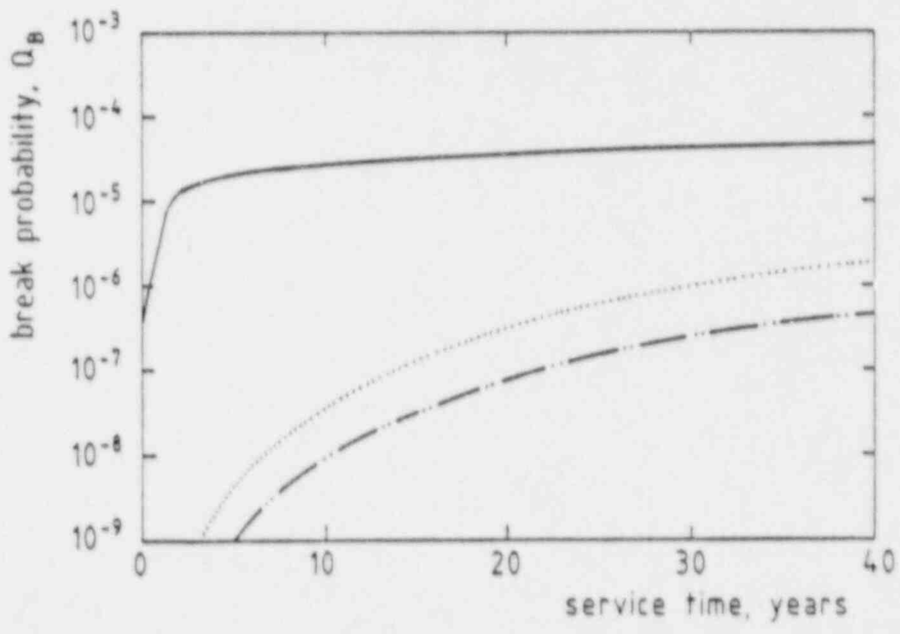

Figure 7: Dependence of the break probability Q_B of the pipe elbow on f(c/a) (f(a) exponential; λ = 1); notation as in fig. 5.

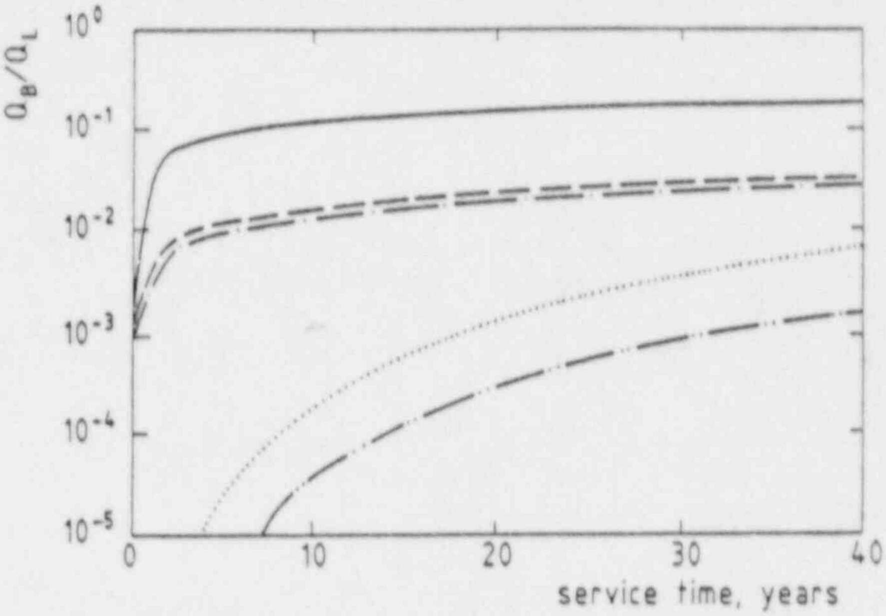

Figure 8: Dependence of the ratio Q_B/Q_L on the distributions of the crack depth a and the aspect ratio c/a, notation as in figs. 4 and 5.

5. Conclusion

A probabilistic leak-before-break analysis calls for a deterministic failure model. Here the propagation by fatigue of single flaws was considered and the corresponding fracture mechanics relations were applied. The assumption of single cracks extending without interaction with other cracks may be correct for welding cracks. The density of cracks initiated by stress corrosion or corrosion fatigue or by thermal fatigue can be very high. Then crack linking may occur and very long cracks may develop. Very long cracks have been observed austenitic stainless steel pipes /7/. A probabilistic description of crack initiation and crack linking is possible in principle, but it requires the developing crack probability density to be known and the deterministic description of crack linkage.

An important point, mostly neglected in probabilistic fracture mechanics, is the correlation of random variables. In a probabilistic analysis three categories of input data exist: the material parameters for unstable crack extension (K_{Ic}, σ_f) and for stable crack extension (C and m of the Paris relation), the crack size parameters (a and c) and loading parameters (internal pressure, thermal stresses). The variation of the material data is caused by inhomogeneties in the microstructures. Therefore it can be assumed that the various material properties are not independent random variables. As to the crack size distribution independence of the random crack depth a and the random crack aspect ratio a/c of semielliptical surface cracks or elliptical internal cracks has been assumed in most of the calculations performed which may be unrealistic. Because of the scarce experimental data available on two-dimensional crack size distributions the question of correlation between crack depth and crack length is still open. Another problem is the correlation between material data and crack size. Welding flaws are created because of an uncorrect welding procedure. It can be expected that the material properties near a welding flaw are different and may be worse than average.

Independent of the above mentioned uncertainties, the contribution of a probabilistic fracture mechanics analysis to a leak-before-break assessment has to be discussed. Due to the uncertainties in the probability distributions especially in the tail behaviour, it is difficult to attribute a clear meaning to the absolute values of the leak or the break probabilities. At the present status of knowledge it is recommended to apply different - but reasonable - input probability distributions and to calculate the leak and break probabilities. An assessment should be made not only on the basis of the absolute break probabilities but on the ratio P_{break}/P_{leak}. If this ratio is small enough for all input data variations and P_{break} stays below a reasonably fixed limit, any failure of the component considered is likely to be caused by a leak.

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A FINITE ELEMENT ANALYSIS FOR INELASTIC BEHAVIOR OF SURFACE CRACK

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1.Introduction

The behavior of surface cracks in the materials of high ductility has been extensively investigated in connection with the application of the Leak-Before-Break concept to the practical design of the nuclear piping system. In this respect, varying the initial crack size and the loading level, parametric results of the growing crack analysis are highly desired. For this kind of problem, some numerical treatments, including large-scale finite element analysis, are indispensable because of the complexity of the geometry and the nonlinearity of the material characteristics. From the economical viewpoint, however, the full finite element method based on the flow theory of plasticity is not suitable for such parametric estimation in the design field.

In this study, a simplified approach, based on the EPRI-GE estimation scheme [1], was employed for the parametric analysis of the surface crack behavior. The objects of the analysis here are plates with a semi-elliptical surface crack subjected to uniform tension or bending. The normalized values of J-integral, called the fully plastic solutions, were parametrically evaluated, varying the crack geometry and the power-hardening exponent "n".

2. Finite Element Analysis

The finite element analysis was performed by using the incompressible FEM program developed by the authors [2], where the material behavior is characterized by the deformation theory of plasticity and the following power-law hardening equation:

$$\bar{\varepsilon}/\varepsilon_0 = \kappa(\bar{\sigma}/\sigma_0)^n \qquad (1)$$

Here κ, ε_0, σ_0 and n are the material constants, while $\bar{\varepsilon}$ and $\bar{\sigma}$ are the Von Mises type equivalent strain and stress, respectively. The material incompressibility was treated by the Selective Reduced Integration / Penalty Function method.

Figures 1 and 2 illustrate the two problems for the present FEM analysis, a plate with a semi-elliptical surface crack subjected to uniform tension σ (Problem T) and that subjected to bending moment M (Problem B), respectively. The bending moment was applied by a couple of axial nodal forces on the plane section of the plate end. According to the beam theory, the axial nodal displacements of this plane section were constrained to vary linearly along the thickness so that it remains plane during deformation.

Varying the nondimensional crack depth a/t the aspect ratio of the ellipse, 13 plates were analyzed. All the cases of the FEM analysis are summarized in Table 1 with the values of a/t and a/c for Problems T and B. The ratios h/c and b/c were both taken to be four in all the cases. Due to the symmetry of the geometry and the loading, a quarter of the plate shown in Figs.1 and 2 was subdivided into the FEM mesh of more than 6500 degrees of freedom.

In the present analysis, the virtual crack extension method [3] was employed in two different manners for the evaluation of the local and global J-integral.

The local J-integral values along the crack front line were estimated by the partial extension δA as shown in Fig.3. The values thus obtained were employed to form a function of the eccentric angle of the ellipse, $J(\phi)$.

On the other hand, the global J-integral was obtained by the virtual extension of the whole crack front. Figure 4 shows the manners of the virtual crack extensions for the global J-integral value in the direction of the plate thickness, J_1, and that in the plate width direction, J_2.

3. Fully Plastic Solutions

Prior to the analysis of the fully plastic solutions, the elastic analysis was performed to confirm the validity of the present solution method. The stress intensity factor at the deepest point of the surface crack, namely $\phi = \pi/2$, was compared with the reference solution [4], in the case of $a/t = 0.6$ and $a/c = 0.4$ for Problem T. The J-integral was converted to the stress intensity factor on the assumption of the plane strain condition. The present solution was found two percent smaller than the reference one. Considering the present solution was calculated without the elastic singular elements, the accuracy of the present method is expected to be good in the fully plastic condition.

3.1 Tensile Load Problem

The local J-integral values for Problem T were normalized into the fully plastic solutions $f(\phi)$ as follows:

$$f(\phi) = J(\phi)/(\kappa\varepsilon_0\sigma_0 t(\sigma/\sigma_0)^{n+1}) \qquad (2)$$

Figures 5 through 13 show the distributions of the fully plastic solutions $f(\phi)$ along the crack front line for the nine crack configurations of Problem T. The distribution of the local J-integral for shallow cracks, i.e. $a/t = 0.2$, has the maximum value at the deepest point, i.e. $\phi = \pi/2$, except that of the semi-circular crack, i.e. $a/c = 1.0$ (see Figs.5 through 7). It is also seen that the J-integral takes the maximum value at some location between the deepest point, i.e. $\phi = \pi/2$, and the free surface, i.e. $\phi = 0$, in deeper cracks (see Figs.8 through 13).

With these results of the local J-distributions, the surface crack growth in the ductile plate can be approximately discussed, assuming that the material has the same value of J-resistance along the crack front line:

1) The J-distribution for a shallow crack has the maximum value at the deepest point. Therefore, a shallow crack tends to grow in the plate-thickness direction. This tendency becomes strong when the crack is semi-elliptical.
2) The J-distribution for a deep crack has the maximum value at some location between the deepest point and the free surface. Therefore, a large amount of crack growth could be occurred between the deepest point and the free surface. The crack shape might not remain semi-elliptical just before the penetration through the wall.

The global J-integral values J_1 and J_2 were similarly normalized as eq.(2) into the fully plastic solutions f_1 and f_2, respectively:

$$f_1 = J_1/(\kappa\varepsilon_0\sigma_0 t(\sigma/\sigma_0)^{n+1}) \qquad (3)$$

$$f_2 = J_2/(\kappa\varepsilon_0\sigma_0 t(\sigma/\sigma_0)^{n+1}) \qquad (4)$$

The values of f_1 and f_2 are shown in Tables 2 and 3, respectively.

For the growing crack analysis, the interpolation of the fully plastic solutions is necessary. First, the values of f_1 and f_2 for each of the nine crack configurations were interpolated as cubic polynomials of η by the least-square fitting:

$$f_i = q_1 + q_2 \cdot \eta + q_3 \cdot \eta^2 + q_4 \cdot \eta^3, \quad i = 1,2 \qquad (5)$$

Then, the coefficients q_i were interpolated on the crack configurations by means of the Lagrange polynomial of the Lagrange-type finite element:

$$q_i = p_1 + p_2 \cdot \xi + p_3 \cdot \eta + p_4 \cdot \xi^2 + p_5 \cdot \xi\eta + p_6 \cdot \eta^2$$

$$+ p_7 \cdot \xi^2\eta + p_8 \cdot \xi\eta^2 + p_9 \cdot \xi^2\eta^2, \quad i = 1,2,3,4 \qquad (6)$$

where ξ and η denote a/t and a/c, respectively. The coefficients p_j, $j = 1,2,\ldots,9$, are shown in Tables 4 and 5.

3.2 Bending Load Problem

The local J-integral values for Problem B were normalized into the fully plastic solutions $f(\phi)$ as follows:

$$f(\phi) = J(\phi)/(\kappa\varepsilon_0\sigma_0 t(m/m_0)^{n+1}) \tag{7}$$

$$\begin{cases} m = M/2b, \ M = 2Pt \\ m_0 = \sigma_0 t^2/2 \end{cases}$$

where m indicates the bending moment per unit plate-width. The bending moment M is given as the product of a couple of forces 2P and the plate thickness t. The reference bending moment per unit plate-width m_0 is defined so that the value of $f(\phi)$ monotonically increases as a function of n.

Figures 14 and 15 show the distributions of $f(\phi)$ along the crack front line for shallow cracks, namely $a/t = 0.2$. The local J-distribution for the semi-elliptical, shallow crack (see Fig.14) has the maximum value at the deepest point while that for the semi-circular, shallow crack (see Fig.15) has the maximum at a location between the deepest point and the free surface, which becomes remarkable with the increase of n. These results of the local J-distributions for shallow cracks present close similarities to that for shallow cracks under tensile loading in section 3.1. This implies that the cracks are within the tensile region as for the analyses of Figs.14 and 15.

On the other hand, as for the analyses of deep cracks, namely $a/t = 0.8$, with a large number of n, the crack opening displacements were negative in the region around the deepest point, where compression is dominant. The distributions of $f(\phi)$ along the crack front line for the deep cracks are shown in Figs.16 and 17, where positive peaks in the compression-dominant region are omitted because they have no meanings for the crack growth. Furthermore, the contact of the crack surface should be analyzed for more strict results. In these figures, the local J-distribution has the maximum value at the location near the free surface, which becomes higher with the increase of n. Assuming the same fracture resistance along the crack front line, it could be predicted that the crack grows mainly in the plate-width direction.

The global J-integral values J_1 and J_2 were normalized into the fully plastic solutions f_1 and f_2, respectively, in the same way as eq(7):

$$f_1 = J_1/(\kappa\varepsilon_0\sigma_0 t(m/m_0)^{n+1}) \tag{8}$$

$$f_2 = J_2/(\kappa\varepsilon_0\sigma_0 t(m/m_0)^{n+1}) \tag{9}$$

The values of f_1 and f_2 are shown in Tables 6 and 7, respectively. In Table 6, the value of f_1 is assumed to be zero for several cases where negative crack opening displacements were found in the compression-dominant region.

Similarly to Problem T, the values of f_1 and f_2 were interpolated as functions of n, a/t and a/c:

$$f_i = q_1 + q_2 \cdot \eta + q_3 \cdot \eta^2 + q_4 \cdot \eta^3, \quad i = 1,2 \qquad (10)$$

where

$$q_i = p_1 + p_2 \cdot \xi + p_3 \cdot \eta + p_4 \cdot \xi\eta, \quad i = 1,2,3,4 \qquad (11)$$

Here ξ and η denote a/t and a/c, respectively. The coefficients p_j, $j = 1,2,\ldots,9$, are shown in Tables 8 and 9.

4. Parametric Analysis of Surface Crack Growth

Several cases of the surface crack growth were analyzed, employing the local J-integral J_1 and J_2 for Problem T as the crack driving forces in the direction of the plate thickness and width, respectively. The material employed here is type SA-106 steel, whose material constants in eq.(1) were given as follows: $\varepsilon_0 = 0.00148$, $\sigma_0 = 304$ MPa, $\kappa = 9.8$ and $n = 5$. The plate thickness "t" was taken to be 10 mm. The purpose of this analysis is to investigate the general trend of the surface crack growth in a plate. The following assumptions were made to simplify the analysis.

a. The crack grows keeping the semi-elliptical shape.

b. No unloading occurs during the crack growth.

c. There is no geometrical effect in the J-integral resistance. The same resistance curves are used for both J_1 and J_2.

Here the J-integral resistance curve J_r was obtained from test data and linearly approximated as follows:

$$J_r = J_{Ic} + dJ/da \times \Delta a \tag{12}$$

The values of J_{Ic} and dJ/da were taken to be 1 MN/m and 200 MPa, respectively. The crack extension Δa was numerically given as the increment of either the crack depth "a" or the crack half-width "c".

The results are shown in Figs.18 and 19, where six cases of the initial crack geometry are analyzed. In these figures, the straight lines mean increase of "a" under constant "c" and "t", implying that no crack extension occurs in the direction of the plate width. It is noted that, in the cases of the shallower initial cracks in Fig.18, i.e. the initial value of $a/t = 0.2$, the three cracks with different initial aspect ratios a/c have almost the same aspect ratio a/c near the penetration through the thickness, while the deeper initial cracks in Fig.19, i.e. the initial value of $a/t = 0.5$, grow mainly in the thickness direction and have different aspect ratios near the penetration.

5 Conclusions

The fully plastic solutions of the semi-elliptical cracks in a plate subjected to uniform tension or bending were obtained for several cases of the crack geometry. The local J-distributions were analyzed in all the cases. The solutions for the global J-integral were successfully interpolated by means of the Lagrange polynomial and the least-square fitting. Simplified analysis of the surface crack growth was performed by using the polynomial expression of the fully plastic solutions. As a result of the numerical experiment, the shallow initial cracks with different initial aspect ratios have almost the same aspect ratio near the penetration through the thickness.

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Table 1 Geometrical parameters of FEM analysis.

Problem T		Problem B	
a/t	a/c	a/t	a/c
0.2	0.2	0.2	0.2
0.2	0.4	0.2	1.0
0.2	1.0	0.8	0.2
0.6	0.2	0.8	1.0
0.6	0.4		
0.6	1.0		
0.8	0.2		
0.8	0.4		
0.8	1.0		

Table 2 Fully plastic solutions f_1 for the global J-integral in the thickness direction (Problem T).

a/t	a/c	n = 1	n = 2	n = 3	n = 5	n = 7	n = 10
0.2	0.2	0.437	0.532	0.573	0.607	0.629	0.669
0.2	0.4	0.394	0.490	0.542	0.608	0.662	0.748
0.2	1.0	0.203	0.255	0.283	0.322	0.355	0.409
0.6	0.2	2.79	4.18	5.28	7.13	8.82	11.4
0.6	0.4	1.86	3.01	3.97	5.59	7.06	9.24
0.6	1.0	0.657	1.05	1.38	1.92	2.40	3.09
0.8	0.2	4.40	6.99	8.71	11.0	12.6	14.9
0.8	0.4	3.05	4.18	5.16	7.17	9.45	13.6
0.8	1.0	1.20	1.76	2.31	3.44	4.75	7.15

Table 3 Fully plastic solutions f_2 for the global J-integral in the width direction (Problem T).

a/t	a/c	n = 1	n = 2	n = 3	n = 5	n = 7	n = 10
0.2	0.2	0.282	0.340	0.362	0.376	0.383	0.400
0.2	0.4	0.219	0.271	0.297	0.327	0.349	0.385
0.2	1.0	0.203	0.255	0.283	0.322	0.355	0.409
0.6	0.2	1.96	2.64	3.10	3.88	4.65	5.86
0.6	0.4	1.11	1.66	2.11	2.85	3.56	4.63
0.6	1.0	0.657	1.05	1.38	1.92	2.40	3.09
0.8	0.2	4.25	5.63	6.36	7.50	8.62	10.5
0.8	0.4	1.53	1.89	2.18	2.84	3.66	5.22
0.8	1.0	1.20	1.76	2.31	3.44	4.75	7.15

Table 4 Coefficients p_i for the fully plastic solutions f_1 (Problem T).

	p_1	p_2	p_3	p_4	p_5	p_6	p_7	p_8	p_9
q_1	1.142	-7.318	-2.220	15.46	16.45	1.566	-38.27	-11.47	25.97
q_2	1.959	-13.12	-10.13	19.18	67.58	7.937	-87.39	-52.76	67.36
q_3	-0.3085	2.004	1.333	-2.779	-8.804	-0.9852	11.50	6.502	-8.428
q_4	0.01482	-0.09622	-0.06099	0.1355	0.4025	0.04592	-0.5328	-0.3026	0.3962

Table 5 Coefficients p_i for the fully plastic solutions f_2 (Problem T).

	p_1	p_2	p_3	p_4	p_5	p_6	p_7	p_8	p_9
q_1	-3.420	23.71	17.13	-23.87	-111.1	-13.23	122.5	85.08	-95.52
q_2	3.126	-22.07	-16.54	33.28	111.6	13.08	-144.0	-87.82	109.9
q_3	-0.6538	4.308	2.716	-5.824	-18.09	-2.023	23.16	13.48	-17.04
q_4	0.02655	-0.1741	-0.1034	0.2394	0.6884	0.07660	-0.8904	-0.5105	0.6499

Table 6 Fully plastic solutions f_1 for the global J-integral in the thickness direction (Problem B).

a/t	a/c	n = 1	n = 2	n = 3	n = 4	n = 5
0.2	0.2	4.32	8.41	16.5	31.8	60.5
0.2	1.0	1.11	2.46	5.07	10.3	20.8
0.8	0.2	1.66	0.483	2.80	0	0
0.8	1.0	0.143	0	0	0	0

Table 7 Fully plastic solutions f_2 for the global J-integral in the width direction (Problem B).

a/t	a/c	n = 1	n = 2	n = 3	n = 4	n = 5
0.2	0.2	1.24	2.44	4.87	9.55	18.4
0.2	1.0	1.90	3.42	6.65	13.0	25.4
0.8	0.2	12.0	21.8	39.9	76.2	129
0.8	1.0	9.76	17.0	30.9	57.0	101

Table 8 Coefficients p_i for the fully plastic solutions f_1 (Problem B).

	p_1	p_2	p_3	p_4
q_1	-2.495	5.663	0.5127	-2.613
q_2	12.11	-15.88	-7.110	9.089
q_3	-4.651	6.279	2.591	-3.545
q_4	1.217	-1.608	-0.7676	1.031

Table 9 Coefficients p_i for the fully plastic solutions f_2 (Problem B).

	p_1	p_2	p_3	p_4
q_1	-4.431	19.88	2.553	-13.14
q_2	3.336	-6.066	-1.168	12.88
q_3	-1.824	5.021	0.6223	-6.403
q_4	0.08112	0.6735	0.08892	0.2848

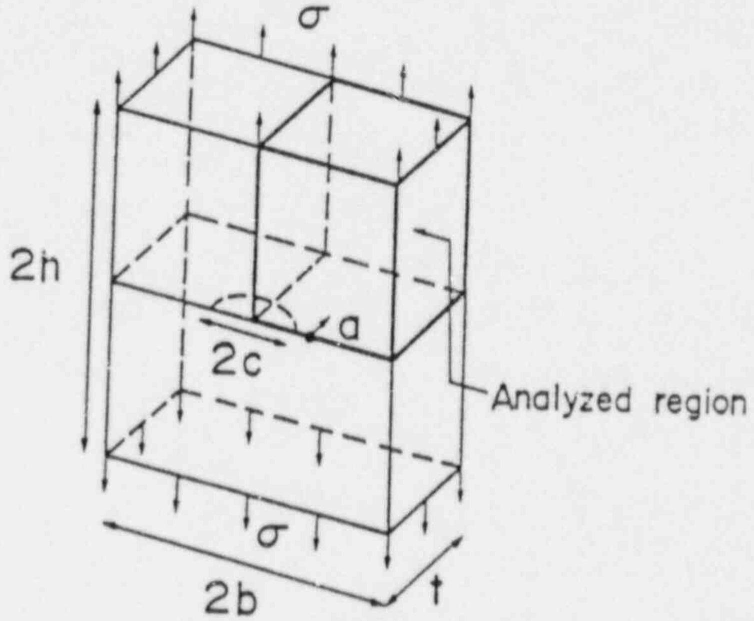

Fig.1 Plate with a semi-elliptical surface crack subjected to uniform tension.

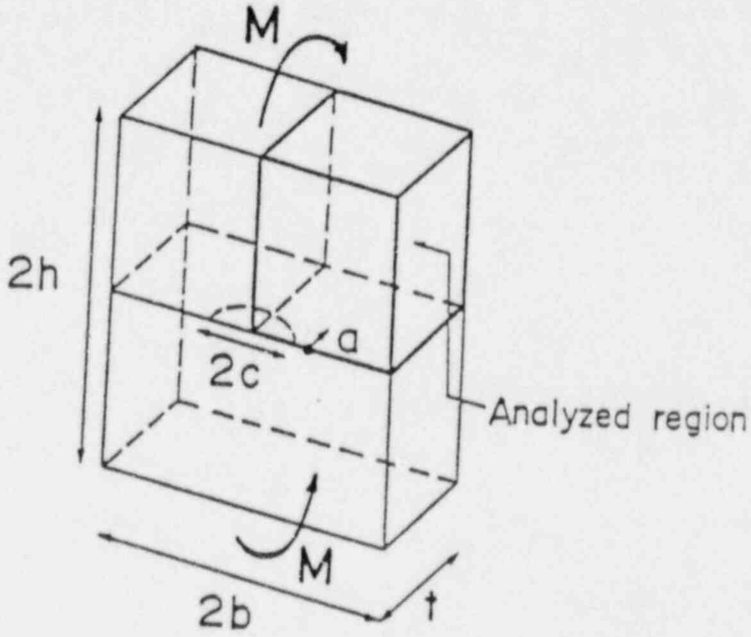

Fig.2 Plate with a semi-elliptical surface crack subjected to bending.

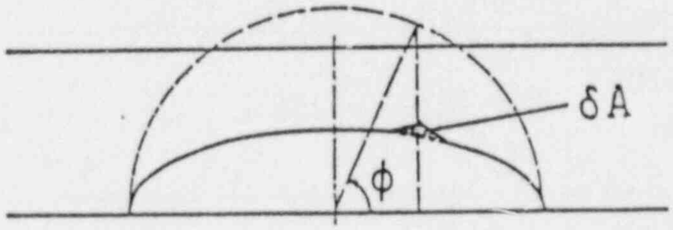

Fig.3 Virtual crack extension for the local J-integral.

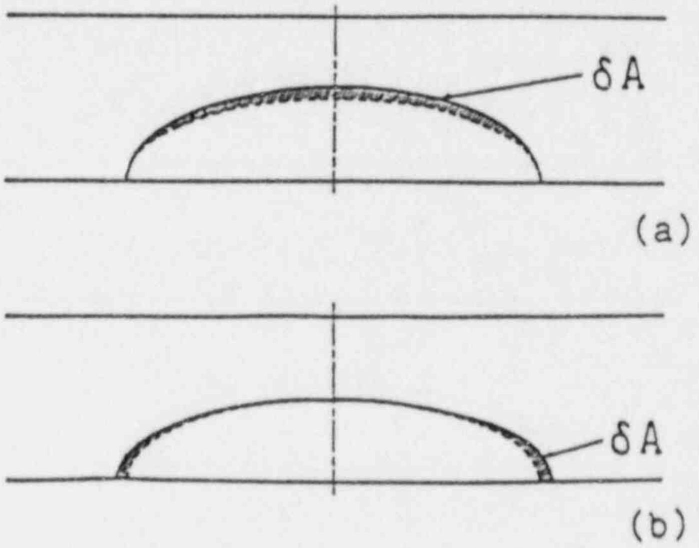

Fig.4 Virtual crack extensions for the global J-integral in the thickness direction (a) and in the width direction (b).

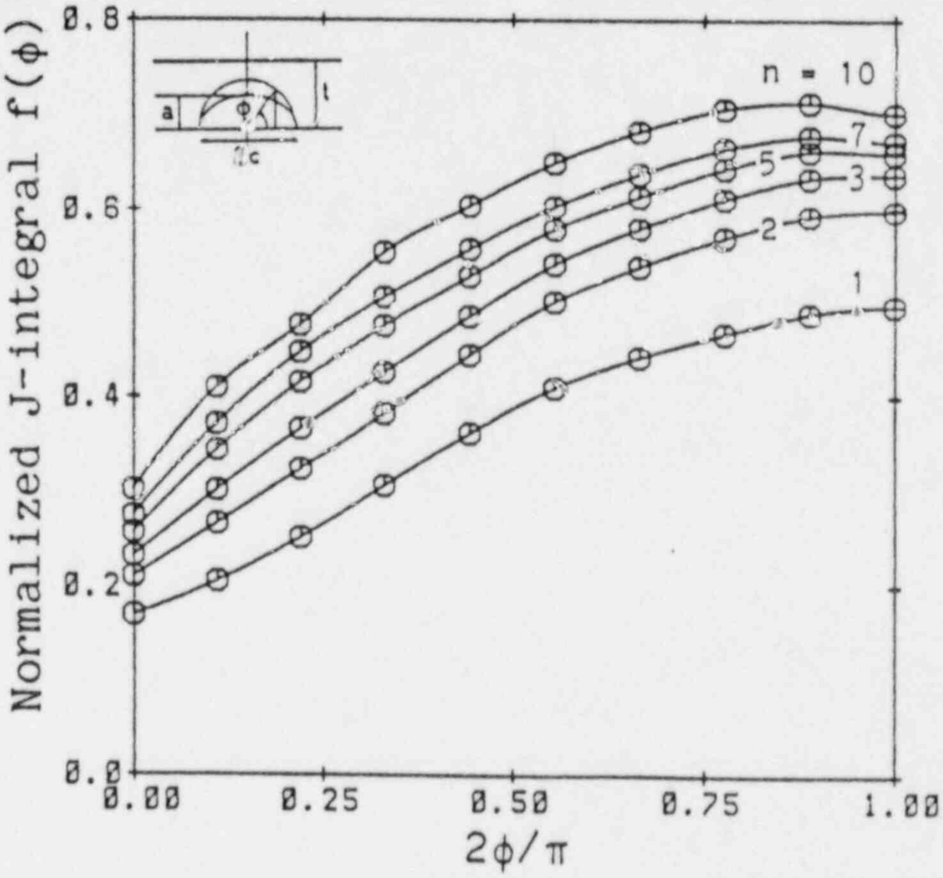

Fig.5 Distribution of the local J-integral
(Problem T, a/t = 0.2, a/c = 0.2).

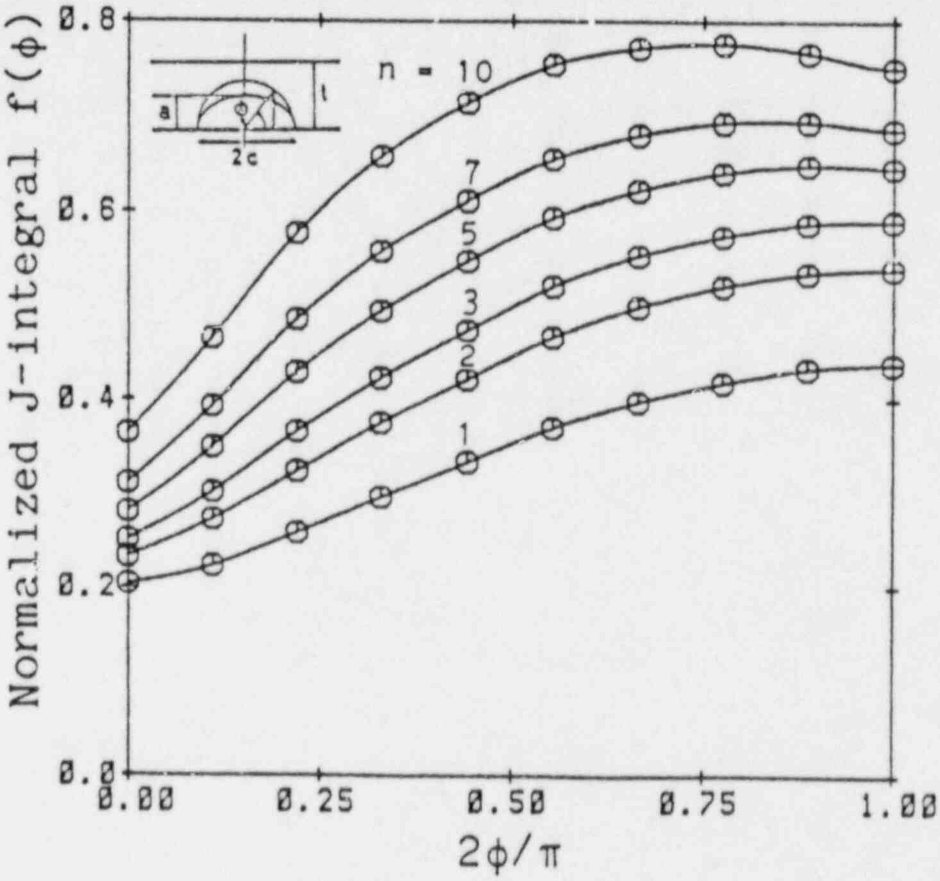

Fig.6 Distribution of the local J-integral (Problem T, $a/t = 0.2$, $a/c = 0.4$).

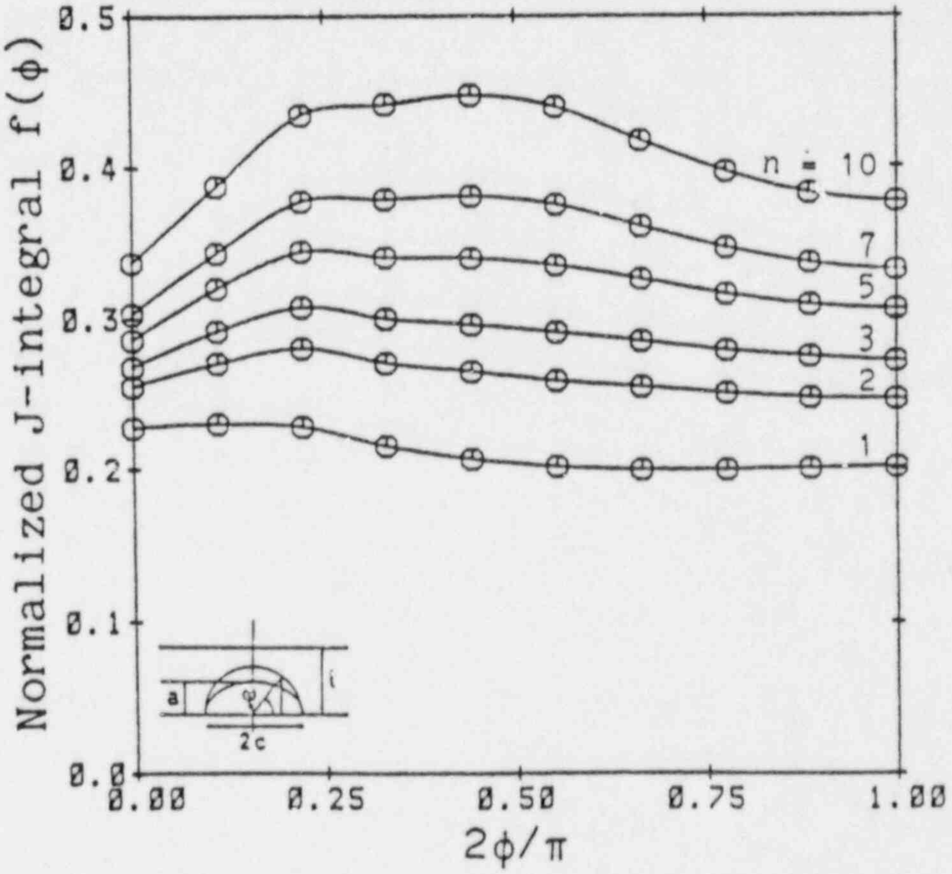

Fig.7 Distribution of the local J-integral
(Problem T, a/t = 0.2, a/c = 1.0).

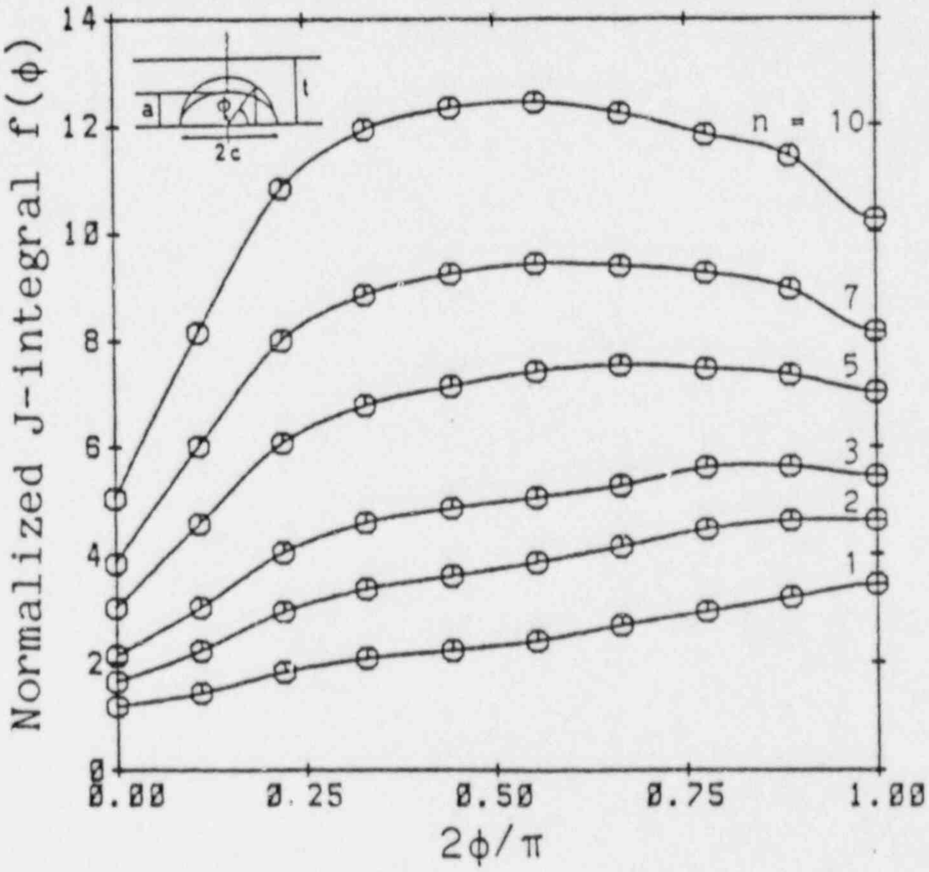

Fig.8 Distribution of the local J-integral (Problem T, a/t = 0.6, a/c = 0.2).

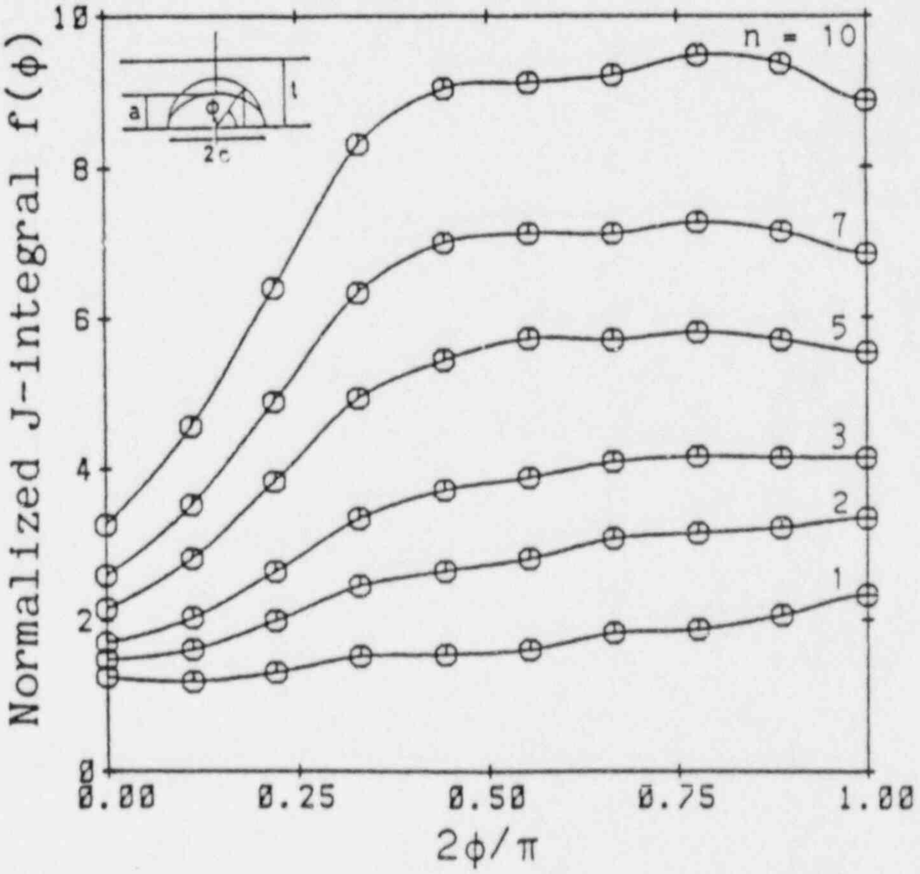

Fig.9 Distribution of the local J-integral (Problem T, a/t = 0.6, a/c = 0.4).

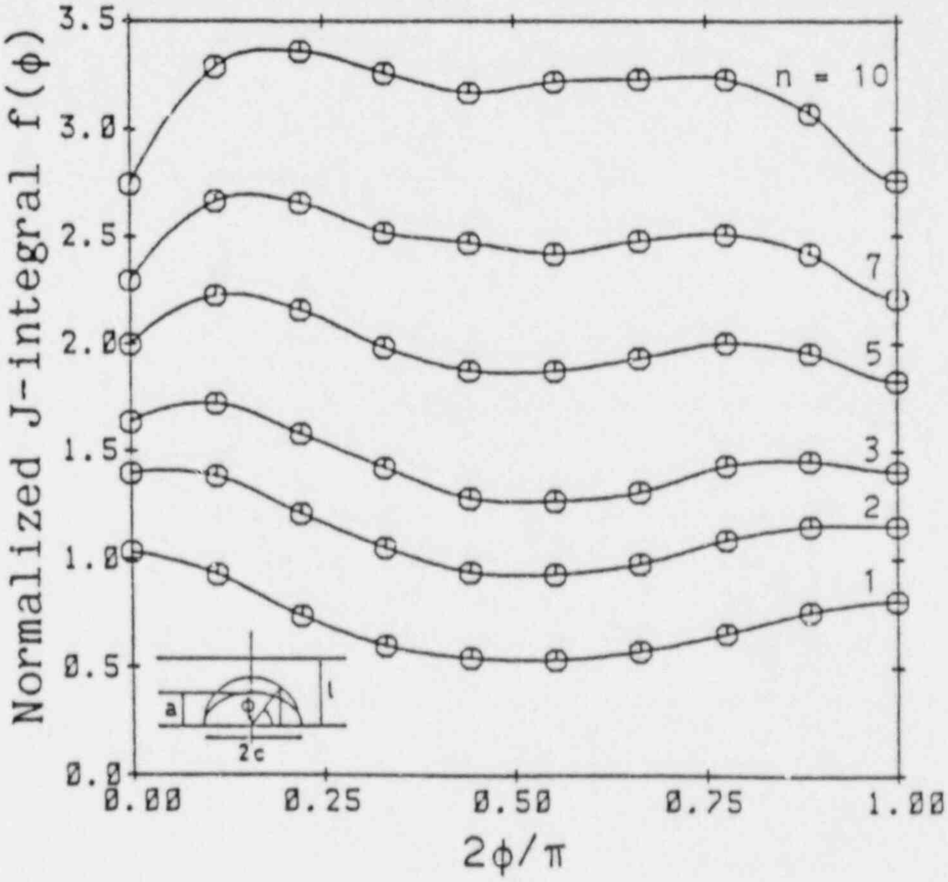

Fig.10 Distribution of the local J-integral (Problem T, $a/t = 0.6$, $a/c = 1.0$).

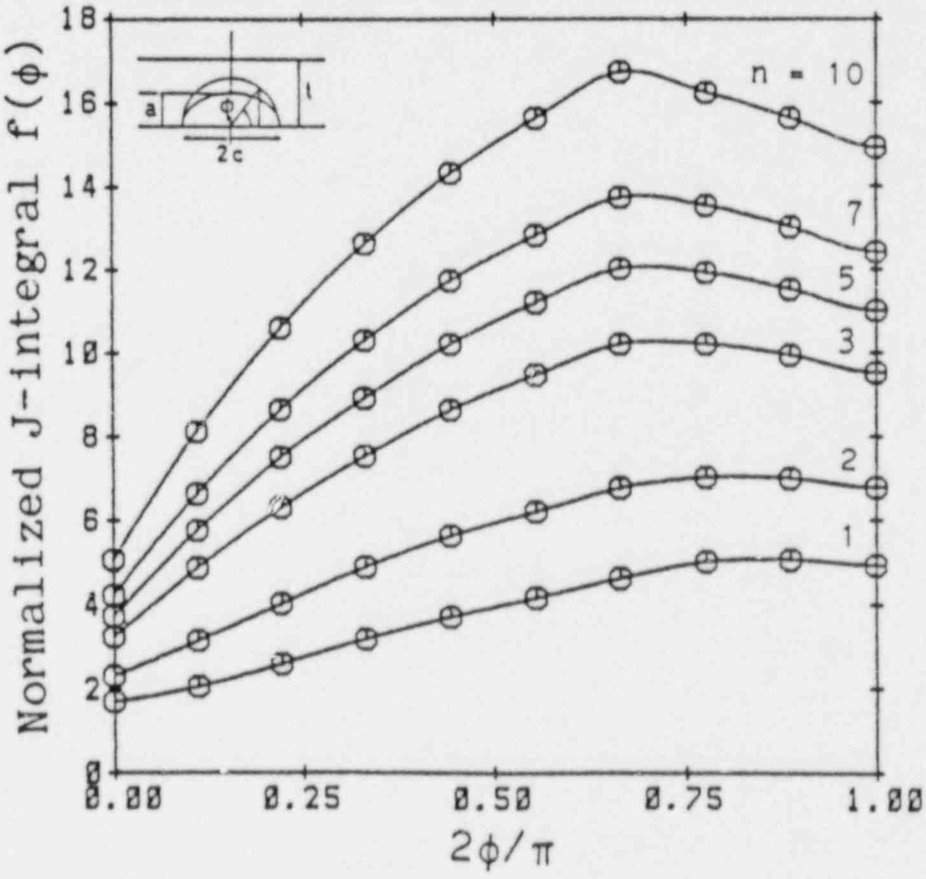

Fig.11 Distribution of the local J-integral
(Problem T, a/t = 0.8, a/c = 0.2).

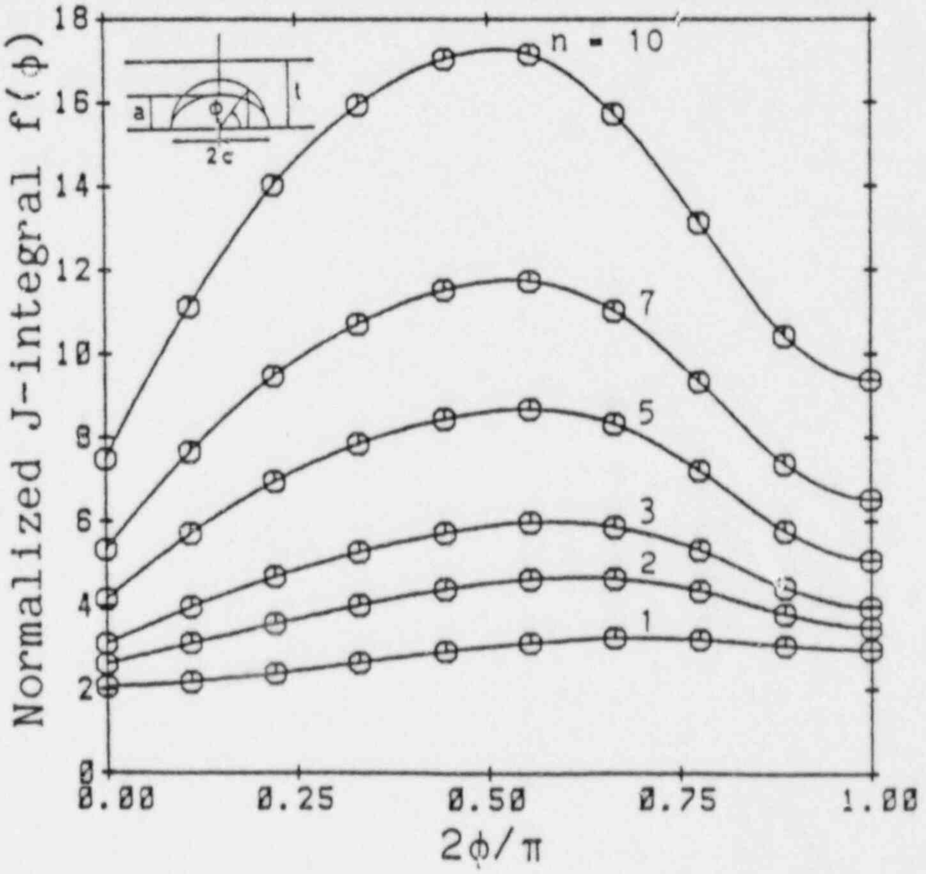

Fig.12 Distribution of the local J-integral (Problem T, a/t = 0.8, a/c = 0.4).

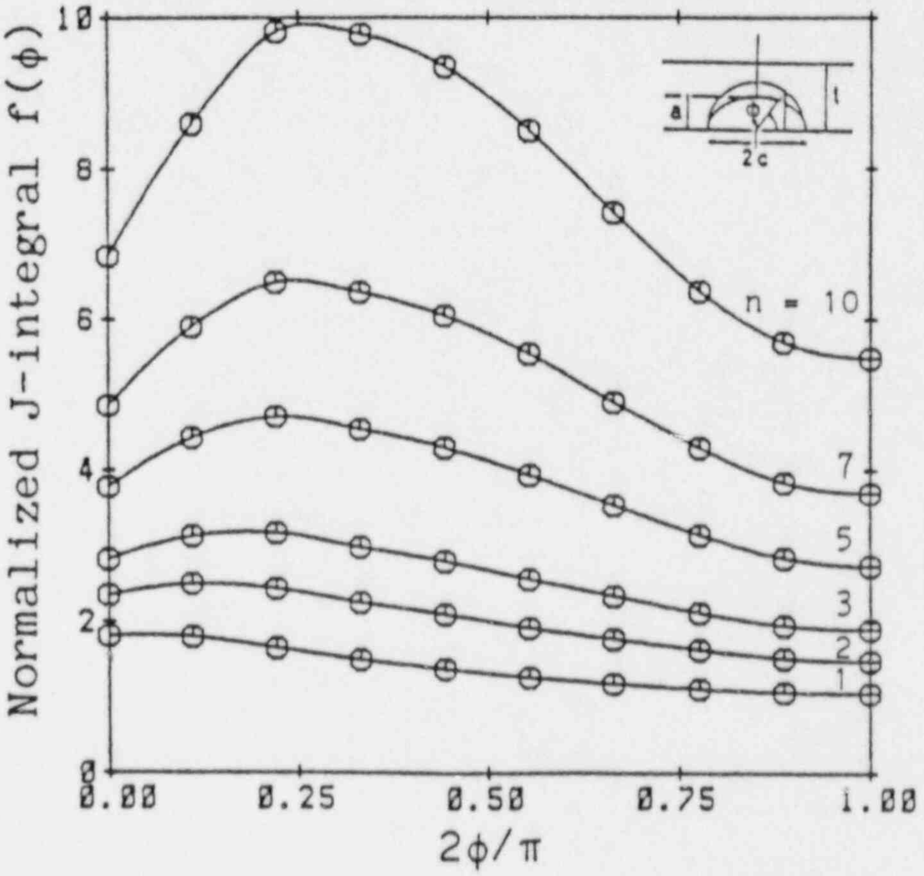

Fig.13 Distribution of the local J-integral
(Problem T, a/t = 0.8, a/c = 1.0).

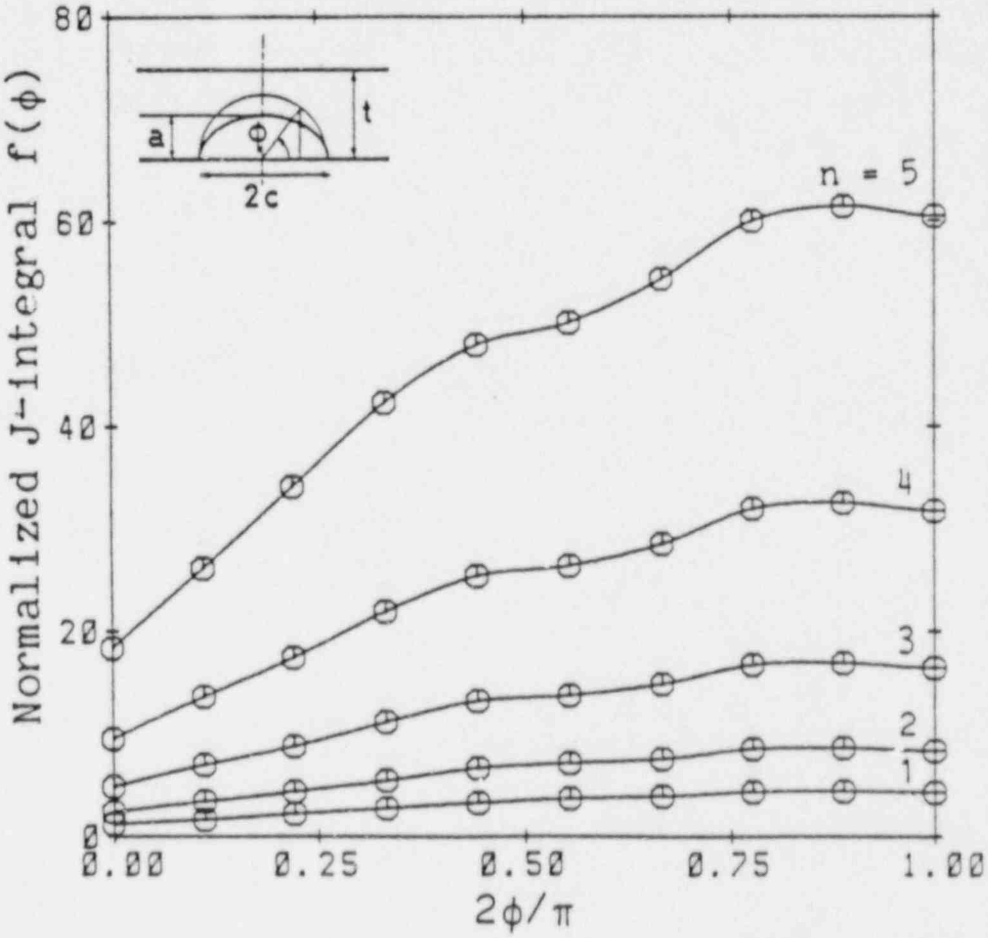

Fig.14 Distribution of the local J-integral (Problem B, a/t = 0.2, a/c = 0.2).

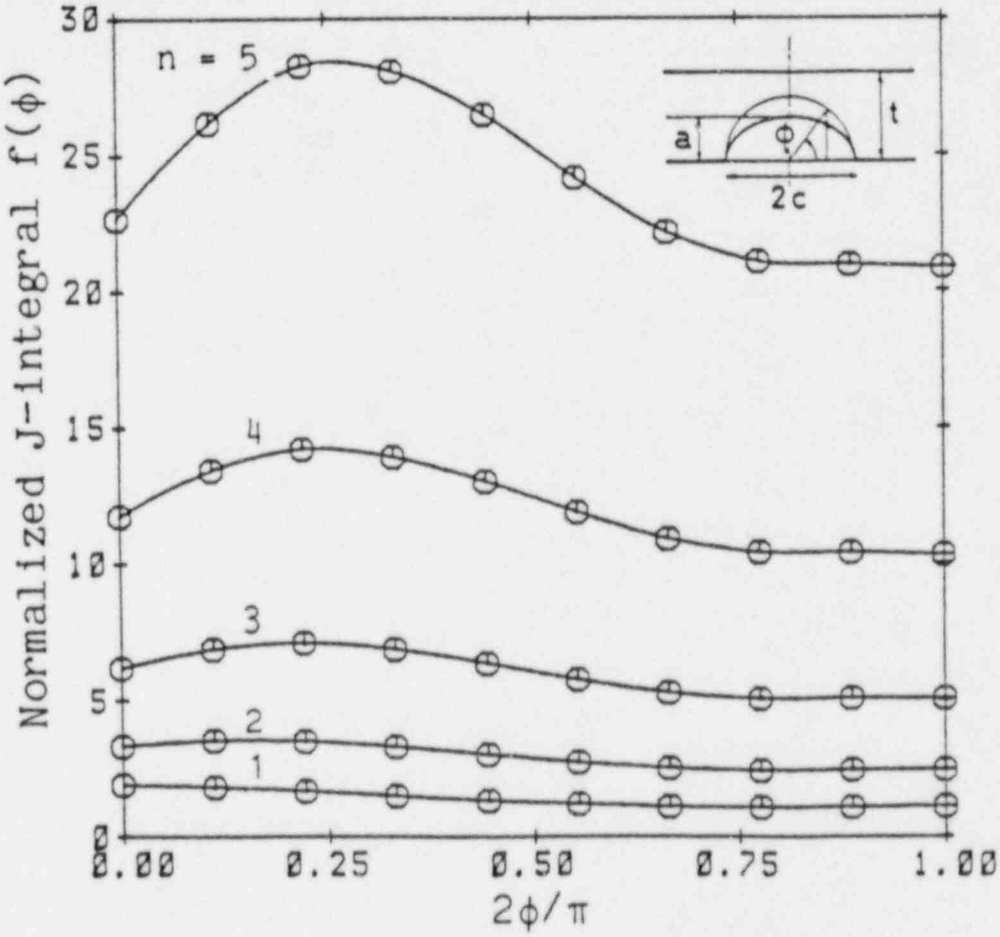

Fig.15 Distribution of the local J-integral
(Problem B, a/t = 0.2, a/c = 1.0).

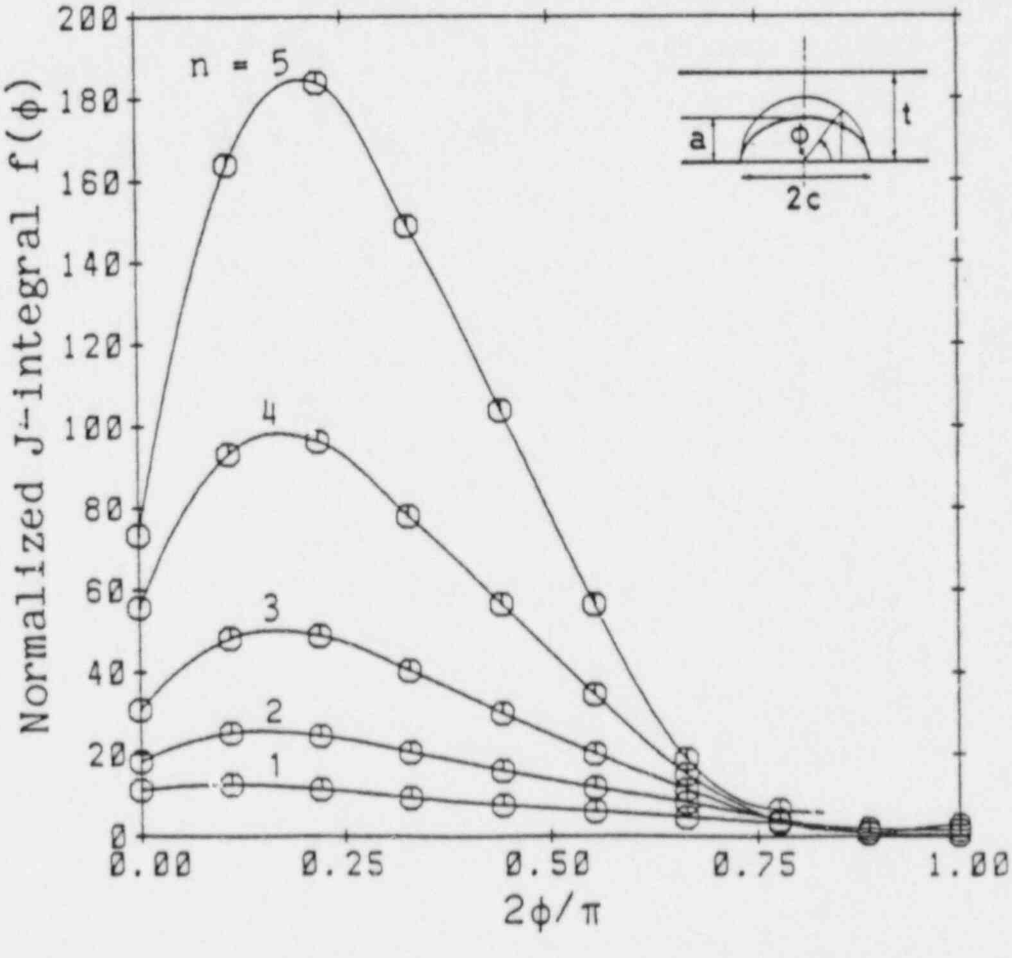

Fig.16 Distribution of the local J-integral (Problem B, a/t = 0.8, a/c = 0.2).

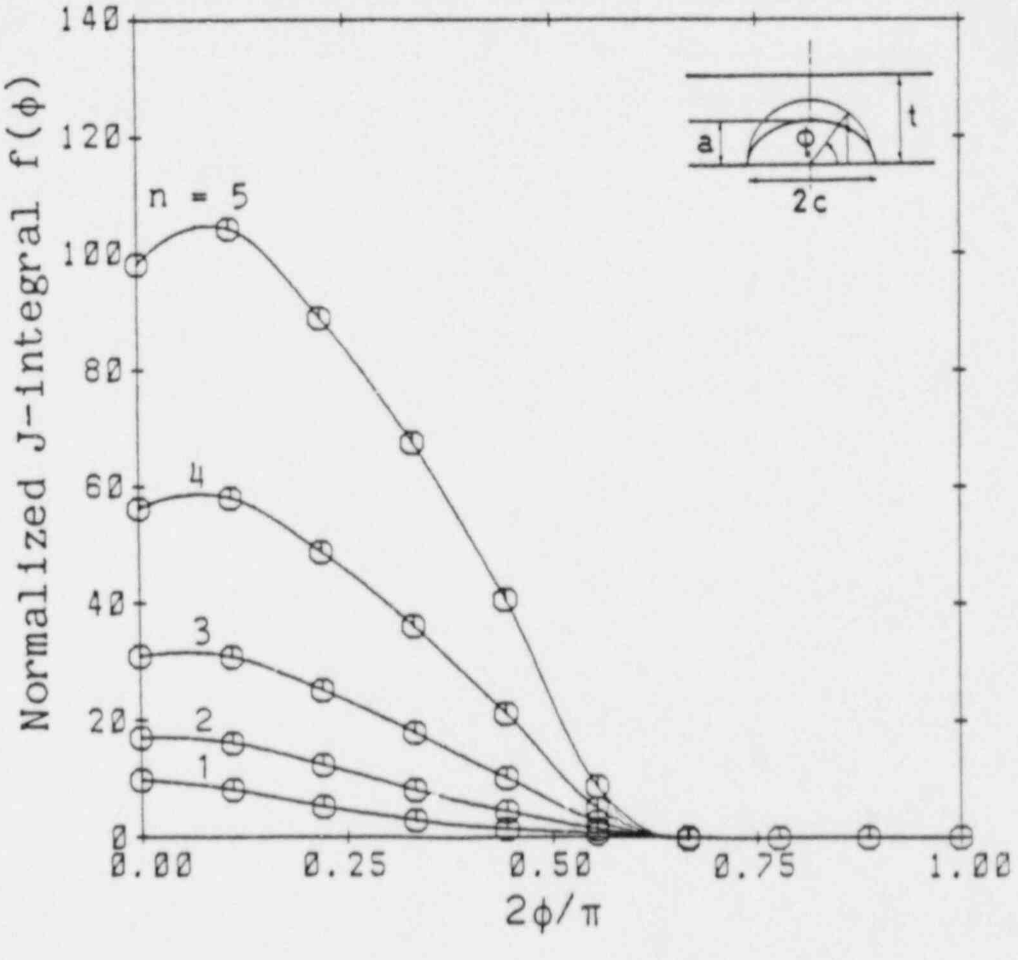

Fig.17 Distribution of the local J-integral (Problem B, a/t = 0.8, a/c = 1.0).

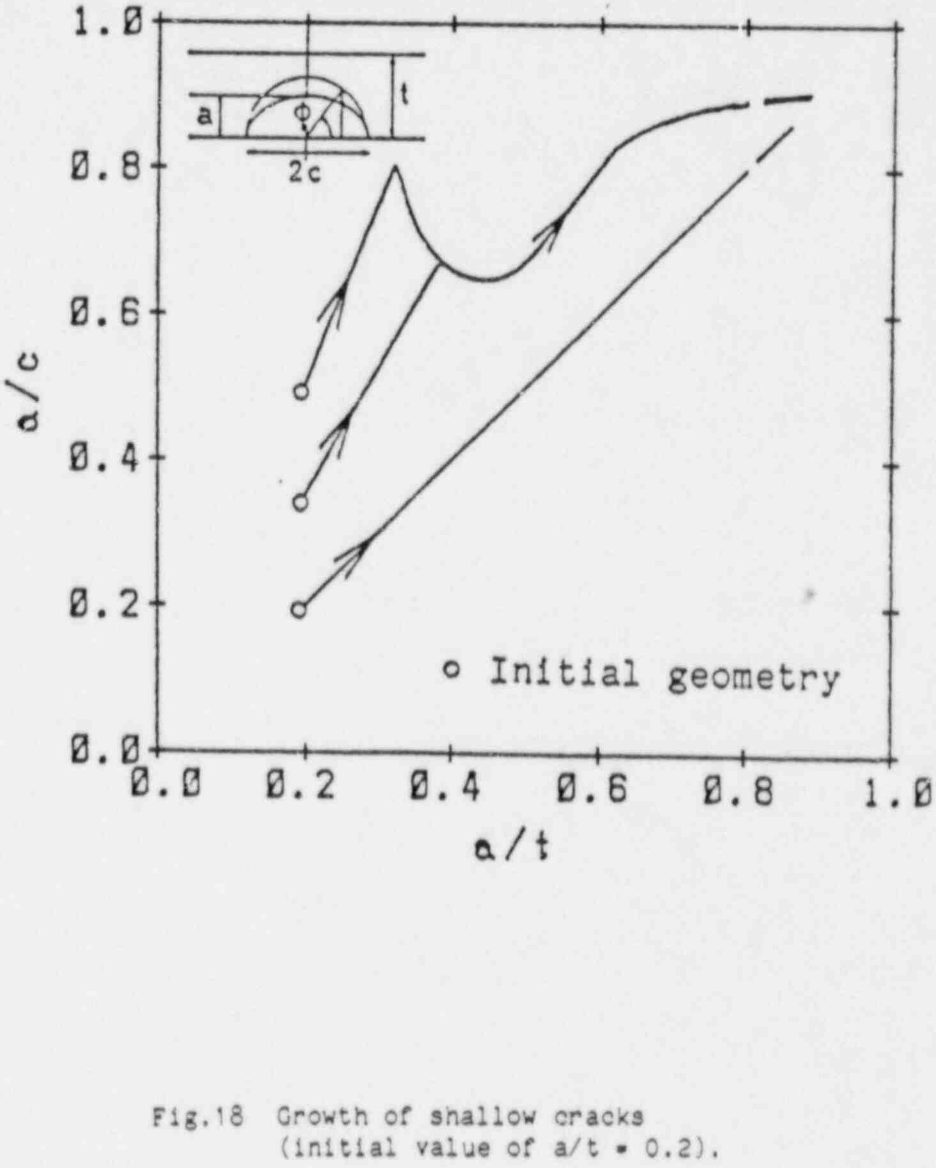

Fig.18 Growth of shallow cracks
(initial value of a/t = 0.2).

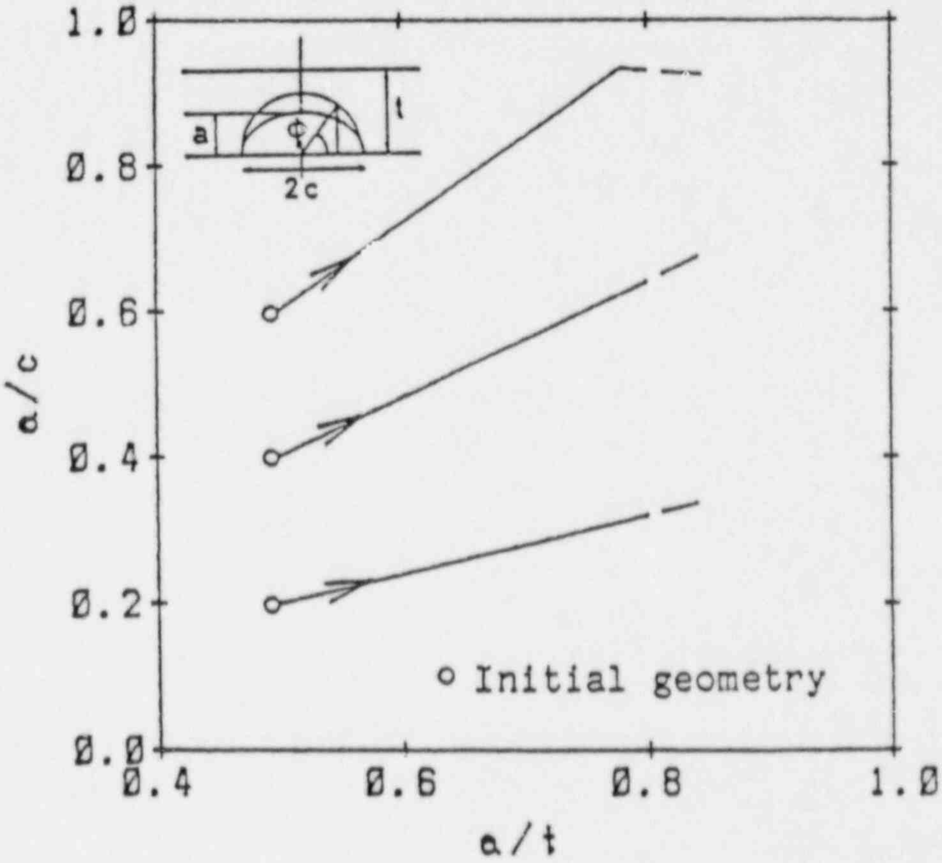

Fig.19 Growth of deep cracks
(initial value of a/t = 0.5).

Round-Robin Study on Ductile Growth of Part-Through Crack in Carbon Steel Plate --Intermediate Report--

Yukio Takahashi, Koichi Kashima and Kazuo Kuwabara
(Central Research Institute of Electric Power Industry)

1. Introduction

Rationalization of the design of nuclear piping system is in progress in many countries. For this purpose, extensive researches are under way for developing the techniques to evaluate the fracture characteristics of piping made of high-ductility materials at a number of institutions. Applicability of the J-integral for ductile fracture is studied in many projects. Several methods were proposed for the evaluation of the J-integral so far but they require the assessment of their accuracy by less simplified methods. We should also rely upon the detailed calculation methods for the evaluation of realistic surface flaws, for which no simplified J-integral evaluation method is available at present.

Detailed calculation for the fracture analysis is usually made by the finite element method. Many elastic-plastic analyses were conducted for the ductile fracture problems using nonlinear finite element codes usually using incremental approach. It is a well known fact that analytical results are influenced by a variety of factors in numerical calculation. Therefore, it is necessary to understand the influence of various factors on the solution and to conduct the analysis with well validated technique and modeling for the practical application.

Several round-robin activities were held on the analysis of ductile fracture in several groups for this purpose(1-4). Two-dimensional problems were the subjects of the analyses in the early studies, where reasonable agreement was obtained within the finite element solutions and also with the predictions by simplified methods in some cases. Recent activities dealt with the pipe fracture problem proposed by Battelle Columbus Laboratories(4,5), and showed fairly good agreement between the finite element solutions for the pipe with a circumferential through-wall crack.

At this time, we need to proceed to more realistic problem for leak-before-break assessment; surface crack problem. Based on this idea, we planned to propose a round-robin study and to make an experiment for it. Following sections describe the principal results obtained so far.

2. Description of experiment

Geometry of specimens subjected to the analysis is shown in Figure 1. They were made of Japanese carbon steel designated STS 42 in Japan Industrial Standards (JIS). This steel is similar to ASTM A106 steel regarding the chemical composition and mechanical properties. Flat specimens were taken from a pipe with inner diameter of 406.4 mm (16 inch) and wall thickness of 36.5 mm (1.44 inch) of this material, making specimen loading direction parallel to the pipe axis. Uniaxial tensile test specimens and 1T CT specimens were also taken from the same pipe to obtain fundamental properties of the material.

A semi-elliptical crack of 40 mm (1.57 inch) width and 5 mm (0.2 inch) depth was introduced from the one side of the specimen surfaces using the electron discharge machining method. Jigs with holes were welded to both ends of the plate for pin loading.

Elongation was measured for the full length of the specimens as well as near the crack. Near-crack displacements were measured on both crack-side and back surfaces of the plates with the gauge length of 24 mm. These are referred to as the global displacement (d), near-crack displacement on crack-side surface (c_1), and near-crack displacement on back surface (c_2), respectively, thereafter.

The test was conducted on four specimens having initial cracks of the same size. Each specimen was loaded quasi-statically until the different amount of tensile displacements at room temperature and then unloaded. After that, they were subjected to tensile force in an atmosphere of liquid nitrogen, which causes brittle fracture of the specimens. By the observation of the specimen surfaces by naked eyes, the portion of ductile crack growth was easily distinguished from that of brittle fracture. In this way, the relation between the ductile crack growth and the displacement values was established.

Figures 2 to 4 show the relationship between the three displacement quantities and applied load for the total four specimens. The relation before unloading was nearly equal for all specimens. This indicated that the difference in initial crack shape or loading condition of the four specimens was small. Thus it can be considered that each specimen represents the different stage of the ductile growth of one crack.

Figure 5 is a photograph of the fracture surfaces after brittle fracture, showing clear distinction between surfaces by ductile fracture at room temperature and brittle one at liquid nitrogen temperature. It can be also seen from this photograph that the cracks propagated little in the direction across the width of the plates but increased only in depth direction until they penetrated the wall thickness. These results established the relation between the crack growth amount at the deepest point of the crack and the near-crack displacement on the crack-side surface as shown in Figure 6.

3. Problem Description

A portion of the specimens 100 mm w [illegible] d 400 mm long was analyzed. It was loaded with uniform forced displacement in tensile direction, which was regarded as the global displacement in the comparison of the results.

Young's modulus was 200,000 MPa and Poisson's ratio 0.3. Figures 7 and 8 show the engineering and the true stress-strain curves obtained from the uniaxial tensile test of the present material. Modeling of stress-strain relation from these data was left to each participant.

It was suggested that the analysis should include the simulation of crack propagation as much as possible. Three options were given for criterion of crack propagation, one commonly called generation phase simulation and the others application phase simulation.

The relation of the near-crack displacement (c_1) versus the crack propagation amount at the deepest point of the crack shown in Figure 6 was used as a criterion for the generation phase simulation, with the width of the crack unchanged within this range. For the interpolation, we recommended to use the following linear relationship shown in Figure 6.

$$c_1 = 2.92 \text{ mm } (0.115 \text{ inch}) + 0.585\Delta a \tag{1}$$

For the application phase simulation, it was adviced that the J-integral resistance curve obtained from the test on CT specimens from the same materal should be used as the crack propagation criterion. The J-integral resistance curve shown in Figure 9 can be approximated by the following formula.

$$J \text{ (MN/m)} = 0.75 + 0.5 \, (\Delta a - 0.6 \text{ mm})^{0.77} \tag{2}$$

where 0.75 MN/m is the J-integral at onset of ductile crack growth (J_{IC}) and 0.6 mm is the pseudo crack extension due to crack tip blunting at J_{IC}. In the numerical calculation which can not take account of pseudo crack extension due to crack tip blunting, the use of the following equation was recommended, instead of equation (2).

$$J \text{ (MN/m)} = 0.75 + 0.5 \, (\Delta a)^{0.77} \tag{3}$$

However, it is expected that "J-controlled crack growth condition" is easily violated with small amount of ductile crack growth in this geometry. So we also gave another application phase crack growth criterion based on crack opening angle (COA). Figure 10 shows the COA resistance curve of the present material obtained by CT specimen tests. It can be seen that COA takes constant value, about 0.55, after small amount of crack growth. Because COA values and CTOA (Crack Tip Opening Angle) values do not differ so much with each other after the transition regime, CTOA = 0.55 was given as the third crack growth criterion. Even in this case, however, initiation of crack growth should be predicted by the use of J-criterion ($J = J_{IC} = 0.75$MN/m).

The choice of the crack growth criterion from the above three options was left to each participant.

4. Participants

Participants to this round-robin study are listed in Table 1 with their affiliations. In total 16 groups from six countries submitted their solutions to us.

In the subsequent sections, we use alphabets to designate each participant but there is no correspondence between these and the order in Table 1.

Table 1 Partipants of Round-Robin Study

Country	Name of Participants	Their Affiliation
England	R. Bradford	Central Electricity Generating Board
France	F. P. Champomier	Framatome
Italy	E. Vitale, L. Bertini	University of Pisa / ENEA
Japan	G. Yagawa, H. Ueda	University of Tokyo
	T. Miyoshi, Y. Yoshida	University of Tokyo
	S. Ueda	Japan Atomic Energy Research Institute
	Y. Takahashi, K. Kashima	Central Research Institute of Electric Power Industry
	E. Murakami	Babcock Hitachi
	H. Doi, S. Sakata	Hitachi
	M. Watanabe	Ishikawajima-Harima Heavy Industries
	T. Shimakawa	Kawasaki Heavy Industries
	K. Hojo, Y. Urabe	Mitsubishi Heavy Industries
	M. Asano	Toshiba
USA	V. Papaspyropoulos, B. Brust	Battelle Columbus Laboratories
West Germany	C. Mattheck, H. Moldenhauer	Nuclear Research Center, Karlsruhe
	E. Keim	Kraftwerk Union

5. Finite Element Solutions

5.1 Solution Methods

13 participants submitted their finite element solutions. Table 2 shows the main characteristics of the methods of these solutions.

Table 2 Main Characteristics of Finite Element Solutions

Solution No.	A	B	C	D	E	F	G	H	I	J	K	L	M
Element Type	8-node	20-node	20-node	20-node	20-node	20-node	20-node	20-node	20-node	20-node	20-node	20-node	20-node +8-node
Number of Elements	829	383	235	306	540	252	306	224	247	293	270	688	767
Number of Nodes	1148	2125	1313	1610	2823	1360	1730	1191	1355	1760	1528	3421	1721
Elastic	○	○	○	○	○	○	○	○	○	○	○	○	○
Elastic-Plastic	○	○	○	○	○	○		○	○		○	○	○
Elastic-Plastic -Crack Growth	○	○		○					○				○
Formulation	Large Disp.	Small Disp.	Small Disp.	Small Disp.	Small Disp.	Small Disp.	Small Disp.	Small Disp.	Small Disp.	Small Disp.	Small Disp.	Small Disp.	Large Disp.
J-integral Evaluation	N/A	VCE	VCE	Surface Int.	VCE	VCE	VCE	Surface Int.	VCE	VCE	VCE	VCE	VCE
Stress-Strain Relation *	True (ML)	Engng (ML)	Engng (ML)	Engng (ML)	Engng (ML)	True (BL)		Engng (ML)	Engng (ML)		True (ML)	Engng (ML)	True (ML)
Crack Growth Criterion	c_1-Δa (modified)	J-Δa		c_1-Δa					c_1-Δa				J-Δa

Note *) BL --- Bilinear Representation
ML --- Multilinear Representation

Calculations were made by using proprietary computer programs or commercial ones, including MARC, ADINA and ABAQUS. All calculations were conducted by three-dimensional brick elements. 20-noded isoparametric elements were used in the all calculations except solution A where 8-noded isoparametric elements were employed. In solution M, both elements were used for the neiborhood of the crack and for other portion, respectively. Solution A modelled the crack by a rectangular shape rather than the semi-elliptical shape.

All participants constructed their finite element models for a quarter part of the whole plate, considering symmetry condition. Number of total elements varied between 224 and 829, while number of total nodes ranged from 1148 to 3421.

Two participants conducted only elastic analysis, six elastic-plastic without crack extension and five elastic-plastic-crack growth analysis. For the modeling of elastic-plastic behavior of the material, seven participants used multi-linear modeling of the engineering stress-strain curve, while four used the approximations for the true stress-strain curve.

Only two participants conducted their calculations based on large displacement formulation and other calculations were conducted based on the small displacement assumption.

The J-integral values along crack front were calculated by virtual crack extension method in many calculations while the surface integration method was also used by two participants (6).

Among the six participants who made crack growth calculation, four used the c_1-Δa relation and two adopted the J-Δa relation as the crack growth criterion in their analyses. In solution A, modified c_1-Δa relation was used for adjusting the stiffness of their solution to experimental data. Modified relation between c_1 and Δa is given as

$$c_1 = 2.0 \text{ mm} + 0.4 \Delta a \tag{4}$$

5.2 Results of Calculations

Figure 11 shows the calculated load versus global displacement relations of 10 solutions. Good agreement is obtained between these solutions. Relatively large difference between solution F and K, and other solutions reflects the difference in modeling of uniaxial stress-strain curve. The results obtained by participants A and M, which used large deformation formulation with true stress-strain relation are in good agreement with those by many participants in which small deformation formulation and engineering stress-strain relation are utilized. This result comes from the fact that most region of the specimens is in approximatelly uniaxial tensile condition. The stiffer results of solutions F and K may be attributed to the use of true stress-strain relation with small deformation formulation in their solutions.

Comparison of c_1 versus applied load relations is shown in Figure 12. In this figure, solutions A, F and K give the results somewhat stiffer than the others although the difference is not so manifest before crack extension, i.e. c_1 less than 2.92 mm (2.0 mm only for solution A). The reason for this may be the same as the above as for solutions F and K. The stiffness of the solution A should be related with the use of 8-noded isoparametric elements. The differences between the solutions become larger as the crack grows. Gradual decrease in the load with the crack extension was predicted by the solutions D and I while load kept increasing according to the solutions A, B and E.

Comparisons of Figures 11 and 12 with Figures 2 and 3 reveal good agreement between the numerical results and experimental results for both the global displacement and the near-crack displacement. Thus it can be concluded that the finite element solutions simulated the actual deformation behavior of the specimens in good accuracy.

Calculated relations between the load and the J-integral at the deepest point of the crack before the crack extension are shown in Figure 13. All solutions are in relatively good agreement with each other. The difference observerd in the range of the J-integral below 200 kN/m is simply the result of difference in the size of load increments. On the other hand, the difference in the range of the J-integral above 300 kN/m maybe was resulted in by the difference of the formulation and the stress-strain modeling. Solution M based on the large deformation formulation gave the larger J-integral value than the others which utilized small deformation formulation. Divergence of the two solutions (F and K) from the others can be considered as the result of the utilization of true versus engineering stress-strain curves. However, the agreement between all the solutions is good in overall sence.

Two participants made calculation of the J-integral during the crack extension according to c_1 -Δa relation. Calculated J versus Δa curves are shown in Figure 14 with the J-resistance curve obtained by CT specimen (without blunting line). The value of the J-integral plotted in this figure was obtained by the largest evaluation path in each solution. J-integral values obtained by two solutions are in good agreement with each other till the crack extension of 2.5 mm but the difference grows after that. These values are also in good agreement with the J-resistance curve obtained by CT specimen tests up to the crack extension of about 1 mm. After this point, the numerical values tend to take smaller values than the CT specimen resistance curve. This maybe comes from the violence of the J-controlled crack growth condition.

On the other hand, two other participants made crack growth analyses by assuming the CT-specimen J-resistance curve. In solution B, crack shape was continuesly modified so that every portion of the crack front grew according to the J-resistance curve (without the assumption of semi-elliptical shape of the extending crack). The nodal shifting method was utilized in addition to the nodal release method. In solution M, the nodal release method was employed with the assumption of semi-elliptical shape of the extending crack, in which the J-integral value at the deepest point of the crack was utilized for deciding the occurence of the crack growth. c_1 versus Δa relations obtained by these calculations are compared with the experimental data in Figure 15. The solution B gives the result which is in relatively good agreement with the experimental result, while smaller c_1 values were obtained by the solution M. The use of 8-noded elements in the solution M is one possible reason for this result.

6. Solution by Simplified Methods

6.1 Description of Methods

Four solutions were obtained by using simplified methods. Description of the outline of each method is shown below.

(1) Method I (R6 method)

In this calculation, J-integral values were estimated from the values of load and crack depth using the R6 rule developed by CEGB (7). The contour of the failure assessment diagram was that called option 2 in (7), which is represented as:

$$K_r = (E\,\varepsilon_{ref}/s_{ref} + L_r^{\,3} s_y/(2E\varepsilon_{ref}))^{-1/2} \qquad \text{for } L_r \leq L_{r\ max} \qquad (5)$$

$$K_r = 0 \qquad \text{for } L_r > L_{r\ max}$$

The contour was thus constracted assuming that the material follows the Ramberg-Osgood relationship given as:

$$\varepsilon_{ref} = s_{ref}/E + 8.78\,\varepsilon_y\,(s_{ref}/s_y)^{3.9} \qquad (6)$$

where ε_{ref}, s_{ref}, s_y and E represent the strain, stress, yield stress and Young's modulus, respectively. It should be noted that this equation was derived from the true stress-strain curve of the different production of STS42 pipe(8) and gives about 10 per cent higher stress than the present material in the strain range between 2 and 8 per cent.*)

It was assumed that stable crack growth occurs when the point representing the loaded specimen is precisely on the failure assessment contour, although this contour was meant to be conservative. For predictive purpose, it is generally recognized that $L_{r\ max}$ is to be taken as s_u/s_y. This was also assumed in the present analysis. The values of s_y and s_u were determined as 362 MPa and 592 MPa, respectively, based on the data in (8).

The reduction of the cross section due to significant plastic deformation was taken into account by the following equations.

$$A_m = A^0 \exp(-\varepsilon_{ref}) \qquad (7) \qquad\qquad s_{ref} = P/A_m \qquad (8)$$

where A_m is the modified cross-section while A^0 is the initial cross-section of uncracked portion of the specimen. A_m can be obtained by solving eqs.(6-8) with iteration procedure. Although this reduction in cross-section area is that of the uncracked section, it was assumed to be valid also for the cracked section.

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*) It was not intended but happened due to the failure of the authors to transmit the completed problem description to the contributer of this solution.

Using this value of A_m, L_r was evaluated as follows:

$$L_r = (P/A_{cm})/s_y \tag{9}$$

$$A_{cm} = (A^0 - \pi ac/2)(A_m/A^0) \tag{10}$$

where a, c denote depth and half length of the crack, respectively.

Stress intensity factor at the deepest point of the crack was estimated using the expression in Ref (9).

The estimation scheme is then the following:

1) Choose Δa
2) Read P, Assume $A_m = A^0$
3) Compute ε_{ref} according to eq.(6) and (8)
4) Compute A_m according to eq.(7)

   Repeat steps 3) and 4) until convergence is achieved
5) Compute L_r according to eq.(9) and (10)
6) Compute K_r from L_r and R6 contour (eq.(5) and (6))
7) Compute stress intensity factor (k) using expressions in Ref (9)
8) Compute $K(\Delta a) = K/K_r$
9) Compute $J(\Delta a) = K^2(\Delta a)/E$

Results are summarized in Table 3.

Table 3 Results by Simplified Method I

Δa(mm)	P(kN)	Lr	Kr	K(MNm$^{-2/3}$)	J(MN/m)
0.0	615.0	1.303	0.224	63.884	0.390
1.0	645.0	1.409	0.201	74.225	0.653
2.0	660.0	1.483	0.187	82.315	0.926
3.0	665.0	1.534	0.178	88.274	1.170
4.0	660.0	1.559	0.174	91.775	1.325
5.0	650.0	1.572	0.172	93.457	1.406
6.0	620.0	1.530	0.179	90.896	1.232
7.0	575.0	1.447	0.194	85.050	0.923

(2) Method II (Simplified line-spring/Reference stress method)

The method presented by Bradford (10) was used for evaluating $c_1 - P$ relation and the J-integral value. In this method, the plate with a part-through crack is modelled by splitting into two 2-dimensional problems (i.e. edge-cracked plate and center-cracked plate). Then the simultaneous equation was derived based on the following conditions.

1) Compatibility of the displacement
2) equivalence of the stress
3) Relation between compliance and J-integral of each portion
4) Relation between J-integral and applied stress derived by the reference stress formulae by Ainsworth (11) of each portion

The details of this method are given in Ref (10). The results obtained by this method are listed in Table 4.

Table 4 Results by Simplified Method II

i) c_1 versus P relation

c_1 (mm)	P(kN)
0.15	300.
0.32	400.
0.56	450.
0.86	500.
1.11	525.
1.32	550.
2.30	575.

ii) c_1 versus J relation

c_1 (mm)	J(MN/m)
0.3	0.07
0.6	0.16
1.0	0.29
1.5	0.55
2.0	0.71
2.13	0.76

(3) Method III (Fully plastic solution method)

In this type of calculations, nondimensional solutions called as fully plastic solutions were used for the estimation of J-integral value at the deepest point of the crack. The fully plastic solutions for the plates with a surface crack were obtained by nonlinear finite element analyses for several combinations of geometrical parameters by Yagawa et al (12). Interpolated expression of these solutions can be utilized for the estimation of the J-integral based on the assumption of power hardening property of the material as follows:

$$J = h \alpha \sigma_0 \varepsilon_0 t \, (\sigma/\sigma_0)^{n+1} \qquad (11)$$

where h, t and σ denote the interpolated fully plastic solution, the thickness of the plate and applied stress at remote section, respectively, while σ_0, ε_0, α and n are material constants describing the strain hardening characteristics of the material.

$$\varepsilon / \varepsilon_0 = \alpha (\sigma / \sigma_0)^n \qquad (12)$$

Two calculations were made based on this method, which are

1) Method III-1, in which the following material constants obtained by the approximation of the true stress-strain curve of the different production

of STS 42 pipe (8) were used as in Simplified Method I, and

$$\sigma_c = 362 \text{ MPa}, \quad \varepsilon_c = 0.00173, \quad \alpha = 8.78, \quad \text{and } n = 3.90$$

2) Method III-2, in which the following material constants obtained by the approximation of the engineering stress-strain curve of the present material were used.

$$\sigma_c = 300 \text{ MPa}, \quad \varepsilon_c = 0.00130, \quad \alpha = 6.54 \quad \text{and } n = 5.62$$

It should be noted that the experimental P-Δa relation was used for the evaluation of the J-integral values after crack initiation, instead of the c_1 - Δa relation.

The results are summarized in Table 5.

Table 5 Results by Simplified Method III

i) Method III-1

c_1(mm)	P(kN)	J(MN/m)	Δa(mm)
1.909	615.	0.328	0.0
2.912	645.	0.639	1.0
4.040	660.	1.024	2.0
5.270	665.	1.457	3.0
6.467	660.	1.871	4.0
7.677	650.	2.270	5.0
8.027	620.	2.321	6.0
7.506	375.	2.049	7.0

ii) Method III-2

c_1(mm)	P(kN)	J(MN/m)	Δa(mm)
0.203	400.	0.0861	0.0
0.546	500.	0.328	0.0
1.29	600.	0.980	0.0
1.66	632.	1.33	0.0
2.54	661.	2.76	1.85
4.14	667.	3.48	3.56
6.49	656.	3.23	5.26
7.99	619.	2.08	6.97

6.2 Comparison of the Results

c_1 versus P relations obtained by three simplified methods are shown in Figure 16. Although the scattering of the solutions is larger than that of the finite element solutions shown in Figure 12, three solutions are in relatively good agreement with each other. Method II gave somewhat smaller load values than the two solutions by Method III, which are in good agreement with each other.

Results of the J-integral estimation for stationary crack stage are shown in Figure 17. The solutions by Method II and Method III-2 are close to the upper bound of the finite element solutions, while Method I and Method III-1 made the prediction of smaller J-integral than the finite element solutions.

Finally, estimated J versus Δa relations are plotted in Figure 18 with the results of finite element calculations. In the early stage of the crack extension, Method I and Method III-1 gave about 60 per cent smaller J-integral values than the finite element solutions. On the contrary, higher J-integral values were predicted by Method III-2. Both Method I and Method III-1 used the stiffer material property than the actual and this may be the reason for the small J-integral values in these solutions. More examination is definitely necessary to lead general conclusions on the characteristics of these simplified methods.

7. Concluding Remarks

Many finite element solutions were obtained as well as some simplified method solutions. Conclusions drawn from the comparisons of these solutions can be summarized as follows:

1) Three-dimensional finite element solutions showed good agreement with each other in general. Main causes for the differences between the solutions are the differences in the modelling of stress-strain relation and small versus large displacement formulation.

2) J-integral versus crack extension relations are in good agreement with J-resistance curve obtained by CT specimen tests. This indicates that J-resistance curve obtained by CT specimen tests can be used for the prediction of initiation and early stage of ductile crack growth from part-through cracks.

3) J-integral values estimated by some simplified methods showed larger scattering than the finite element solutions, although the deformation behavior was predicted with relatively good accuracy. More detailed study is needed to clarify the applicability of the simplified methods.

Acknowledgment

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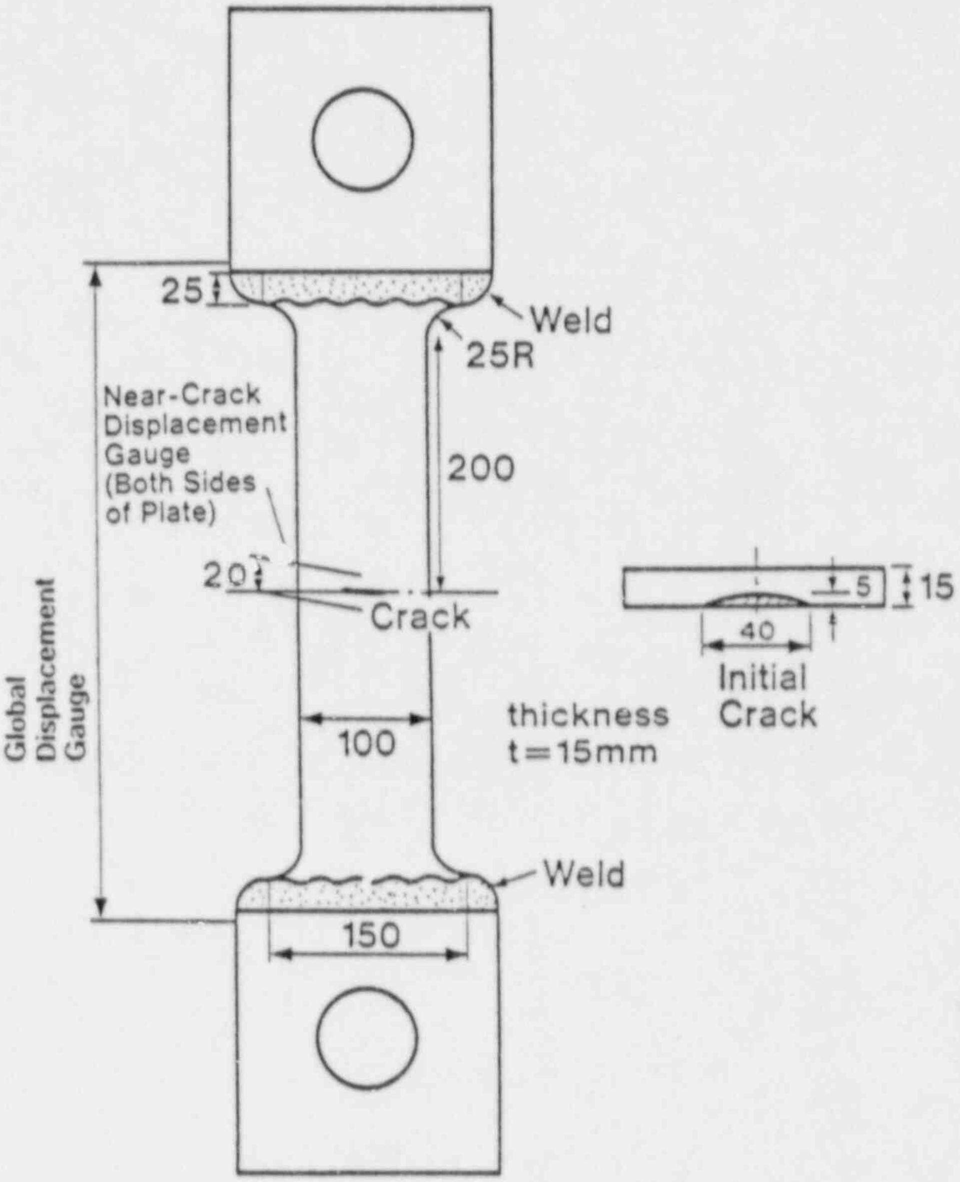


Figure 1. Geometry of the test specimens
(Dimensions in mm)

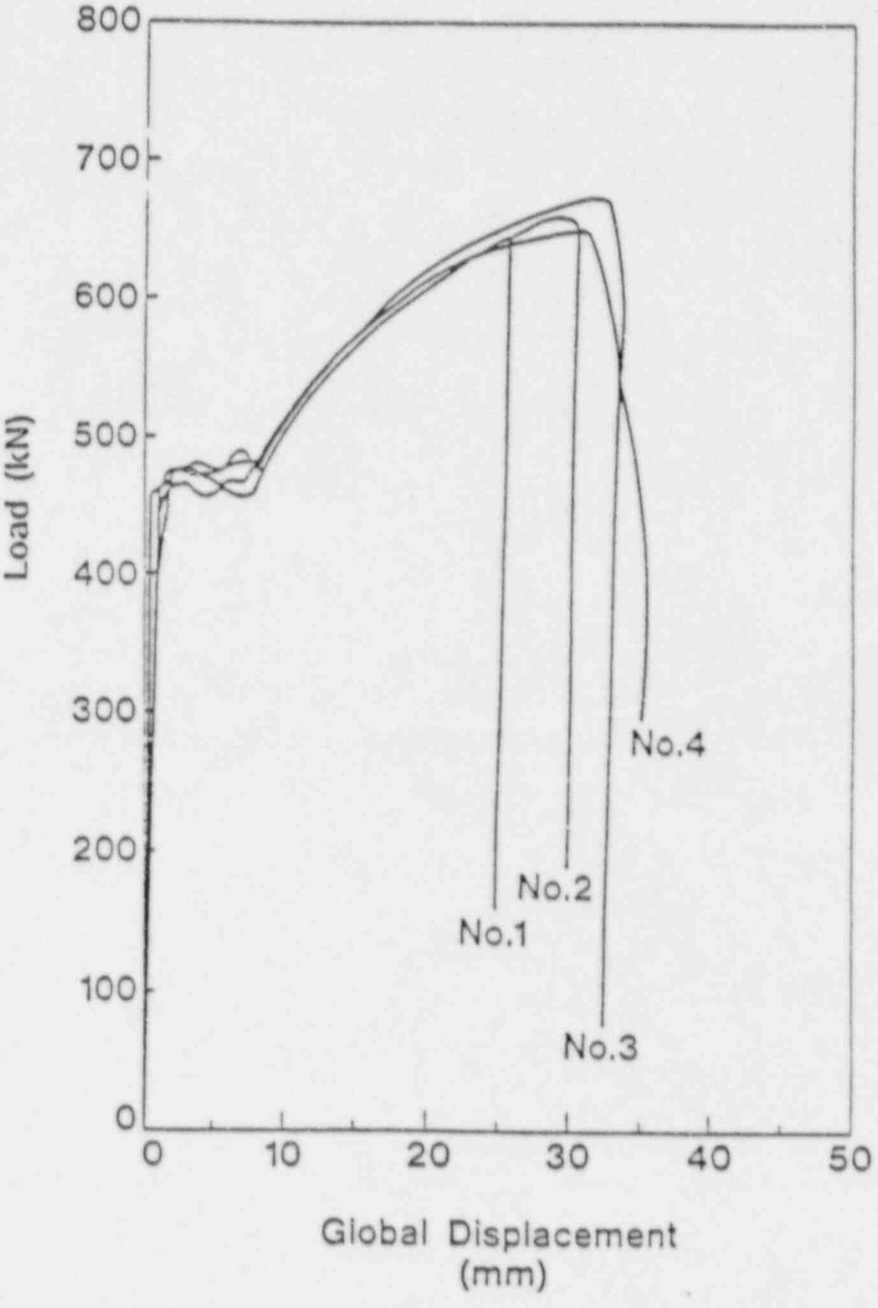


Figure 2. Relations between Load and Global Displacement (d)

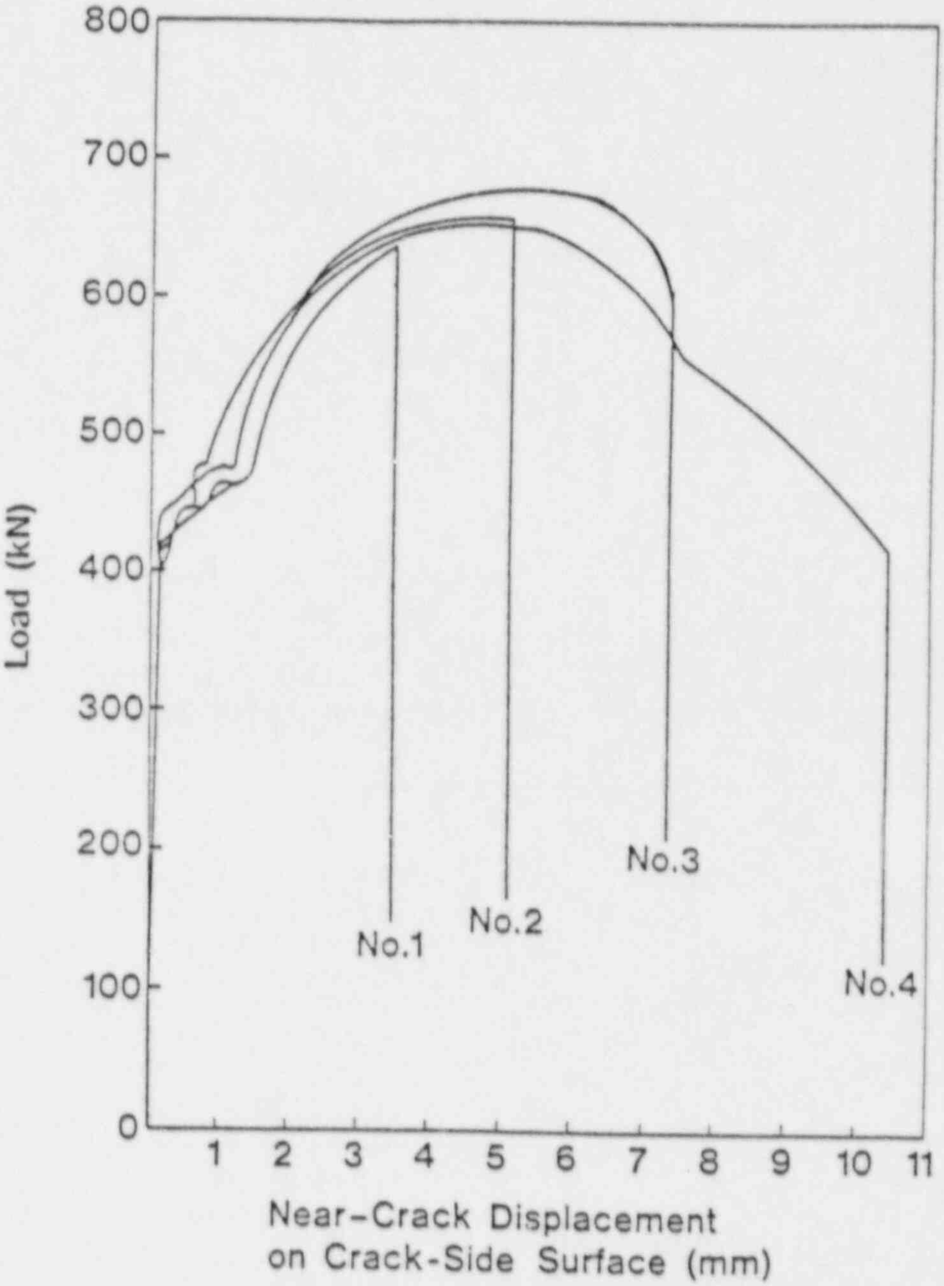


Figure 3. Relations between Load and Near-Crack Displacement on Crack-Side Surface (c_1)

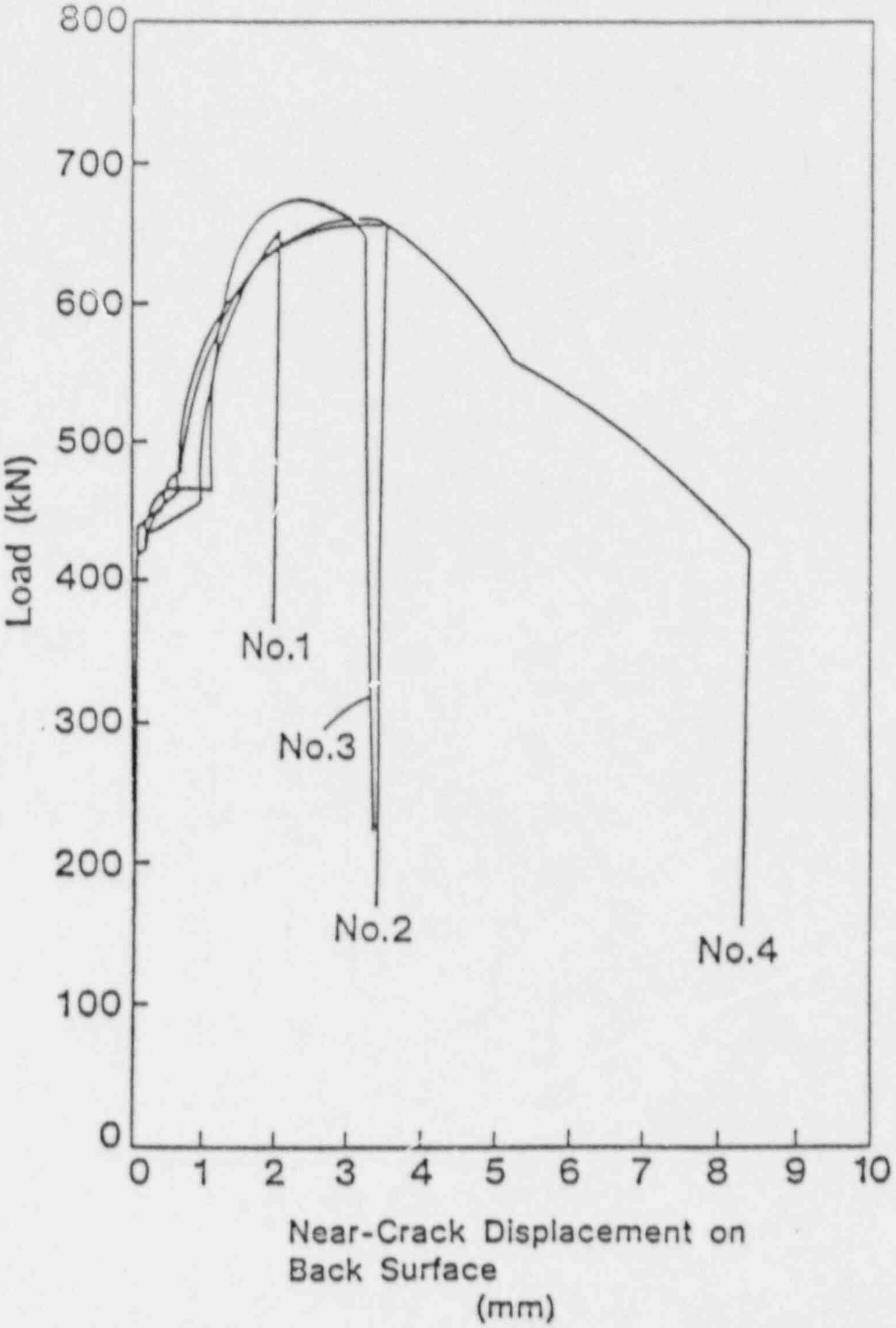
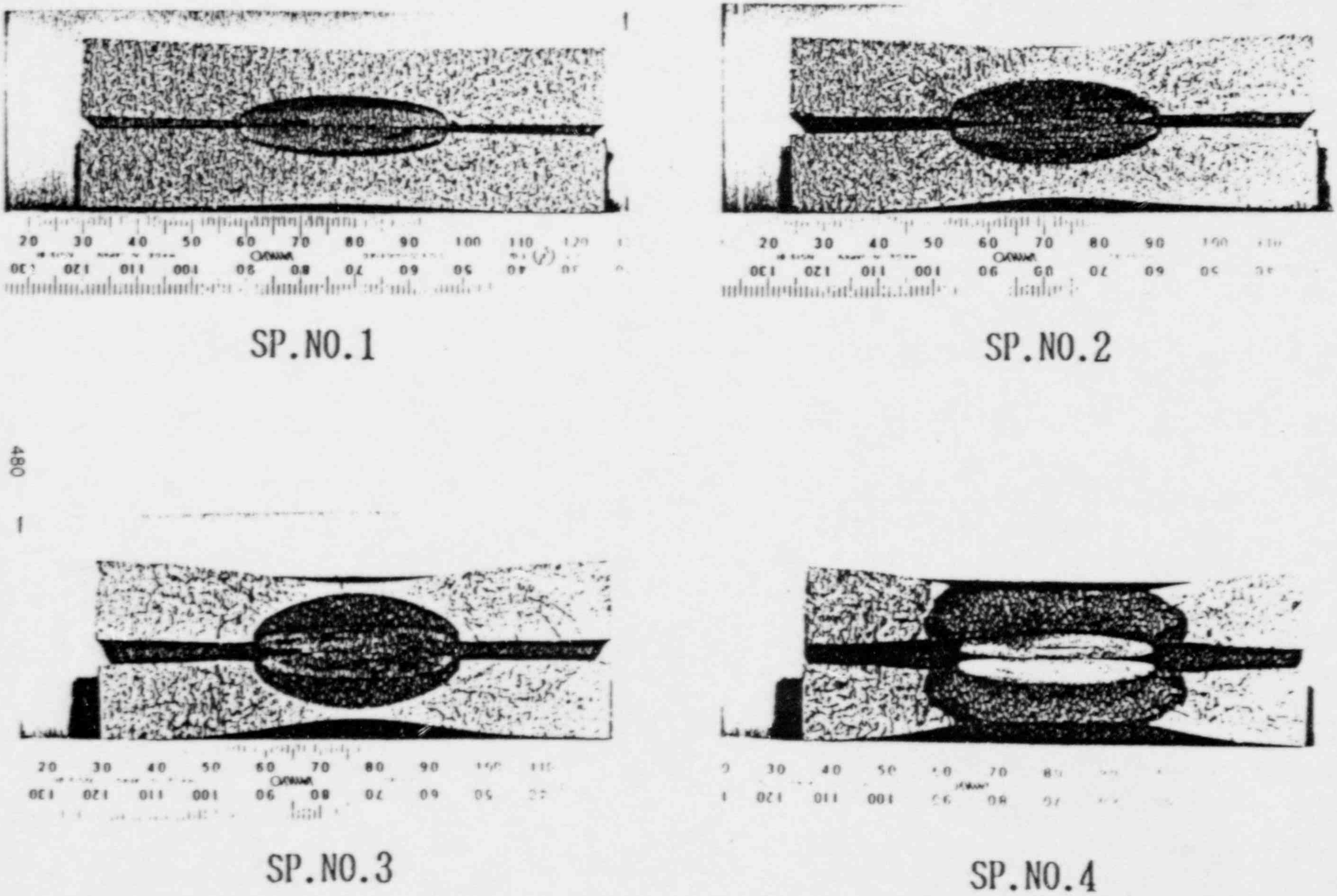


Figure 4. Relations between Load and Near-Crack Displacement on Back Surface (c_2)



SP.NO.1

SP.NO.2

SP.NO.3

SP.NO.4

Figure 5. Fracture Surface Appearance of Each Specimen

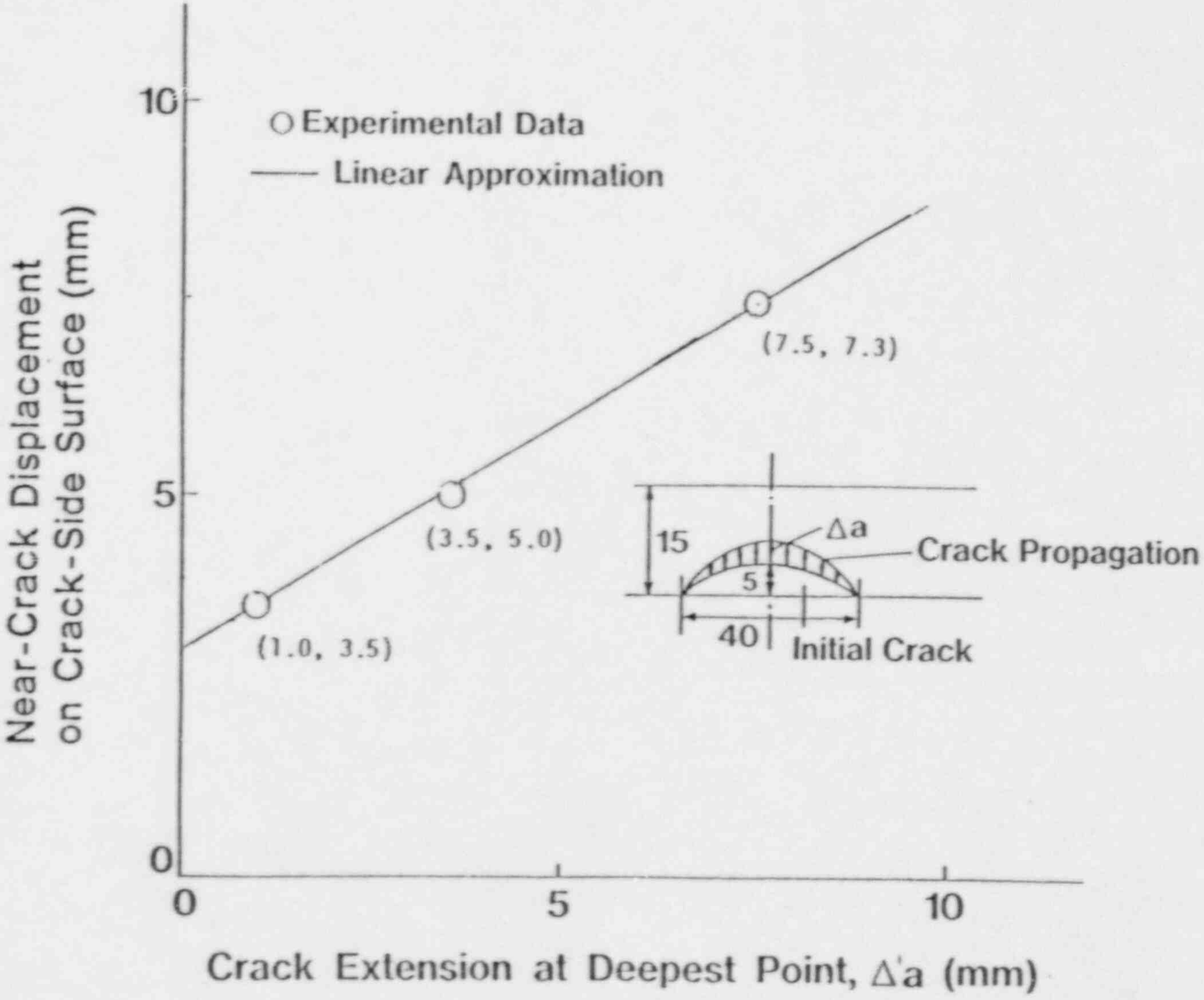


Figure 6. Relation between Crack Extension at Deepest Point and Near-Crack Displacement on Crack-Side Surface (c_1)

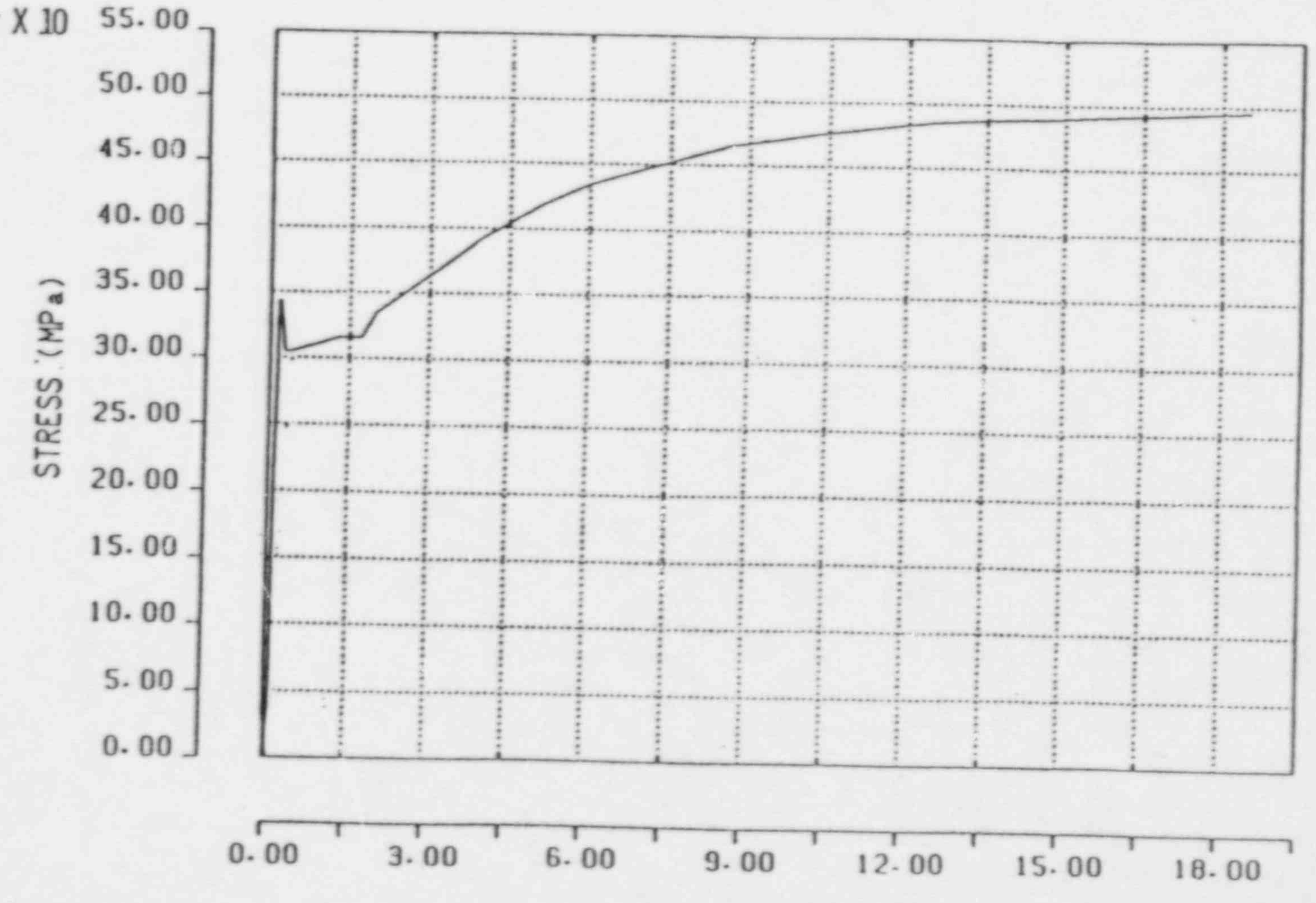


Figure 7. Engineering Stress - Strain Curve

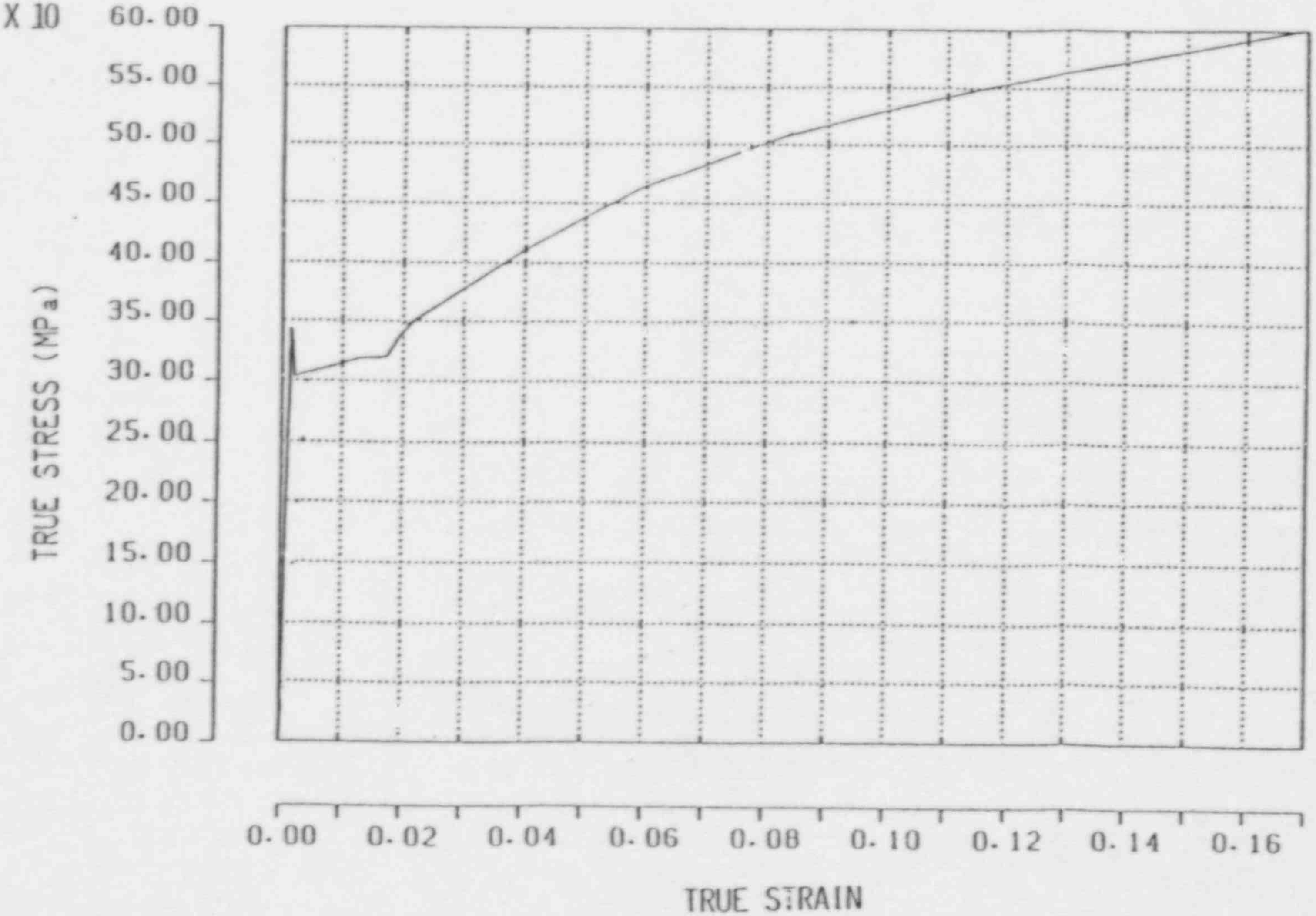


Figure 8. True Stress - Strain Curve

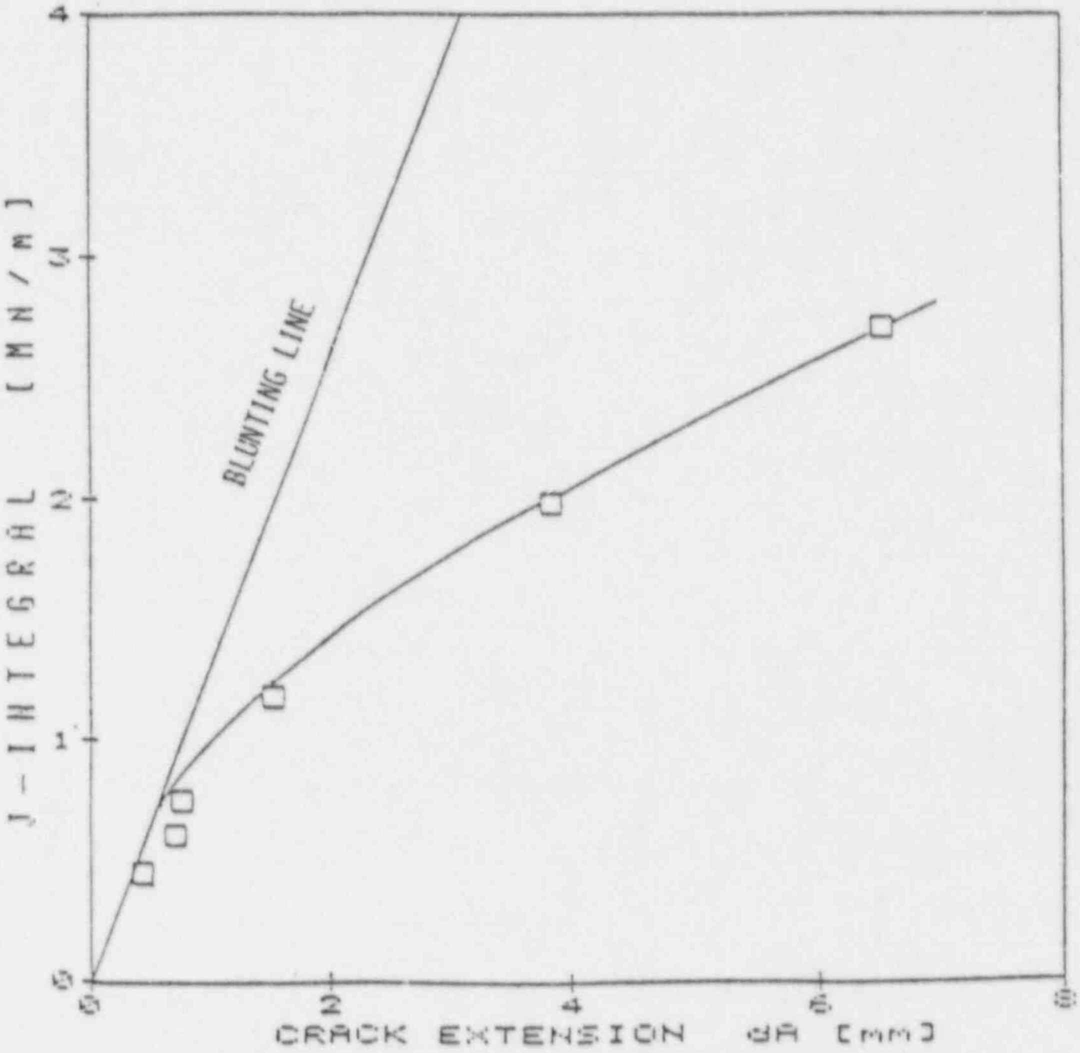


Figure 9. J-integral resistance Curve
Obtained from CT Specimen Tests

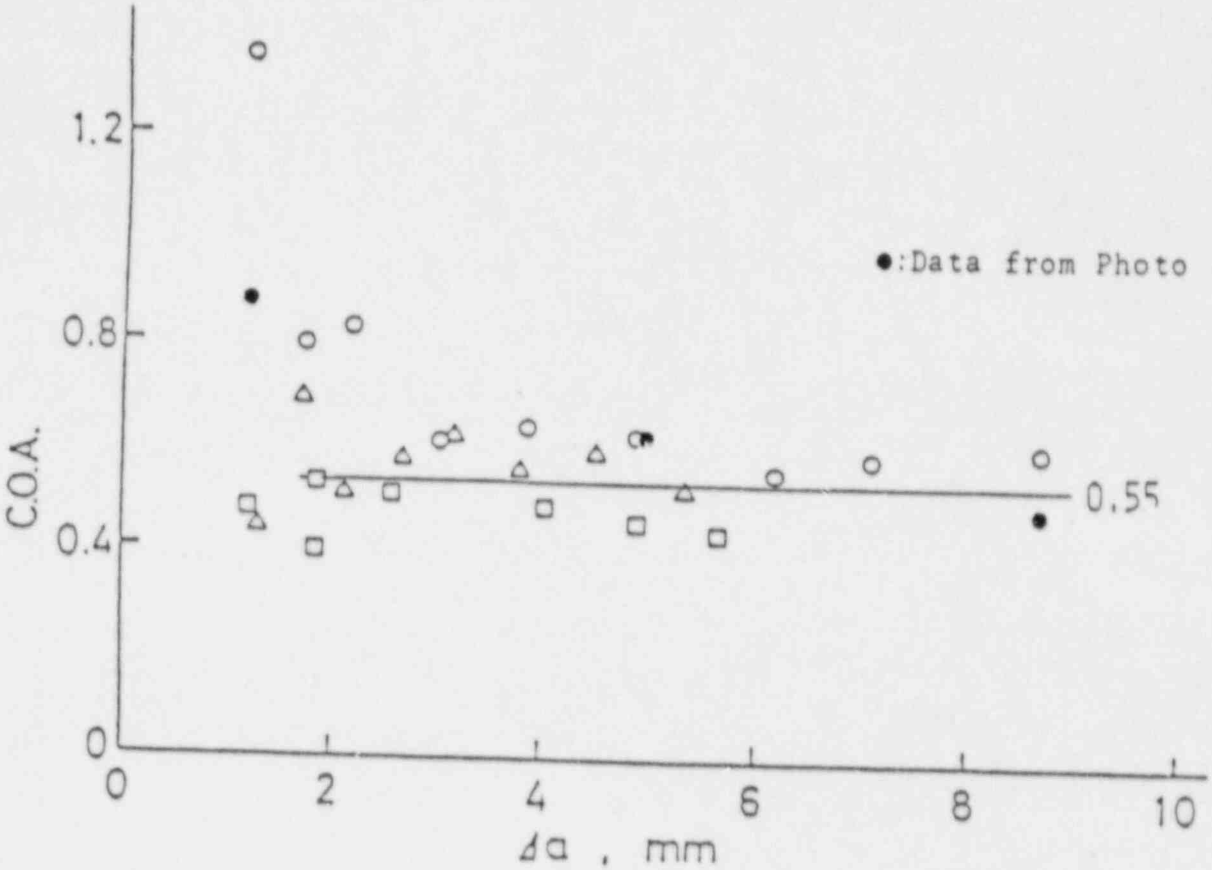


Figure 10. COA Resistance Curve Obtained from CT Specimen Tests

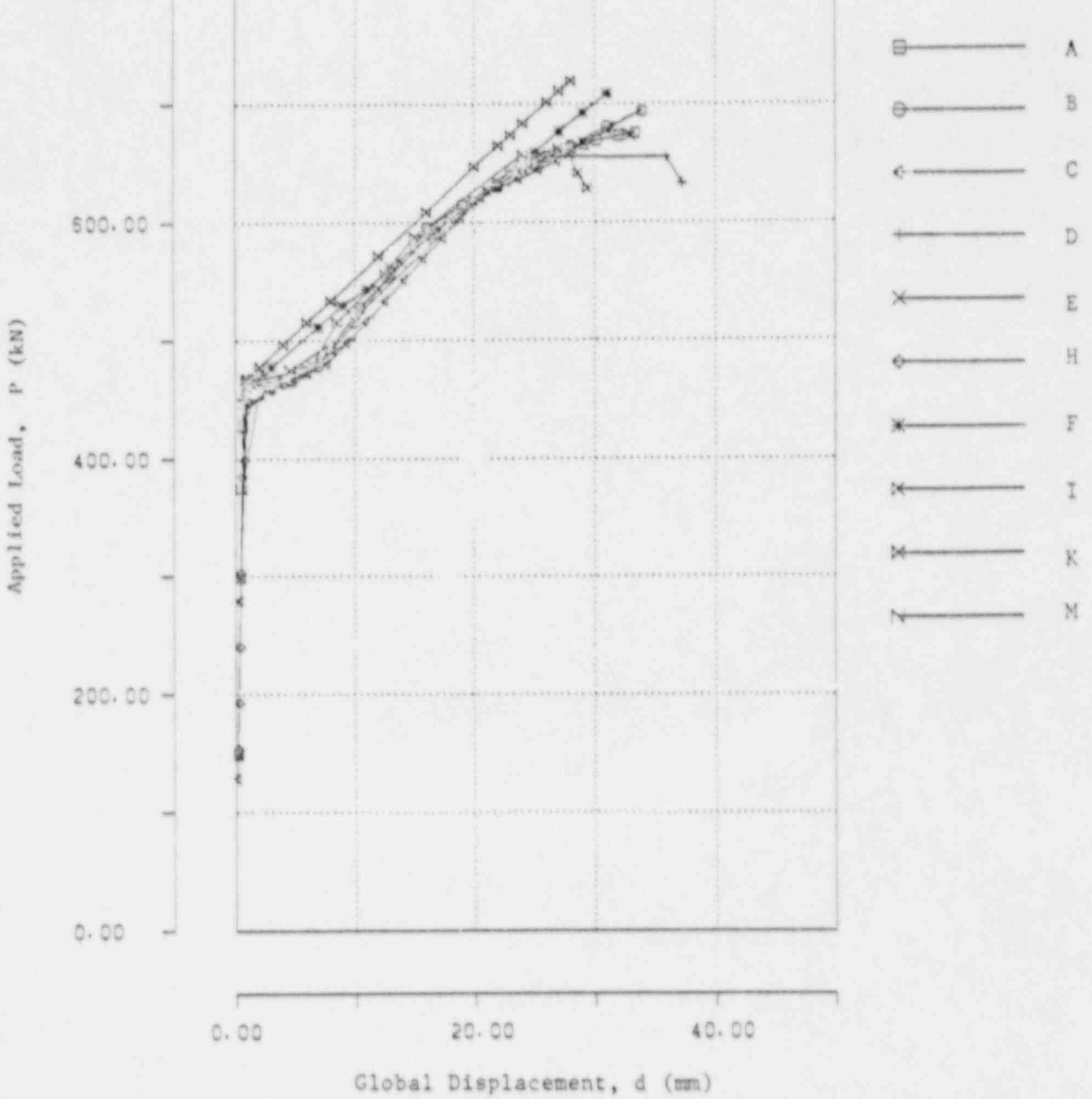


Figure 11. Relation between Load and Global Displacement Obtained by Finite Element Calculations

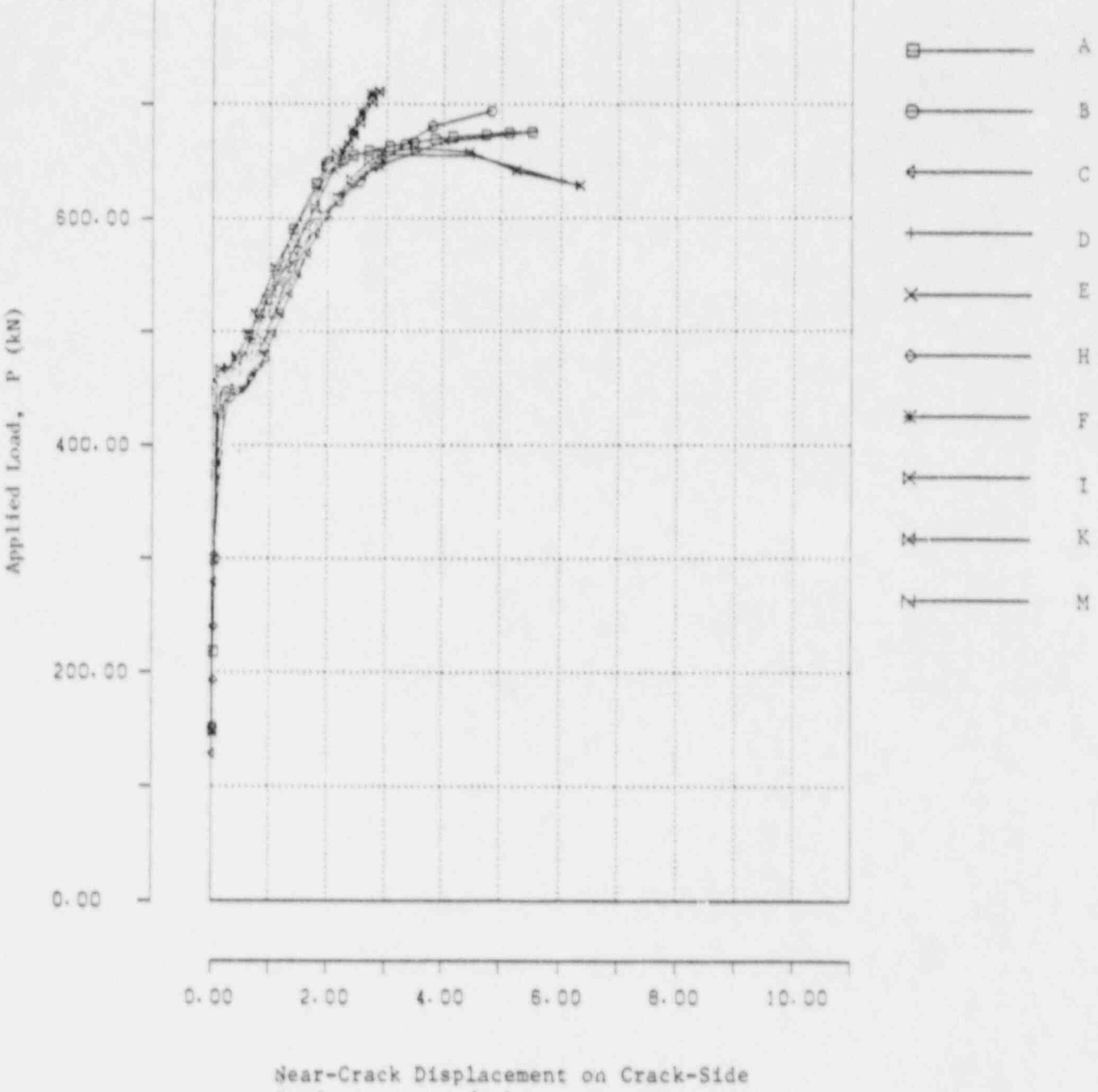


Figure 12 Relation between Load and Near-Crack Displacement on Crack-Side Surface Obtained by Finite Element Calculations

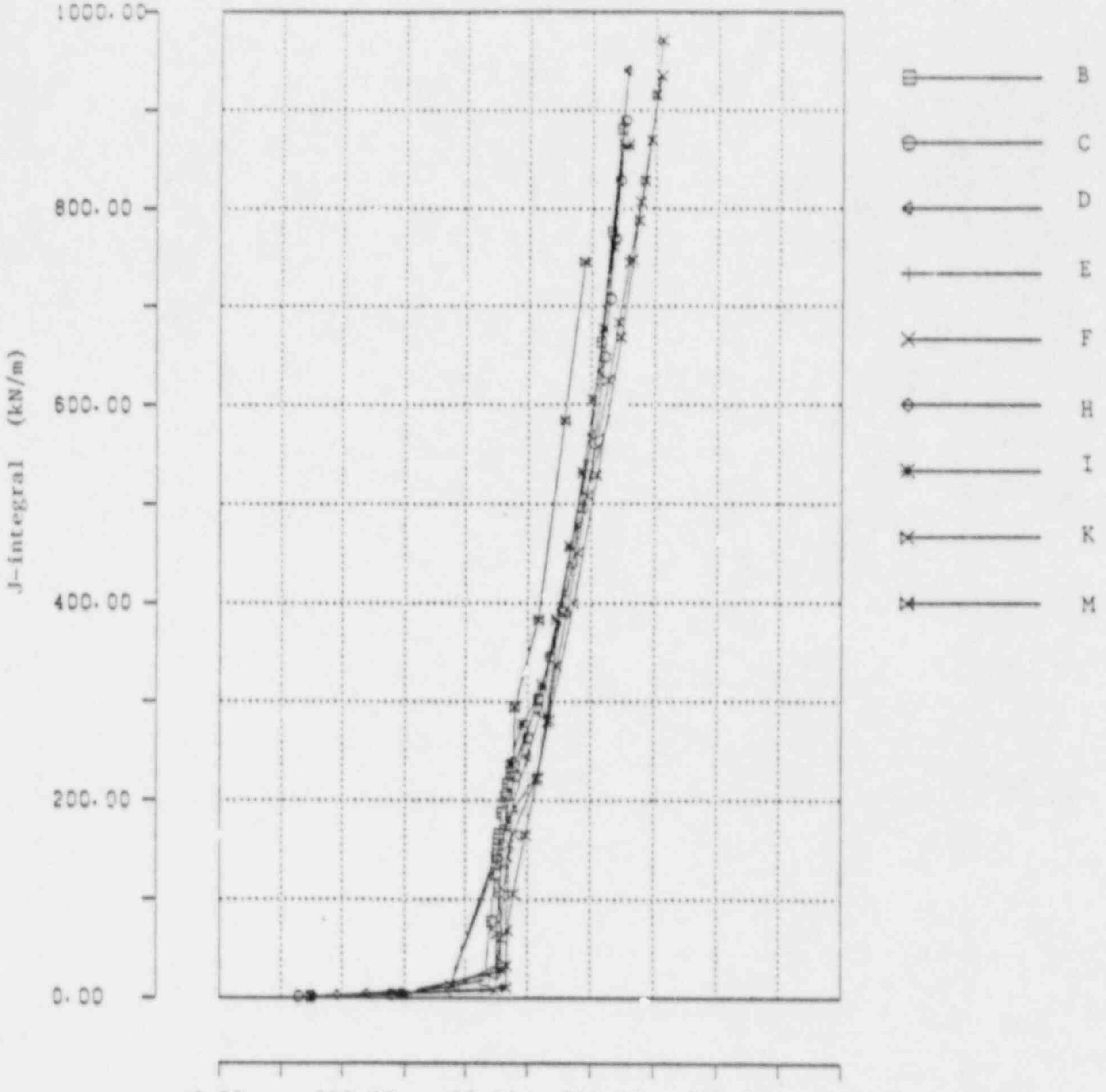


Figure 13 Relation between Load and J-integral at the Deepest Point of the Crack Obtained by Finite Element Calculations (up to onset of crack growth)

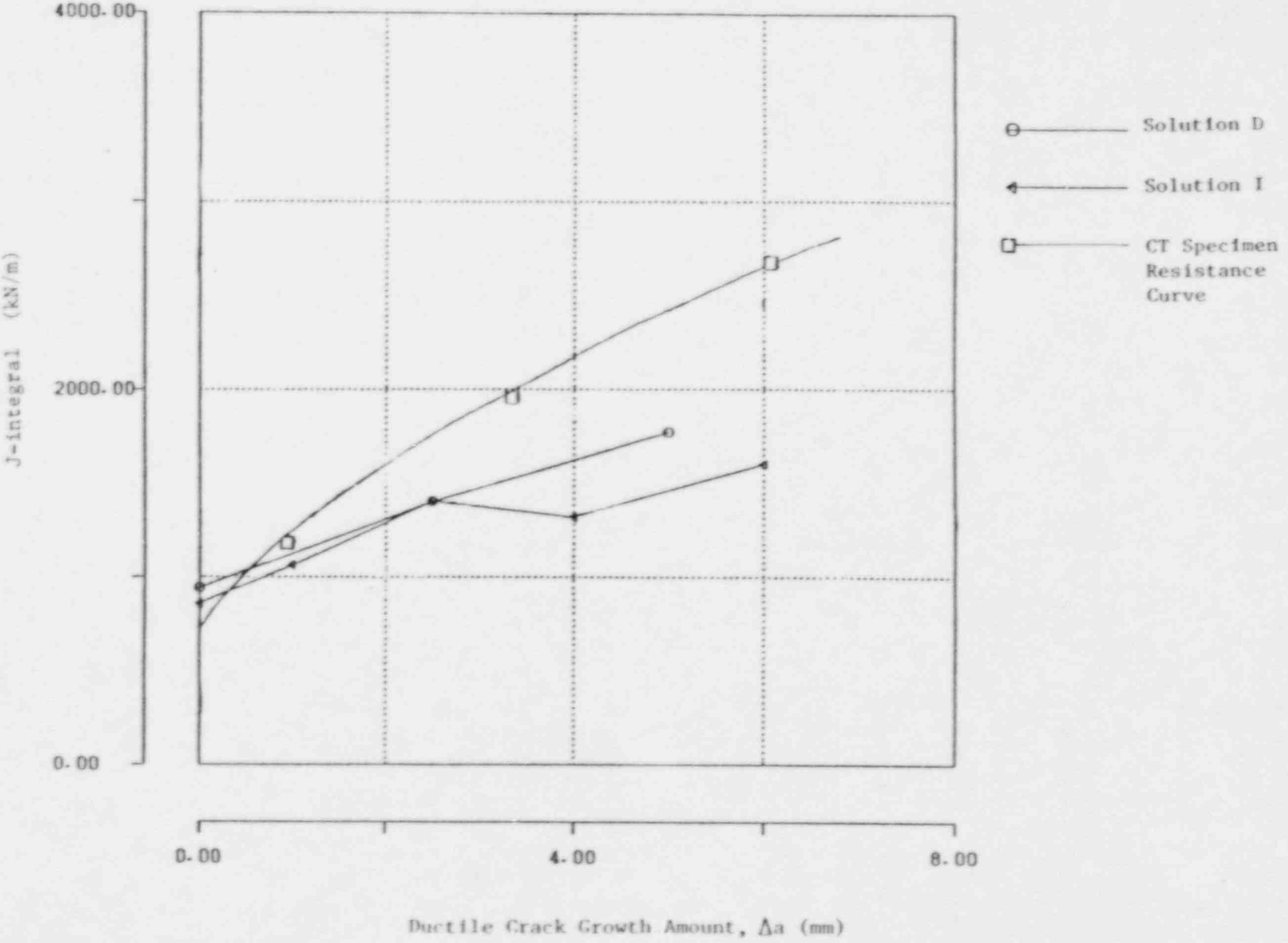


Figure 14 Comparison of J-Δa Relations Obtained by Finite Element Calculations with CT Specimen Resistance Curve

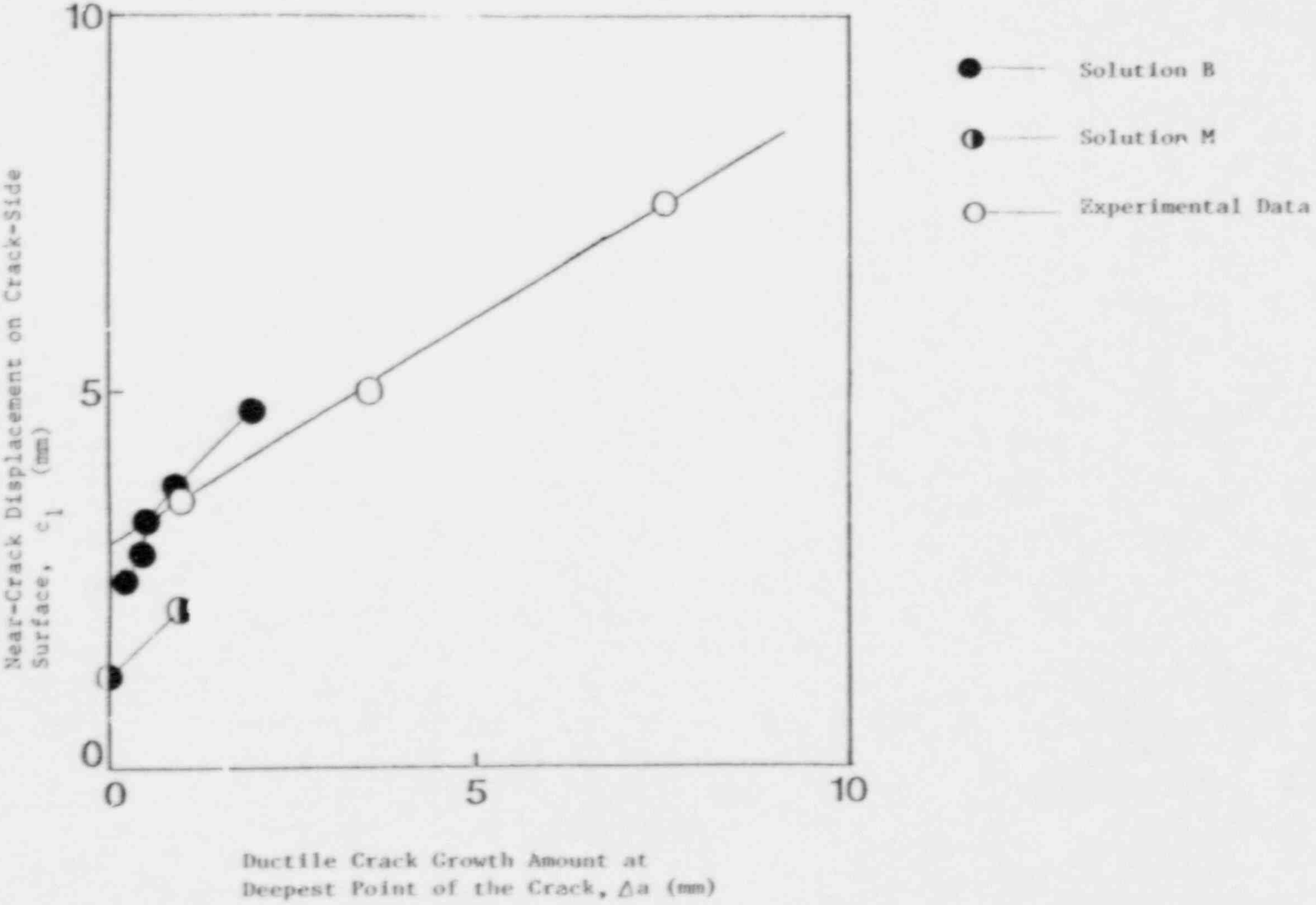


Figure 15 Comparison of $c_1 - \Delta a$ Relations Obtained by Finite Element Calculations and Experimental Data

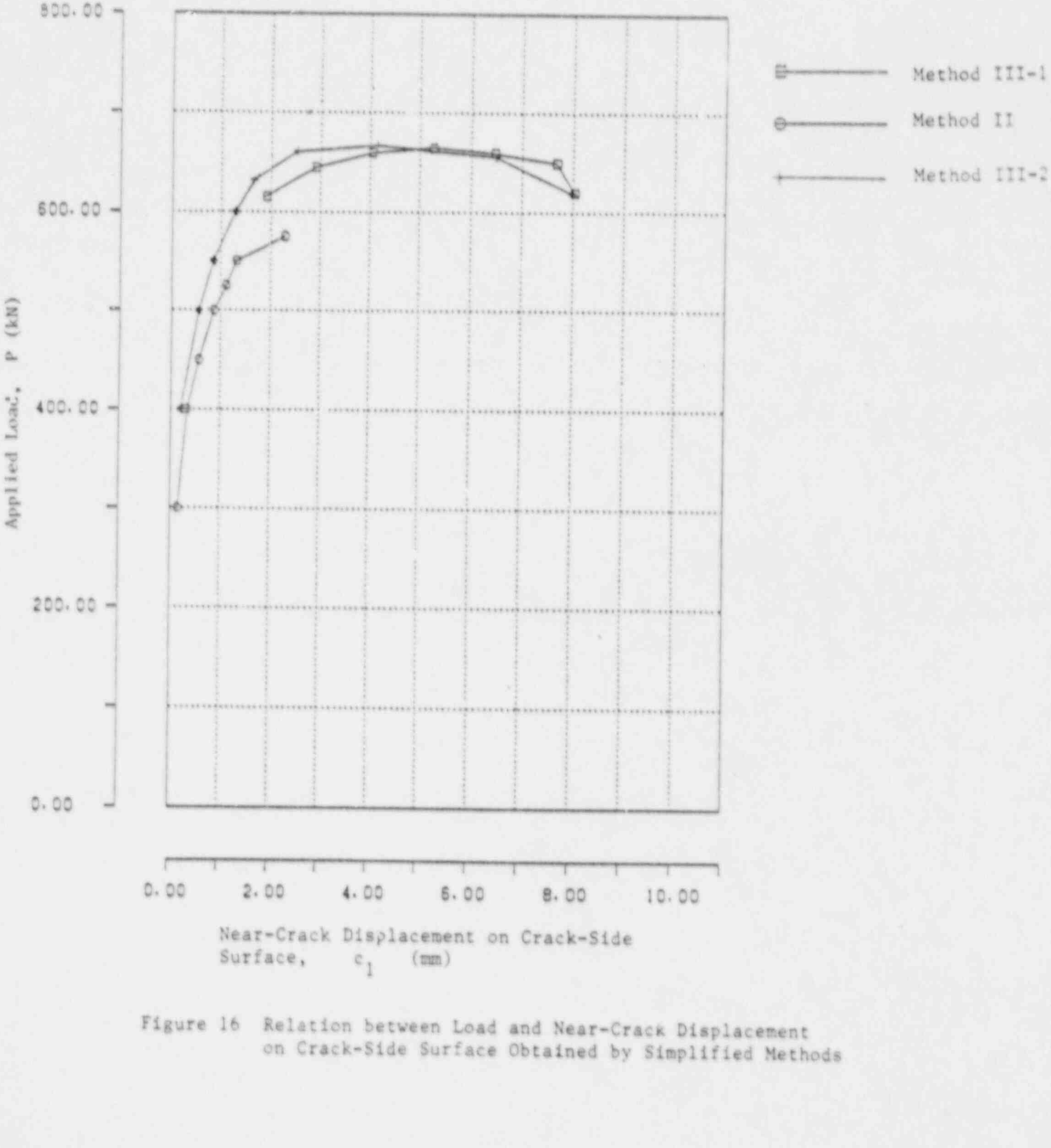


Figure 16 Relation between Load and Near-Crack Displacement on Crack-Side Surface Obtained by Simplified Methods

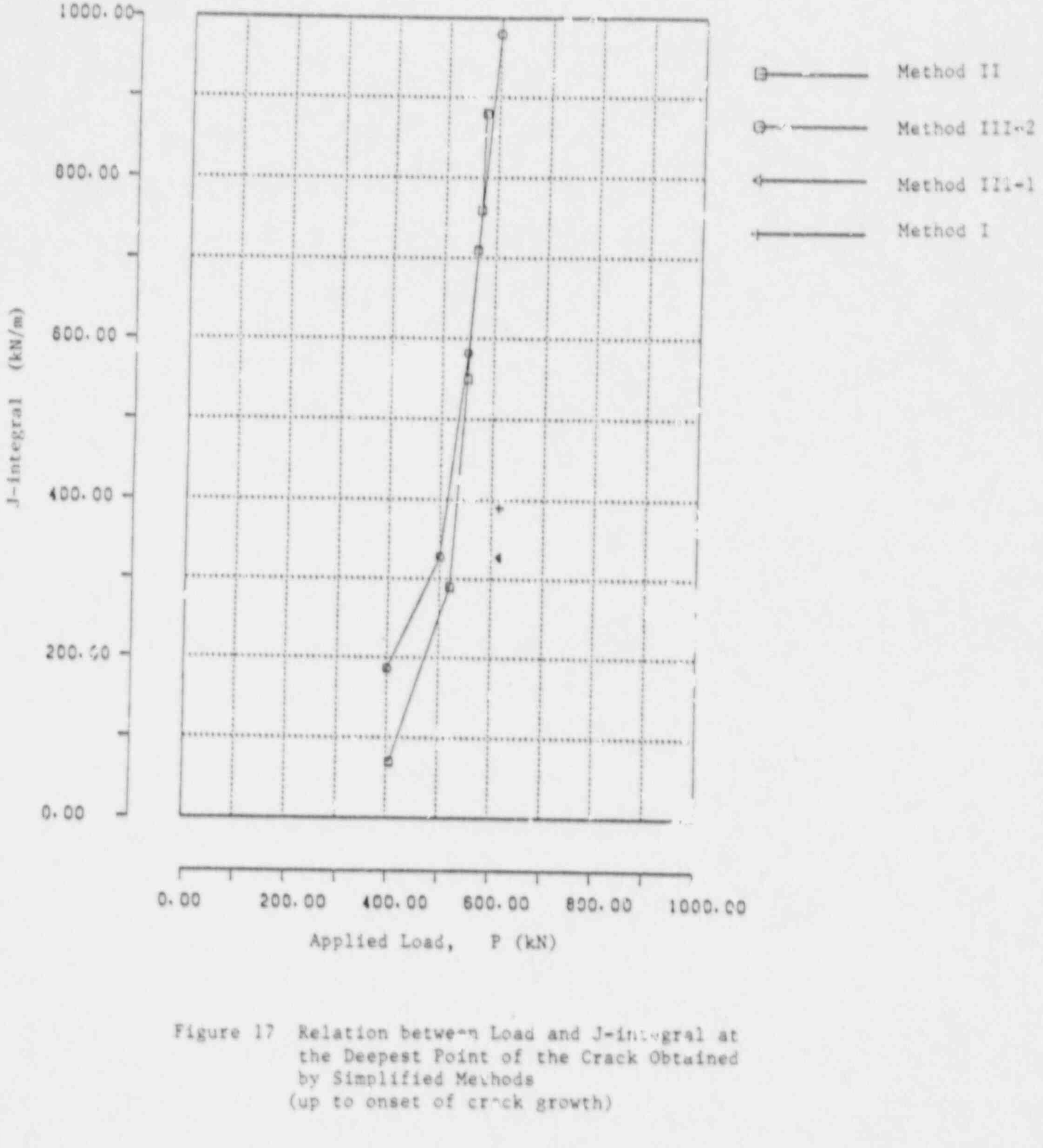


Figure 17 Relation between Load and J-integral at the Deepest Point of the Crack Obtained by Simplified Methods (up to onset of crack growth)

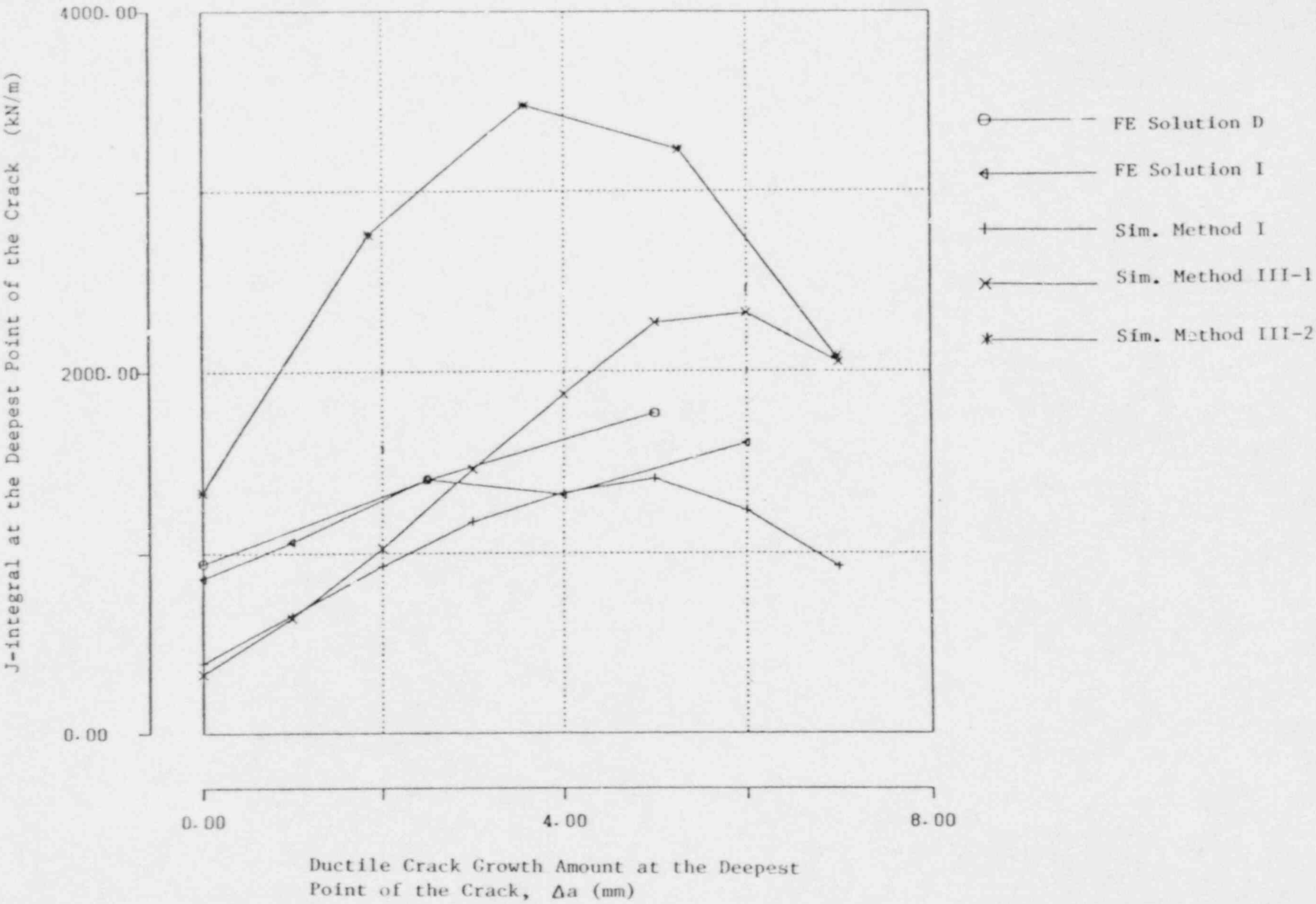


Figure 18 Comparison of J - Δa Relations Obtained by Finite Element Calculations and Simplified Methods

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APPENDIX B

QUESTIONNAIRE FROM THIS MEETING

LEAK-BEFORE-BREAK SEMINAR
QUESTIONNAIRE

Name (optional) ____________________________

Organization (optional) ____

Country (mandatory) _________________________

1. There are some limiting conditions reflecting actual piping failure experiences (IGSCC, erosion, material toughness, quality level etc.) to apply LBB concept. What kind of limiting conditions are you seriously concerned about and why do you think so ?

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2. In case that LBB concept may be conceivably applied to the definition of LOCA and design conditions of engineered safety features, what must be done to obtain an international concensus, technically, politically, etc. if you are positive ? / why do you think so if you are negative ?

3. What kinds of research work should have priority for the progress of leak-before-break technology in general, according to your opinion ?

4. Anything you felt about this seminar or others

APPENDIX C

TABLES GIVING RESPONSES ARRANGED BY COUNTRY

1. There are some limiting conditions reflecting actual piping failure experiences (IGSCC, erosion, material toughness, quality level, etc.) to apply LBB concept. What kind of limiting conditions are you seriously concerned about and why do you think so?	Participant's Country	2. In case that LBB concept may be conceivably applied to the definition of LOCA and design conditions of engineered safety features, what must be done to obtain an international consensus, technically, politically, etc. if you are positive?/why do you think so if you are negative?
1) SCC, thermal fatigue, loading condition. They are limiting conditions due to the difficulty to establish such condition in service.	Argentina	1) LBB concept may be conceivably applied to design conditions, but these conditions must be very well established and known for each case.
2) i) The design and operational practices must provide an extremely high degree of confidence that all known mechanisms of failure have been considered and adequately addressed. For example, if erosion is a possible mechanism for a specific section of piping, a rigorous monitoring program must be implemented to assess wall-thinning etc. ii) I am concerned about extending LBB to secondary side piping (outside container) where it may be difficult to rule out the important failure mechanisms, and as well difficult to establish reliable leakage detection capability. I'm not satisfied that operators walking through a plant provide an adequate degree of confidence in leakage detection.	Canada	2) In the long term, the LBB concept must be expanded to rationalize nuclear safety design and the basis for engineered safety features. However, significant technological developments are still required before extension of the LBB concept can be presented in a regulatory and political framework.
3) All conditions which adversely affect the quality and crack growth are of concern. The most limiting conditions are corrosive influences. In this case LBB can only be applied under very limited boundary conditions. With respect to the control of all parameters which have to be fulfilled, the application of LBB has to be verified for a small quantity of piping within the plant.	Federal Republic of Germany	3) An international consensus is growing. The existing bodies for information exchange at CSNI could be used to enforce the discussion between regulatory people.
4) i) Thermal fatigue under complex local loads like stratification Reasons: not taking into account in the design and very complex thermal loadings. ii) How toughness material with not reliable NDE Reasons: initial defect difficult to follow and critical crack small iii) Small diameter piping. Reasons: Leak detection and non-economic justifications	France	4) LBB is an interesting concept. International consensus is necessary by type of plant PWR/BWR with an association of safety approach on our step by step study especially to define the limits of application and the definition of a new realistic design accident. It's more a safety problem than a mechanical problem.
5) Decrease in the fracture toughness because of some metallurgical effect. Dynamic loading (probably more severe at RT than at high temperature) on large crack in particular and piping with high R/t ratio	Italy	5) There is no need at present to change old procedure. Large LOCA is less severe than small LOCA with stagnation so a PTS analysis of small LOCA encompasses large LOCA condition. After all you may have a leak greater than 5% or 10% or whatever as results of several (not only one) failures (from flanges or valves in particular). With a DEGB based ECCS you are covered.

1. There are some limiting conditions reflecting actual piping failure experiences (IGSCC, erosion, material toughness, quality level, etc.) to apply LBB concept. What kind of limiting conditions are you seriously concerned about and why do you think so?	Participant's Country	**2. In case that LBB concept may be conceivably applied to the definition of LOCA and design conditions of engineered safety features, what must be done to obtain an international consensus, technically, politically, etc. if you are positive?/why do you think so if you are negative?**
6) i) IGSCC - Erosion - Diameter ii) Limiting conditions are the thickness (t) and diameter (D) so to have a major fracture strength.	Italy	6) i) To verify the internals of the reactor pressure vessel (PWR/BWR) at a flow = 100% a pipe. ii) Major segregation of sub-compartments of the reactor buildings. iii) Design supports of RPV-SG-RCP with a flow = 100% a pipe. iv) Independency and redundancy of ECCS trains.
7) i) IGSCC & EAC: can cause sudden failures ii) Welding procedures: are responsible of introducing initial cracks iii) Leak detection systems reliability: can cause delay on cracks detection	Italy	
8) Quality level is the most important. An application of LBB is limited to safety grade piping system. The reason is that these piping systems are required of high fracture resistance, severe control of design, fabrication and installation, and periodic inspections to avoid failure.	Japan	8) At present and in the near future, it is doubtful to apply LBB completely mainly because of difficulty of getting public consensus and still having a loss of fluid. But there may be a chance to reduce system capacity of those important to safety.
9) IGSCC or erosion, corrosion. In Japanese case, LBB concept have only fatigue cracks. So, it's very important that decreasing pipe thickness by erosion or corrosion.	Japan	
10) It is necessary to be thorough about quality control and operation control (including maintenance inspection and control).	Japan	10) Considering the size of the effects of a nuclear accident, it is important to have a consensus with respect to all technology, standards, etc.
11) I am concerned about quality level. Because of materials of piping without defects, which are object to LBB, are manufactured by increasingly high quality level.	Japan	11) I am positive, so I hope that, to obtain an international consensus for LBB concept, international seminar as well as this seminar is opened many times.
12) SCC Reason: Mechanism and growth rate are not clear.	Japan	12) To do more realistic analysis, cooperation in the following fields must be done. i) operation experience (stress, temp, flaw...) ii) fracture mechanics iii) monitoring (leak, material properties...) iv) full size test
13) Sorry, I have no opinion.	Japan	13) What kind of component is LBB concept applied?
14) Human factor. You never know what a man will do. Other than the above, I am concerned about high cycle vibration, which is the most likely cause of pipe breaks.	Japan	14) I am negative to apply LBB concept to them. We should be careful to reduce safety apparatus unless the existence itself is not harmful for human beings.

1. **There are some limiting conditions reflecting actual piping failure experiences (IGSCC, erosion, material toughness, quality level, etc.) to apply LBB concept. What kind of limiting conditions are you seriously concerned about and why do you think so?**	Participant's Country	2. **In case that LBB concept may be conceivably applied to the definition of LOCA and design conditions of engineered safety features, what must be done to obtain an international consensus, technically, politically, etc. if you are positive?/why do you think so if you are negative?**
15) Material toughness after crack initiation, because instability fracture is considered to occur after some crack extension. Assessment of instability and its relation to material properties are key factors for LBB evaluation.	Japan	15) It is necessary to obtain international consensus to unify the evaluation method using standardized material properties such as Jic, J-R curve, Cv data, yield and tensile stress, and so on.
16) i) Erosion: Predictions of deterioration in overall pipe toughness due to erosion are not always reflected in the design. ii) Quality level: I don't know if the range of quality control, and in particular ISI, sufficient for the introduction of LBB.	Japan	16) Because there are problems relating to guaranteeing the reliability of the leak detection. I don't know enough to comment further.
17) I think the limiting condition could be divided into two categories. One is the "basic" material quality which assures toughness and allowable initial defect. The other is the operating or observation measure which assures the limiting condition to be preserved during plant life. In those senses, I'm seriously concerned about material toughness and quality level as well as leak detectibility and ISI.	Japan	17) I think this issue depends on how "Defense in Depth" is interpreted. I want to be positive; however, the catastrophic reactor accident such as "Chernobyl" event made me feel pessimistic on this issue. Because although establishment of LBB is based on the thought that all the failure mechanisms concerning piping are considered, in the field of safety assessment, we should consider the "uncertainty" in nature.
18) Material toughness, erosion and IGSCC, in my observation and opinion, are the main factors. Specifically the material toughness requirements for various operation and transient or fault conditions have to be established and documentation thereof should be specified. Design condition and /or guidelines for operation to avoid corrosion and erosion should be studied and given especially from the aspect of piping routing and layout.	Taiwan, Republic of China	18) The scope of the applications of LBB, and the approach/procedure of the evaluations for approval of LBB applications and required analysis and accuracy of the computer programs used should be further discussed and assessed, for clarifications of the inconsistent approaches and results of some present studies performed by different countries. Verifications of the existing computer code have to be continued.
19) We tend to believe that for safety reasons we ought to reject the applicability of LBB concepts for any known failure phenomenon such as IGSCC or inferior quality of piping as you mentioned. In other words, LBB can only be applied to piping of known quality and proven that toughness is not downgraded by poor quality control at shop or unfavorable service conditions at service. As such, we shall consider IGSCC, erosion/corrosion, water hammer, thermal shocks, water chemistry, material toughness, quality control at fabrication, etc. when applying LBB concept in plant design.	Taiwan, Republic of China	19) At the moment, we tend to agree that LBB shall not be applied in defining LOCA loads or engineering safety features to resist this load. Elimination of pipe whip restraints in our opinion is persuasive to the public on bases of (i) higher plant safety due to plant maintenance/in-service inspection improvements (ii) lower plant construction cost. Of course, the second clause is not a consideration of plant safety. However, the ultimate protection for the public, which is the containment, shall not be compromised in terms of safety and thus shall not apply LBB in design.
20) Any one whose evolution is not clearly known (in a qualitative and quantitative way).	Spain	
21) We do not intend to use the LBB concept for design purposes, but will require that LBB in connection with damage on a case-by-case basis.	Sweden	21) I do not know.

1. There are some limiting conditions reflecting actual piping failure experiences (IGSCC, erosion, material toughness, quality level, etc.) to apply LBB concept. What kind of limiting conditions are you seriously concerned about and why do you think so?	Participant's Country	2. In case that LBB concept may be conceivably applied to the definition of LOCA and design conditions of engineered safety features, what must be done to obtain an international consensus, technically, politically, etc. if you are positive?/why do you think so if you are negative?
22) A large enough part through crack which can become unstable when a sudden load increase occurs. This situation can be brought about by the different parameters (conditions) working together (IGSCC, corrosion fatigue, material toughness, load, etc.). Experience has shown that such conditions exist and the chance of having such a situation in the future is still relevant. Furthermore, I think at the present time we still do not know enough of the complex phenomena and mechanisms involved.	Switzerland	22) i) Technically and scientifically, a lot has still to be done, before this concept can be generally applied. If you cannot apply LBB concept generally, I don't think this concept will be acceptable. ii) The technical acceptance is a precondition for the political acceptability. iii) That is why an international cooperative effort is a necessity to reach an international consensus. iv) The defense in depth concept shall be maintained in all cases.
23) i) Complex flaws/part through flaws with large θ values: may occur if IGSCC, creep or some forms of thermal fatigue are occurring. Potential for flaws in elbows or T's, or other complex components. ii) Uncertainties in toughness: transition temperature behaviour in C and low alloy steels,; variations in weld zone toughness (HAZ; various weld procedures and consumables; strain aging effects; directionality effects (low energy ductile tearing); toughness of components (forged T's; elbows) iii) Uncertainties in loads: overpressure risk (safety/relief valve performance); water hammer; thermal stratification; off-normal system operating conditions; hindrance of thermal expansion.	Switzerland	23) I don't think that LBB is far enough advanced for this application and doubt that it ever will be. There are too many uncertainties. It obviously can be used on a case by case basis for elimination of pipe whip restraints. I think it is more important that we use LBB technology to exclude pipe breaks in piping of "significant size" (size large enough to produce consequent damage or significant contamination (release of radioactivity)). The LBB studies will help to clearly define how we are to achieve this aim -- selection of materials; welding procedures; stress/F.M. analyses; ISI, leak detection, etc.
24) LBB should not be applied to any system where there is a possibility of a long surface crack occurring. Long surface cracked pipe are more likely to produce a DEGB, even with very little system compliance.	U.S.A.	24) Technically, we need to know more about material properties, i.e. dynamic strain-aging, fusion line toughness, and cyclic effects on J-R curve. In addition, a better understanding of finite length surface cracked pipe by improvements in J-estimation schemes. This combined with the material studies will help to show that a surface crack will not fail and cause a break-before-leak.
25) The LBB concept as it is applied in the design should not be used to accept continuing degradation of piping. Therefore, U. S. NRC has specifically prohibited the use of LBB as basis for accepting those degradation mechanisms. As a result, piping suffering active degradation should be precluded from applying LBB.	U.S.A.	25) LBB should be extended to ECCS or containment simply because DEGB was really used as a surrogate to cover other potential LOCA; e.g., SIG meanway cover blowout, pump seal failures, etc. By demonstrating the incredibility of DEGB does not preclude other type OCA from occurring.
26) i) SCC - want to make certain that variations in operational water chemistry do not lead to SCC intergranular <u>or</u> transgranular (IG or TG) that would not be predicted from laboratory studies. ii) fatigue - "Unanticipated" cyclic loading has caused, and continues to cause, fatigue failures in piping systems. The problem is in adequate definition of <u>service</u> loadings. iii) quality - U. S. LBB regulations are applicable only to high-quality piping (ASME Code Classes 1, 2 or equivalent). The problem is making certain that the as-built piping is adequately represented by the design.	U.S.A.	26) It is not clear that LBB should govern the design of the engineered safety features (ECCS for example). Using the large pipe break as the design basis is a convenient way to account for other potential accident scenarios. It assures an adequate design basis for these systems. It is possible that some things, such as environmental qualifications or flooding considerations, may be established considering LBB.

3. What kinds of research work should have priority for the progress of leak-before-break technology in general, according to your opinion?	Participant's Country	4. Anything you felt about this seminar or others
2) i) Tests on "full scale" size pipes and components (large diameters 16 to 24 inches) for validation under actual conditions present in a power plant (of course these are expensive, but it provides the only convincing proof that the position is valid). ii) Tests on complex geometries (elbows, bends, T's, etc.) with longitudinal crack growth on large diameter pipes. iii) Validation of leak-rate models under full system pressure and temp. conditions and these tests must be conducted over long time periods (up to 20-25 hours) to establish steady state conditions.	Canada	
3) Research or fracture behaviour of more complex geometries, crack area and leak rate prediction.	Federal Republic of Germany	3) The seminar was a very fruitful information exchange and left enough time for interesting discussion. It was well organized.
4) i) complete updated word data bank of leak/break in nuclear pipes or similar industry. ii) define a test procedure on simple specimen to determine the fracture behaviour of a pipe and to limit the number of pipe tests. iii) complete all the different programs presented in this seminar.	France	4) Very interesting and productive seminar. A clear comparison of the different approaches of the different countries has to complete this seminar on acceptance or no acceptance of LBB, on limits of application (S.S/CS, diameter, steam line outside inside...) on consequences (intervals, supports...), on different steps of demonstration and margin. May I have a copy of all transparencies used?
5) Those which represent the limiting conditions in point (1).	Italy	5) Very useful in exchange opinions and confront results, questioning researchers who work quite far from each other.
6) Dynamic (seismic) response of cracked piping.	Italy	6) Very interesting, with very good organization. I hope, in a next seminar, to see more experimental results.
7) i) Develop surface cracked pipe J estimation scheme ii) Multiple and complex cracks study iii) Geometry effects on materials fracture resistance iv) High temperature pipe tests and material properties qualification	Italy	
8) Development of precise leak detection system and monitoring system of integrity of pressure boundary.	Japan	8) LBB seminar is beneficial to get an international consensus for not only fracture mechanics but also understanding of each country's circumstances of reactor safety consideration. Therefore, many countries must have a chance to hold international conferences of specific areas so that many people in the host country may participate in the conference.
9) Recently it has been clear of fracture mechanics. But for leak rate test is not complete. I wish more leak rate test.	Japan	9) Thank you very much for CRIEPI. See you again on nuclear conference.

3. What kinds of research work should have priority for the progress of leak-before-break technology in general, according to your opinion?	Participant's Country	4. Anything you felt about this seminar or others
10) Piping integrity should definitely be guaranteed by possessors via ISI; in cases where they fail to do so, the LBB concept should not be applied to nuclear power. Therefore there is a basis for the application of the LBB concept to piping that is always in good condition, but it is necessary to study in all cases under what circumstances piping subject to external force gives rise to what extent of damage.	Japan	10) Insofar as all participants discuss enthusiastically and build an international consensus, it was very effective.
11) I think that research work for increasing quality of materials and examination techniques have priority.	Japan	11) Nothing.
12) Continuous monitoring technique of defects.	Japan	
13) Development of leak detection method, estimation method of fracture mechanics parameter.	Japan	
14) International cooperation in accumulating failure data in operating plants and laboratories.	Japan	14) Japanese should be more open to present their results or the plans of study.
15) International round-robin tests for standardization of expressing fracture material properties and its verification.	Japan	
16) Nothing in particular.	Japan	16) Understanding the content of each country's LBB-related research and the extent of its development was very helpful. However, it was impossible to read any of the diagrams and tables shown by means of the overhead projector, something I hope will be remedied in the future.
17) Probabilistic approach.	Japan	17) Very useful in deepening international understanding in various fields of LBB technology. I felt the field of leak rate verification has developed significantly and widely as well.
18) Material testing, pipe testing and development of more accurate computer programs have to be done. Meantime clarification of LBB requirements (some ambiguous requirements) have to be done as well.	Taiwan, Republic of China	18) If the aspect of the public acceptance of LBB can be discussed in this seminar, then it would be more appropriate.
19) We feel that crack detection technology needs more research to increase the level of confidence that all SC are detected early during ISI and accuracy is high enough to warrant the subsequent crack growth calculation and instability analysis meaningful. We feel the link between detectable leakage rate and critical crack size needs more refinement to a level that assurance of nonoccurrence of catastrophic failure of components is assured by monitoring leakage rate of systems. To apply the LBB concept on a global scale, much research in material testing and component testing and a comprehensive screening criteria of using this concept shall be developed.	Taiwan, Republic of China	

3. What kinds of research work should have priority for the progress of leak-before-break technology in general, according to your opinion?	Participant's Country	4. Anything you felt about this seminar or others
20) From the experimental point of view the test should reproduce actual conditions as close as possible.	Spain	20) Very interesting and constructive.
21) I do not know.	Sweden	21) It has been very stimulating to meet and discuss with researchers and engineers the many questions that are connected with LBB.
22) i) To comprehensively know and understand all the mechanisms and phenomena causing or aggravating cracks in piping. ii) To understand the behaviour of the piping containing a crack under the different boundary conditions and environment. iii) To know all the possible loads that can exist in the plant. iv) Have a tool to adequately assess the safety margins that issufficiently validated.	Switzerland	22) I think seminars like this one and international research programmes like IPIRG are steps in the right direction.
23) i) Definition of appropriate material toughness values (see my answer to question 1) ii) Assessment of various sources of stress and their categorisation with respect to their interaction and influence on crack initiation growth and instability. Appropriate testing is required on pipe components. iii) The complete process of crack initiation, growth and instability should be considered. Particularly for stress corrosion cracking and thermal fatigue. This would allow better understanding of crack shape e.g. complex circumferential cracks; cracks in components.	Switzerland	23) Excellent organisation and hospitality. This includes the visit to JAERI and the seminar. I could not expect a better seminar in this respect. Thank you very much. With respect to the technical content I think that perhaps fewer presentations and more discussion (with active chairmen provoking discussions) would be beneficial.
24) i) Improved surface-cracked pipe analyses under tension and bending. ii) Develop a screening criteria to assess what carbon steels are susceptible to dynamic strain aging. iii) Evaluation of fusion line toughness in stainless steel, carbon steel, and bimetallic welds. iv) Evaluation of cyclic loading from a seismic event on the J-R curve of materials, and a simplified criteria to assess these potential effects.	U.S.A.	24) CRIEPI was a very good host!
25) Material characterization, fracture behavior, and reliability leak detectors should go hand-in-hand; all three areas should have the same high priority.	U.S.A.	25) Generally excellent. However, the leak detection equipment is missing from the program. I think the reliable, accurate leakage detection system is the foundation of applying LBB. The program is loaded with material/mechanical characterization. (The validity of applying LBB to plant piping design is based on the premises that, when there is a leak, the leak would be detected (before the pipe breaks) and appropriate action can be taken to shut down the plant.)
26) i) System performance - simulated service loading and environment to validate our ability to predict service degradation (fatigue, corrosion, erosion corrosion, etc.) ii) Water hammer and other dynamic loading.	U.S.A.	26) This was a very well organized and planned seminar. The hosts and organizers are to be commended for this seminar. Thank you!

APPENDIX D
QUESTIONNAIRE AND RESPONSES FROM COLUMBUS LBB MEETING

WHAT DO YOU THING LEAK-BEFORE-BREAK MEANS?	PARTICIPANT'S COUNTRY	WHAT ARE YOUR CRITICAL CONCERNS IN REGARD TO LEAK-BEFORE-BREAK?
o If a material has been show not to be susceptible to SCC or water hammer, then it can be shown that a through wall crack will grow slowly and at a detectable rate. Therefore, a pipe will leak for a long period of time before the crack will propagate catastrophically fast.	USA	o I think there should be an internationally accepted leak-before-break policy. In the nuclear industry by having countries with opposing policies and views on leak-before-break can create a general public concern which could be detrimental to the entire nuclear industry.
o Pipe failure, for those pipes satisfying certain criteria, will always result in a leak that can be detected, before the pipe catastrophically fails (DEGB).	USA	o Leak detection improvement requirements will require IEEE instrumentation. For near term or operating plants this is going to be costly.
o LBB means we know enough about the pipe material behavior, the pipe fabrication quality and the service loading conditions so that we can be assured that any potential flaw in the pipe would grow through the wall and leak detectably long before it would grow to a critical crack length. The pipe with a detectable leakage crack must be able to withstand an SSE.	USA	o LBB is not justified for all pipes. We all know of some pipes which broke before leaking. We will loose the firm ground we have gained if LBB is applied as a catch-all for all pipes.
o LBB means that there is a high probability ($P_{leak}/P_{break} >> 1.0$, say 10^2), that breaches the pressure boundary will be small (Area of leak/Area of break $<< 1.0$, say 10^{-2}). It does not imply that the probability of a break is small.	USA	o I am concerned that the leak-before-break may become a rationale for eliminating important and effective measures in the defense in depth concept (i.e., inservice inspection) along with poorly conceived measures such as pipe whip restraints. I am also concerned with confusion with LBB and low probability of failure. One should require both LBB and low probability of failure before policy changes are made. LBB provides a defense in depth measure, since the occurrence of leaks will provide a warning and opportunity to implement corrective measures to the elimination of leaks as well as potential breaks. Another concern is that LBB will lead to "overly fancy" criteria and analysis schemes, that are based on speculation and assumptions about crack length, leak rates and leak detection. It appears that LBB requires all of the following: high toughness, low stresses, and short cracks. One must have assurance that each requirement is satisfied, and not place too much focus on any one of the requirements.
o The leak-before-break concept means that, with a high degree of probability, failure of the pressure boundary will be signaled by a detectable leak that will provide ample time to safely shutdown the plant.	USA	o 1. Application to complex geometry piping components; e.g., elbows, reducers, tees and, in particular, fabricated branch connections. 2. Bolted joints 3. Potential of leak blockage by crud accumulation 4. Torsional moments.
o "It's about time!" Sensible approach to getting away from the extremely conservative approach of break postulation with no rational basis. Technically, it means demonstration that pipe failure will proceed from leakage to break under extreme conditions.	USA	o It is also time to recognize that IGSCC is solved as the Japanese have recognized.
o LBB means you can demonstrate via fracture mechanics that a through wall crack of a specified size can be tolerated for a "sufficient" length of time before it will lead to catastrophic pipe failure. During this "sufficient" time period one has the ability to detect this leak, recognize its significance, and take appropriate action to mitigate its consequences.	USA	o 1. Acceptance of current IGSCC mitigation techniques, e.g., IHSI, H_2 water chemistry, so that LBB can be applied to stainless steel piping used in BWRs. 2. Assurance that leak detection systems are adequate and sensitive.
o A defect will grow in a stable manner through the wall of a pressure boundary in such a manner that the contents will leak at a detectable rate long before catastrophic failure could occur.	USA	o 1. Leak detection 2. Shape of defect growth in the presence of complex loading and residual stresses.

WHAT DO YOU THINK LEAK-BEFORE-BREAK MEANS?	PARTICIPANT'S COUNTRY	WHAT ARE YOUR CRITICAL CONCERNS IN REGARD TO LEAK-BEFORE-BREAK?
o For straight, seamless LWR pipes, circumferential cracking predominates. The probability of a stable through wall (leaking) crack is much greater than an unstable through wall crack or double-ended guillotine break.	USA	o Not enough work on LBB of elbows and tees. More work needs to be done on various fatigue failures (e.g., vibration, thermal, corrosion, etc.) of pipes. More leak detection work needs to be done for Class 2 & 3 pipes outside containment. More elastic-plastic fracture handbook solutions are needed for various part-through and through wall geometries.
o Methodology to evaluate the effects of flaws discovered in pressure components during manufacturing, installation and plant operation.	USA	o So far the phenomenon is not completely known to have an exact theory which could be applicable with 100% certainty to all cases discovered. To postulate that you will have always leak before break or to calculate and/or prove that we always have leak before break are different understandings of the same problem.
o Near certainty that stable through wall cracks will always supplant unstable cracks in the component of interest under all conceivable loadings.	USA	o Standard fracture parameters do not apply because of materials and crack geometries involved requiring near total dependence on experimental methods and correlations which make probabilistic statements of "near certainty" nearly impossible. Also, sections and details near rigid connections will need to be tested in full scale for verification of predictions.
o Phenomena of confidently DETECTING leakage of a system in a TIMELY manner. Allowing the systems to de-energize and be repaired well before the pressure boundary can fail catastrophically.	CANADA	o 1. Material values of parent and welds including orientation of crack 2. Crack geometry part-through/through-wall/orientation 3. Use of small specimen data for real life 4. Leak detection sensitivity and reliability 5. The role of inservice inspection 6. Understanding 3-D (mixed mode) fracture, i.e., pipes are neither plane stress nor plane strain 7. Design transients; do they envelope reality? 8. Change of design basis for ECCS/Containment.
o LBB means in itself "leak detectable before break" so that plant may have enough time to take corrective action. But, it may flexibly be interpreted as LBB type of failure mode, that is, leak stable before break with certain conditions regardless of leak detection capability.	JAPAN	o 1. R&D work by own country is still required to establish criteria. 2. Development of alternative criteria always require verification test in spite of existence of state of the art to be capable of predicting failure. 3. Different countries have different scenario including basic conditions. (Basis of postulated crack, safety margin to be considered, etc.) 4 Discussion still exists between mechanistic and non-mechanistic break, although LBB concept has been thought to be sound technical basis 5. Simplified handbook EPFM should be developed for loading conditions to be considered.
o We have a proverb in Japan that "faster one yield, lesser he loses". The LBB could be explained by this proverb.	JAPAN	o The lack of methodology to assess the stability (break load) for surface-cracked pipe for any size and any geometry such as tees, elbows, etc. with reasonable conservativeness.
o One of the fracture manners of material.	JAPAN	o Introduction of fracture mechanics results to nuclear design rule and establishment rational and economical design under the international consensus.

WHAT DO YOU THINK LEAK-BEFORE-BREAK MEANS?	PARTICIPANT'S COUNTRY	WHAT ARE YOUR CRITICAL CONCERNS IN REGARD TO LEAK-BEFORE-BREAK?
o LBB is an area where we should design nuclear piping, especially involved in safety. In that sense, we should know many things about LBB.	JAPAN	o Qualification of leak detection systems.
o Defense in depth with smaller but reasonable margin for pipe break concern.	JAPAN	o Not to have contradiction. The criteria should be so arranged that the design improvement can be taken into account.
o The propagation of part-wall, embedded and through wall defects in longitudinal and circumferential directions in fittings and piping under all postulated conditions to give stable leakage(s) rather than fast ductile/brittle failure.	SOUTH AFRICA	o The use of accurate, qualified analytical methods for calculating the crack sizes and associated leak rates from originally part wall, embedded and through wall defects in cast/wrought austenitic and SG tubing material in Primary/Safety systems of PWR's.
o A condition in which a part wall limited length defect penetrates a pressure boundary to produce a stable through wall flaw which provides noticeable leakage with ample safety margin for remedial action before any further instability condition arises under the postulated design loading.	UNITED KINGDOM	o 360° circumferential defects; IGSCC; treatment of thermal stresses; validating tearing instability analysis beyond the current limits on J controlled growth.
o Non-disruptive failure.	UNITED KINGDOM	o As the evidence of high aspect ratio circumferential (360°) cracks cannot be absolutely excluded, and until inspection techniques are validated to the extent and reliability necessary short falls in LBB technology exist.
o During normal operation or during accidental (SSE) overload a crack may break but not become unstable. During subsequent operating periods, the leak will become detectable before critical conditions be reached for the normal operating conditions and for the accidents specified for design.	FRANCE	o A practical concern: Balance between leak detection ability and inservice inspection: (a) If a reliable leak detection system can be proven, can inservice inspection requirements be lessened. (b) For circuits with no IGSC, no water hammer, little fatigue, can an inservice inspection compensate for a lower leak detection sensitivity (steam line). A fundamental concern: Avoid misunderstandings on the defense in depth concept in relation to the replacement of pipe and jet impingement restraints (which are material) by leak detection which are immaterial.
o LBB means that a pipe will never experience a DEGB, if cracks of a given size are detected by ISI <u>and</u> if leaks of a given rate are detected by a leak detection system. (= defense in depth principle)	BELGIUM	o 1. The thresholds on crack size and leak rate must be determined by a combination of experiments and fracture mechanics technology, for all type of materials, for all type of cracks, for all loading conditions. 2. Then, procedures for application of the LBB approach to all HEL of new and existing plants MUST be set up and contemplated in documents like SRP and BTP.
o A combination of loading conditions, geometric characteristics (including crack size) and materials properties which lead to a stable situation despite the presence of the crack itself.	ITALY	o No response indicated.
o It means that it is possible to pursue a technical approach based on the fact that leak of a flawed piping occurs before breaking due to flaw propagation.	ITALY	o If the phenomenon is really and completely supported by experimental and analytical data like it appears - the only position I see coherent and acceptable is that NRC is beginning to assume.
o It is a very good new approach: The material has enough toughness that an existing crack can be tolerated with a large safety margin with respect to the critical crack length.	ITALY	o The official regulatory position between national safety authorities are different.

WHAT DO YOU THINK LEAK-BEFORE-BREAK MEANS?	PARTICIPANT'S COUNTRY	WHAT ARE YOUR CRITICAL CONCERNS IN REGARD TO LEAK-BEFORE-BREAK?
o The theory, backed-up by experiments, which aims to demonstrate that the flaw size which results in detectable leakage is much smaller than the flaw size which would lead to piping failure. There is no mechanism for developing a large break without going through an extended period during which the crack would leak copiously at a detectable rate.	ITALY	o No response indicated.
o (1) Arrest of crack after penetration; (2) Detectable leak long before break; (3) Loss of load - shut down - before break; (4) Deterministic and probabilistic proof of fail safe behavior.	FRG	o Control of the four conditions that define LBB.
o Every flaw which might exist at the beginning of life (e.g., NDT-assessment) will develop (under all conceivable loads during life) in a way that leak occurs before catastrophic failure (considering also extreme loads), and there is sufficient time and means (leak detection) to find the leak and to take action.	FRG	o Leak detection and localization may be difficult; principal understanding of crack shape development including stable or unstable ligament failure (influence of stress state or constraint on local crack propagation) not well established; influence of residual stresses in welds uncertain; gradients of material properties, anisotropy may be difficult to include.
o Any crack growth which might occur for whatever imaginable reasons remains stable.	FINLAND	o The overall LBB concept is ok but at least the following tasks should be completed before its extensive use: 1) refinement of leak rate predictions, 2) development of improved 3D calculation methods which take into account the inhomogenity of material, 3) development of elastic-plastic materials data bank which includes micromechanism-based statistical reliability limits and the effect of environment, 4) refinement of fatigue crack growth rate predictions with real plant transients in realistic environmental conditions, and 5) experimental verification of the LBB concept in complicated piping sections. International cooperation is required for achieving the above goals.
o The new approach to analyze pipe cracks and to make design provisions against failures.	FINLAND	o Leak size; leak detection; and dimensioning at main component support (DEGB or not?)
o By the term break we mean the event (B) that the pipe is severed into two parts. By the term leak we mean the event (L) that water/steam escape to the environment. By LBB is then meant that the event L precedes the event B. In general, the term LBB is not useful in its literal sense but has to be further precised. One example that we find useful is the following. There should always exist a sufficient margin in time between the event that a detectable flow occurs and the event that an unpermissible flow occurs. The second event should always be preceded by the first one.	SWEDEN	o Numerous. Just to mention a few: leakage rate calculations, role of residual stresses in governing the growth both for fatigue and IGSCC situations.
o Leak means loss of fluid and dynamic loads not greater than those considered as a design basis. Example: In case of a fully protected system, DEGB is a leak.	SWITZERLAND	o Avoidance of large defects by good engineering is better than allowing for such defects and compensating by complex protection devices. First priority: improvement of engineering. Second priority: development of methods for assessment.
o Break through of the crack without significant crack growth in length.	SWITZERLAND	o Nonnuclear applications: avoidance of disintegration of transportable high-pressure vessels.

WHAT DO YOU THINK LEAK-BEFORE-BREAK MEANS?	PARTICIPANT'S CONTRY	WHAT ARE YOUR CRITICAL CONCERNS IN REGARD TO LEAK-BEFORE-BREAK?
o LBB means that the resulting through wall crack is "stable" or is in a "stable tearing mode" for a sufficient long time and the leak rate is sufficiently large (but not too large to cause havoc) ensuring its detectability and locatability by the leak detection systems in use at the plant in question.	SWITZERLAND	o 1. Validation of the tearing instability concept and its application especially for the different existing complex piping systems with their specific characteristics. 2. Comprehensive knowledge of the specific loed and temperature conditions for the specific plant in question. 3. Assurance that the material in question will behave in a sufficiently ductile manner under all expected loading and temperature conditions, so that the tearing instability concept can be properly applied. 4. The relation between real through wall crack length, its opening area and effective leak rate. 5. The behavior of "large" part through wall cracks especially in the circumferential direction under all expected loading conditions. 6. The mechanisms of crack growth with special consideration of their shape changes (a/c-ratio) of subcritical part through cracks, also considering corrosion and other effects. 7. The capability of non-destructive testing methods, e.g., ultrasonic, to adequately and reliably detect and characterize cracks especially in austenitic materials. 8. The development of a leak detection system that can reliably detect and locate leaks with sufficient sensitivity. 9. Last but not least: can corrosion be excluded!

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The third in a series of international Leak-Before-Break (LBB) Seminars supported in part by the U.S. Nuclear Regulatory Commission was held at TEPCO Hall in the Tokyo Electric Power Company's (TEPCO) Electric Power Museum on May 14 and 15, 1987. The seminar updated the international policies and supporting research on LBB. Attendees included representatives from regulatory agencies, electric utility representatives, fabricators of nuclear power plants, research organizations, and university professors.

Regulatory policy was the subject of presentations by Mr. G. Arlotto (U.S. NRC, U.S.A.), Dr. H. Schultz (GRS, W. Germany), Dr. P. Milella (ENEA-DISP, Italy), Dr. C. Faidy, P. Jamet, and S. Bhandari (EDF/Septen, CEA/CEN, and Framatome, France), and Mr. T. Fukuzawa (MITI, Japan). Dr. F. Nilsson presented revised nondestructive inspection requirements relative to LBB in Sweden. In addition, several papers on the supporting research programs discussed regulatory policy. Questions following the presentations of the papers focused on the impact of various LBB policies or the impact of research findings. Supporting research programs were reviewed on the first and second day by several participants from the U.S., Japan, Germany, Canada, Italy, Sweden, England, and France.

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