FRACTURE TESTING IN SUPPORT OF RPV FAILURE PREVENTION IN SWITZERLAND

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Analytical Methodsfor fracture prevention are playing an ever increasing role in RPV safety evaluation in particular for non-inspectable areas.

Currently if critical defect sizes are not sufficiently larger than the dimensions of the non-inspectable areas, full reliance has to be placed on fracture mechanic's analysis.

Based on the maximum tolerated (or maximum actual) fabrication defect size in the area, defect growth to the end of life has to be calculated under conservative assumptions. The so calculated end-of-life maximum defect size has to be sufficiently small compared to the critical defect size.

This paper deals with the application to RPV's of the fracture prevention concept, which has been introduced in an earlier paper by the same authors/9/.It presents the results of the actual materials testing, NDT-methods applied and stress analyses.

In a first step a preliminary fracture mechanics analysis has to be performed using for instance the method of Appendix G in Section III of the ASME-Code.

When evaluating nonductile failure all severe transients, (for example inadvertent operation of the ECC-Systems, etc.) and accident conditions shall be considered in the analysis to determine the most limiting case, together with appropriate load combinations, for the different parts of the RPV. Also residual stresses shall be taken into account for the determination of the local stresses in the analysis. Estimated lower bound values of material characteristics (K_{Ic}, COD etc.) may be used, provided they can be justified.

In a second step the definitive and detailed verification of the protection against nonductile failure for the different parts of the actual RPV has to be performed. This is achieved by comparing the critical with the tolerated defect sizes in the RPV. The critical defect sizes are determined using the local stresses from the definitive stress report, including residual stresses and the measured values of fracture toughness, COD etc., of the actual base material, HAZ and weld deposit. A knowledge of the true local stresses and the corresponding temperatures of the material for the pertinent cases is a necessary precondition. The tolerated defect sizes have to be defined and based upon the specified and applied NDT-Technique, the calibration method and the uncertainties.

The protection against nonductile failure shall be demonstrated over the total life of the plant. Special attention shall be given to fatigue and corrosion assisted crack growth resp. neutron embrittlement. For parts of the RPV, where the accessibility for the inservice inspection is limited, correspondingly higher safety margins shall be incorporated. The safety factor, defined as the ratio of critical to actual tolerated defect size at the end of plant life should range between 5 and 20.

Highlights of methods, results and experience gained during the second step will be presented: COD, and Jc results on CT-specimens ranging from 2T to 10T. The problems and results of fatigue crack propagation rate (da/dN) measurements and their correlation to the behaviour of model vessels will be cited.

1. Introduction

The failure prevention of the RCPB*, above all the RPV, is of utmost importance for the safety of a nuclear power plant. The method used in evaluating a fracture safe design has evolved since the beginning of this decade from the Fracture Analysis Diagram (FAD) according to W. Pellini /2/ to the LEFM approach as introduced by G. Irwin /3/. The ASME-BPVC /4/ has incorporated this LEFM approach for the first time in its summer 1972 addenda. Today the LEFM is a well established method and is being applied extensively.

There are some limitations in the LEFM approach, which encouraged several well known researchers e.g. Wells /5/, Rice /6/, Begley and Landes /7/, Merkle /8/ etc. to seek improvements in the elastoplastic field. The EPFM-Methods attempt to realistically describe the fracture behaviour of the structure in the range of transitional material behaviour. At present the most frequently used methods involve the J-Integral and the COD-concepts.

Following these developments, we in Switzerland have introduced a concept of fracture prevention for the RPV, incorporating EPFM-methods to supplement the LEFM-method. In this way an improvement in the assessment of the effects of flaws on the safety margins of the RPV becomes feasible. To implement this concept a fracture mechanics test-program is necessary. In this test program, the elastoplastic fracture toughness parameters δ and J of the actual material are determined and used in the evaluation (FM-analysis).

This paper describes briefly the concept and its application on RPV's in Switzerland. As the main topic, the results of the FM-test-program and other relevant investigations are presented and discussed. This new concept employing a component and fabrication oriented sequence , is a pragmatic approach applicable to specific RPV's. It differs in philosophy and method from the research program in the FRG/1/,which incorporates broadly based, systematic, fundamental investigations of a great number of different materials, test method and test parameters. Such an approach is only feasible for countries which have a substantial nuclear industry to support it.

2. The two step procedure in the fracture analysis

Irrespective of the particular method used, the following data are required for an analysis against non-ductile failure:

a) toughness data for the materials
b) the stress history of the structure
c) data on existing or possibly undetected cracks in the structure
d) fatigue crack growth data for the materials

An appropriate approach is to calculate critical crack sizes in order to obtain some knowledge on the tolerance of the structure for cracks. Then, crack growth under service conditions is considered and possible end of service life crack sizes in the structure are estimated. Comparison between the critical and the possible crack sizes yields the margin against non-ductile failure of the structure at any time up to the end of its life. On the other hand, postulating minimum values of the safety margin, specifies the tolerable crack sizes and hence the minimum required capability of the non-destructive test method. Cracks larger than

*Reactor Coolant Pressure Boundary

the tolerable ones have to be repaired. If this is not possible the mode of operation must be modified such, that a specified safety margin is achieved.

A preliminary fracture mechanics analysis is required by the licensing authority at an early stage of a nuclear reactor project, long before all the detailed data are available. Therefore a two step procedure has been adopted for the analysis against non-ductile failure /9, 10/.

In a first step, which accompanies the sizing calculations for the pressure boundary, approximate values for the stresses and the properties of the materials are used. The procedures and methods as described by the ASME Code, Sec. III, App. G may be used either by postulating quarter thickness cracks or by employing more realistic crack sizes, if justifiable. The purpose of this usually rather conservative analysis is to identify the relevant loading combinations and the critical areas of the components at an early stage.

The second step must, as far as possible use realistic data. Since these emerge as the project progresses, this step usually takes the form of a long term programme running simultaneously with the design and test work. The analyses are concentrated mainly on the critical loads and areas already indicated by the first step, provided completeness can be proven.

Often,the use of linear elastic fracture mechanics methods in the first step leads to nominal safety margins, which do not meet the specified requirements. This is particularly true for highly stressed areas with residual stresses and emergency loading conditions. In such cases elastic-plastic methods must be used. These take into account the reserve in ductility, which the structural materials show at and above the lowest loading temperatures. The result is a more realistic evaluation of the expected fracture behaviour. As a matter of principle, a more realistic treatment is always preferable to one based on several assumptions with unknown degrees of conservatism. A more realistic treatment provides a better guide to actual safety margin of the structure under operating conditions. In any case, both steps of the analysis have to be performed. In summary, the detailed final analysis of the expected fracture behaviour, on which the acceptance of the design is based, and thus has to be presented before the first pressurisation, consists of the following parts:

- Assessment of existing cracks and crack-like defects in the components by means of suitable non-destructive testing. Assessment of the detection limit of the testing technique applied, to indicate an upper bound for non-detectable defects.
- Determination of fracture toughness parameters and fatigue crack growth data from the actual materials in the range of operating temperatures.
- Calculation of critical crack sizes for the location and orientation of real or assumed cracks in the component and their comparison.
- Specification of recommended safety margins and assessment of the safety factors resulting from the analysis (see table 1). Introducing reasonable variations of the input data into a nominal safety analysis, as described here, indicates that rather large safety factors should be used and that the kind of method and data determines the recommended minimum safety margins /10/.

3. Some observations and results for quasistatic loading

In elasto-plastic fracture mechanics testing it is important first to define criteria. Based on experimental evidence, such as the typical load displacement curves of Figs.1-4, the following aspects may be considered.

The initiation of crack extension from an existing crack represents a safe force and displacement lower boundary; smaller forces or crack opening displacements do not produce any significant effect.

Maximum load and corresponding displacement of a test specimen may serve as an upper boundary. However, if ductile tearing is present up to full separation, this criterion can also become conservative relative to the vessel behaviour under certain conditions. For example, in a thick walled pressure vessel the necessary work to produce an equivalent crack opening displacement in the ductile region will become much more than in the specimen, see Fig. 1 /11/. On the other hand, higher stiffness or constraint (three dimensional tension for example) may lead to embrittlement effects especially in the ductile-brittle transition temperature range.

Different examples of material behaviour as a function of temperature are represented in Figs. 2, 3 and 4. They represent the behaviour of 10T CT specimens of RPV steel A, similar to A 508 cl. 3 /12/. At -42 ^{o}C, see specimen H4, initiation of crack extension leads immediately to fast (brittle) fracture; the linear elastic fracture toughness is low, no plastic deformation can be observed on the load-displacement record. Specimen H1 at 0 ^{o}C exhibits initiation at a somewhat lower load P_i than the maximum load P_{max}. P_Q, defined by the 5 % secant method is significantly less. The steel exhibits fully ductile behaviour in specimen H2 at 71 ^{o}C; the curve continued to fall slowly after passing P_{max} until full separation of the specimen.

Ductile fracture involves extensive plastic deformation in this type of steel. Heavy wall components consume very high amounts of strain energy per crack growth increment. Material separation, if any, will be slow and will soon come to a standstill* In this extreme case no fracture mechanics is necessary if normal fabrication and control procedures are applied, i.e. no extreme defects or material deterioration are present.

The variation in load P form P_Q through P_i to P_{max} does not reflect the full crack tip stress relaxation capacity of the steel at 71 ^{o}C, which is better related to the absorbed energy or the crack opening displacement values. This is illustrated in Fig. 5 where CT-specimens, cut out of actual component materials (Steel 22 NiMoCr 37) originating from different steelmakers and weldments, were tested at 15 to 20 ^{o}C and showed no further increase in K_{Ii} above δ_i 0,24 mm. The corresponding representation of valid fracture toughness and initiation stress intensity values as a function of temperature are given in Fig. 6.

K_{Qi}-values, based on COD or on J-Integral, are to be seen for steel A in Table 2 /13/. Differences for initiation-based fracture toughnesses show scatter, which is surprisingly small when considering the variety of methods applied.

With increasing temperature the yield points tend to decrease. Also P_i and P_{max} will fall,

*see also HSST program, Series 2: Intermediate Test Vessels V-3, V-4 and V-6, Rep. ORNL-5059, Fig. 3.20 and Fig. 3.34.

as indicated in Table 3 for 6T CT specimens of steel A of which H7 was already fully ductile at 31 °C. With still higher temperatures a further decrease is found also on different steels.

The above drop does not necessarily represent an effective decrease in fracture initiation resistance, as the COD at initiation (δ_i) is showing. COD at P_{max}, (δ_{max}) was also constant in the temperature range observed. δ_{max} and P_{max} however are very much dependent on the stiffness of the structure and would not be representative for a vessel of the same wall thickness as the specimen. Therefore methods which apply criteria above the initiation of the crack extension point in the load-displacement diagram may be conservative in cases of full ductility, but not conservative in the transitional range. In actual cases the initiation criteria were always sufficient to prove safety against non-ductile failure both from the viewpoint of applicability of such criteria as well as from the viewpoint of meeting certain minimum values.

On the other hand, at the initiation of crack extension, size effects on COD (and therefore also on J) seem to be negligible within limits. This was further suggested by comparing 30 mm thick 2T CT geometry specimens and surface precracked wide plate of the same thickness (unpublished Sulzer results).

Fractography of H1 specimen of steel A /14/ tested at 0 °C showed interesting features, see Fig. 7. A cut perpendicular to the slit and fatigue fracture plane at mid-thickness revealed the following regions: fatigue precrack, quasistatic subcritical crack growth and fast fracture.

The presence of both mode I and mode II fractures can be observed at the end of the stable crack growth. The applicable criterion for the initiation of crack extension is a mode I criterion, later at the beginning of instability a mode II is governing.

The result of a pronounced yield and deformation process can be seen in Fig. 8. The half δ_i value is 0,24 mm, which is in good agreement with the conservatively extrapolated value of 0,45 mm for the total δ_i at 0 °C, according to the ASK procedure /15/.

4. Dynamic crack initiation fracture mechanics

Increasing deformation rate can produce embrittlement of steels. So checking the fracture mechanics parameters at the highest possible deformation rates of the actual component seems to be advisable. However, local actual deformation rate is dependent not only on the rate of external loading, but also on geometry. The same loading rate may result in a low deformation rate on a notch and crack free specimen, whereas in the presence of a sharp notch or crack plastic deformation would concentrate in the small volume near the notch root or crack front and hence produce a significantly higher total deformation rate.

Appropriate loading rates should be applied on specimens and should include the most unfavourable configuration, i.e. the sharpest and deepest notches or, more generally, the most effective stress concentrators to be expected in the structure. Possible existing locally embrittlement areas should be suitably represented. Such specimens will usually be of the large compound type, tested rather slowly at the lowest loading temperature of the structure, similarly to the Wells wide plate test ("adapted testing procedure").

Another solution to detect minimum initiation fracture toughness parameters is to employ small, Charpy-type specimens. The weakest areas can be searched and investigated by stepwise checking of heat affected zone properties at different distances from the fusion line. Following ASTM E 399 procedures a standard three point bend specimen may be employed. An impact velocity below the usual 5 m/s represents an upper limit strain rate for RPV conditions in the precracked Charpy-type specimens (limit value testing). If a maximum possible local strain rate in the structure is known, a less conservative approach becomes possible: The bending rate can be adopted to the maximum estimated strain rate at the notch root or at the fatigue crack front in the structure. With appropriate instrumentation according to /16/ and force recording as a function of time or function of deflection, the limiting dynamic fracture initiation toughness may be defined. Such values are compared to static ones in Fig. 9 /12/.

If dynamic initiation as a limiting condition is acceptable, the quasistatic and dynamic initiation testing may be regarded as sufficient. Additional, appropriate crack propagation and arrest testing may give supporting evidence in this respect. Crack arrest testing has to take into account notch or defect geometry, component shape and load over time functions as parameters.

5. Fatigue crack propagation

Crack propagation rates da/dN are measured usually on standardised specimens. Crack propagation in the component is calculated under the assumption, that da/dN is the same in the component as in the specimen.

Environmental conditions are often simulated. However, variations in the state-of-stress history caused by variations in the growth and shape of fatigue cracks are generally not considered. Fig. 10 shows the change in crack shape in both a plate and in a vessel. The assumption of constant crack shape is conservative at first glance; a relatively quicker depthwise growth compared to surface propagation seems to enhance the possibility of leak-before-break failure. However, equivalent critical crack geometries may be reached in special cases with fewer load cycles than indicated by the assumption of constant shape growth.

6. Conclusions

An additional step to prove fracture safety using actual component material, stresses and defect size data has been proposed before. Results of material characteristics show higher values than usually found.

Fracture toughness parameters, measured at the lowest loading temperatures on specimens, cut from actual RPV component materials are significantly higher at initiation of crack extension, than valid linear elastic or 5 % secant K-values.

Whilst load at initiation and hence K_{Ii} do not increase above a shelf value given by plastic deformation, COD and J show increasing values up to a still higher level. (These latter values are high enough to prove safety against crack extension under quasistatic loading for BWR and PWR vessels of material with good toughness and sufficient ductility).

The apparently wide range of relative insensitivity of initiation based COD and J to size effects facilitates both testing and the application to actual structures.

Higher strain rate effects on initiation may be investigated by adapted testing or limit value procedures; some results were shown.

Appropriate crack propagation, crack arrest testing and appraisal of structures will become necessary only if initiation cannot be excluded under the given service conditions.

Fatigue crack propagation testing and prediction should include not only environmental simulation, but also take into account possible fatigue crack shape change effects.

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References

/1/ Programm Forschung zur Sicherheit von Leichtwasserreaktoren 1977-1980. Der Bundesminister für Forschung und Technologie. Bonn 1978.

/2/ PELLINI, W.S., Puzak, P.P., "Fracture Analysis Diagram Procedures", WRC-Bulletin Nr.88, 1963.
PELLINI, W.S.,"Advances in Fracture Toughness Characterization Procedures", NRL-Report 67/3, 3 April 1968.
PELLINI, W.S., "Principles of Fracture-Safe Design, Part I", "Welding J." WR 50 (1971), page 91 ff.

/3/ IRWIN, G.R., "Analysis of Stresses and Strain near the End of a Crack", Journal of Applied Mechanics (Trans. of the ASME) 24, 1957, page 361.
IRWIN, G.R., "Fracture. Handbuch der Physik, page 551 ff, Berlin 1958, Springer-Verlag.

/4/ ASME Boiler and Pressure Vessel Code. Section III. Appendix G: "Protection against Non-ductile Failure"Summer Addenda 1972.

/5/ WELLS, A.A., "Unstable Crack Propagation in Metals, Cleavage and Fast Fracture", Proceeding Crack Propagation Symposium, Cranfield, Sept. 1961.

/6/ RICE, J.R., "A path independent Integral and the Approximate Analysis of Strain Concentration by Notches and Cracks", Journal of Applied Mechanics (Trans. of the ASME) 35, June 1978.
RICE, J.R., "Mathematical Analysis in the Mechanics of Fracture", Chapter 3 in H. Liebowitz (Ed.), Fracture, Bd. II, Academic Press, New York/London, 1968.

/7/ BEGLEY, J.A., and LANDES, J.D., "The J-Integral as a Fracture Criterion", ASTM STP 514, 1972.

/8/ MERKLE, J.G. and CORTEN, H.T., "A J-Integral Analysis for Compact Specimen, Considering Axial Froce as Well as Bending Effects", Trans. ASME, Serie J, Journal of Pressure Vessel Technology 96, pp. 286-92 (November 1974).

/9/ NJO, D.H., VARGA, T., PRANTL, G., "The concept for the protection of the RPV against non-ductile failure as it is applied in Switzerland", IAEA Specialists Meeting, Dec. 1975, Winterthur, Switzerland, IAEA-189.

/10/ PRANTL, G., VARGA, T., NJO, D.H., "Some aspects relating to the reliability of steel RPV's", IAEA Symposium, Oct. 1977, Vienna, Austria.

/11/ WELLS, A.A., Private Communications 1974.

/12/ JUNKER, M., VARGA, T., and M. SHAKESHAFT, "Bruchmechanische Untersuchung an einer Sonderschmelze", Sulzer Report to NOK, Nov. 1976.

/13/ SAHGAL, S., VARGA, T., JUNKER, M. and M. SHAKESHAFT, "Pressure Vessel Fracture Safety Investigations using as Criterion the Onset of Crack Propagation", Paper G 1/4, 4th SMIRT Conference, San Francisco 1977.

/14/ VARGA, T. and B. WALSER, "Plastisches und pseudoplastisches Bruchverhalten", (to be published).

/15/ VARGA, T., PRANTL, G. and D.H. NJO, ASK-AN-220: "Test Procedure for C.O.D. Determination". (Rev. 1, October 1978) Abteilung für die Sicherheit der Kernanlagen, Switzerland.

/16/ VARGA, T., JUNKER, M., NJO, D.H. and G. PRANTL, ASK-AN-425: "Proposed Method for Instrumented Precracked Charpy-Type Tests", (Rev. 1, Dec. 1978) Abteilung für die Sicherheit der Kernanlagen, Switzerland.

TABLE I Recommended safety factors between critical crack sizes and tolerated (existing or postulated) cracks or crack-like defects.

Toughness data / Stress data including thermal and residual stresses	Lower bound values e.g. K_{IR} (ASME)	Typical values of equivalent materials e.g. K_{Ic} (ASME)	Data from the individual materials at the relevant temperatures
Stresses according to design rules (sizing calculations)	≈ 5 FIRST STEP	≈ 20	
Stresses from a detailed realistic stress analysis			≈ 2 SECOND STEP

TABLE II

Stress intensity values computed in accordance with J-Integral and COD values.

Temperature °C	Specimen Nr	K_Q-Values $N \cdot mm^{-3/2}$			
		K_{J_i} *	$K_{J_{max}}$ *	K_{δ_i} **	$K_{\delta_{max}}$ **
− 42	H4(10")				2'518
0	H1(10")	10'355	10'895	[illegible]870	11'400
+ 71	H2(10")	12'218	14'363	10'940	13'400
0	H6(6")	8'831	[illegible]	[illegible]	11'700
+ 31	H7(6")	12'379	14'384	12'690	14'760
+ 70	H5(6")	11'467	13'571	[illegible]390	11'060

* according to J. Merkle and H. Corten

** according to ASK-AN-220

TABLE III

Critical load and COD values as measured.

Temperature °C	Specimen Nr	Load			COD	
		P_Q kN	P_i kN	P_{max} kN	δ_i mm	δ_{max} mm
− 42	H4(10")	–	–	1'070	–	0.025
0	H1(10")	3'870	4'920	5'080	0.45	0.60
+ 71	H2(10")	3'530	5'220	5'470	0.60	0.90
0	H6(6")	1'480	2'060	2'260	0.40	0.60
+ 31	H7(6")	1'500	2'500	2'570	0.74	1.00
+ 70	H5(6")	1.510	2'330	2'450	0.75	1.04

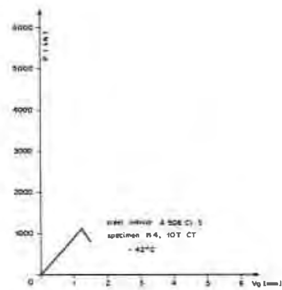

Fig. 2 Load-displacement diagram of specimen H4, 10T CT, steel similar to A 508 cl. 3 at -42 °C.

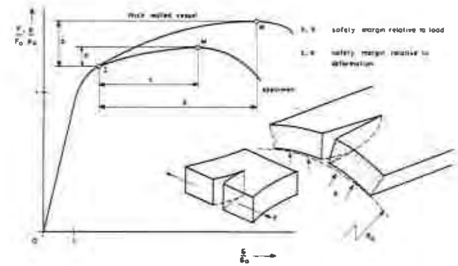

Fig.1 Schematic presentation of the safety margins of a specimen and a vessel (same material).

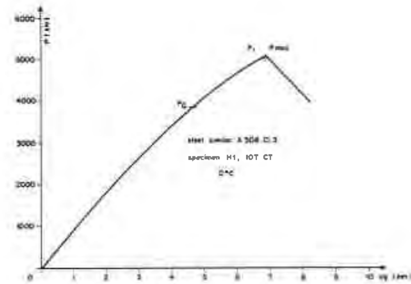

Fig. 3 Load-displacement diagram of specimen H1 at 0°C.

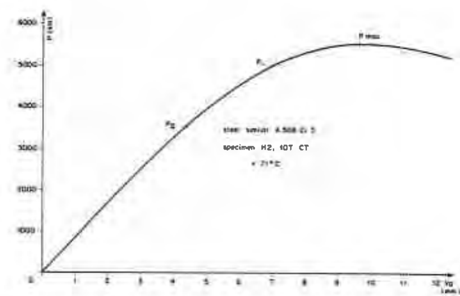

Fig. 4 Load-displacement diagram of specimen H2 at +71 °C.

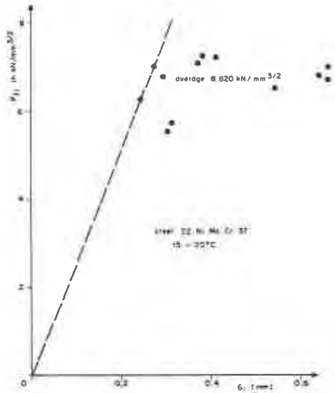

Fig 5 Stress intensity at initiation in function of COD at initiation (Extrapolation method according to ASK-AN-220), Steel 22 NiMoCr 37 forgings, weld metals and HAZ at 15 to 20 ^{o}C, 2T to 4T CT specimens, different steelmakers, heats and weldments

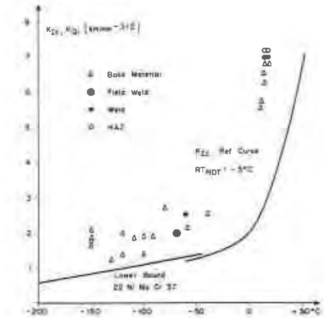

Fig 6 Valid fracture toughnesses resp stress intensities at initiation over temperature, Steel 22 NiMoCr 37 forgings, weld metals and HAZ (low temperature values: KWU tests) 2T to 4T CT specimens

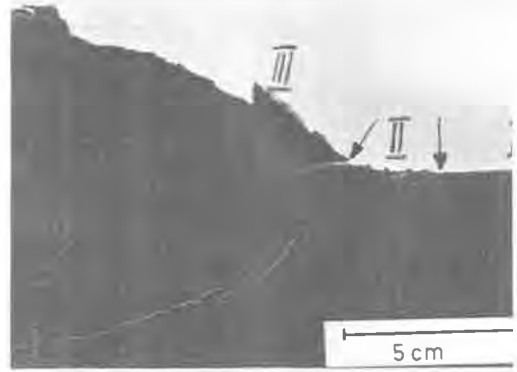

Fig. 7 Cut perpendicular to the slit and fatigue crack plane, mid section of specimen Nl, tested at 0 ^{o}C. Regions: I fatigue precrack, II quasi-static subcritical crack growth, III fast fracture

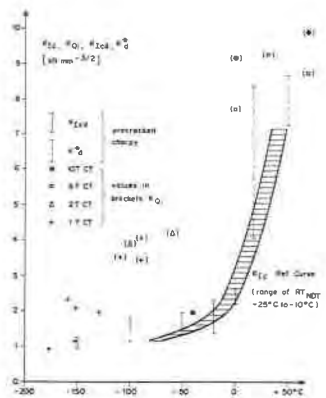

Fig. 9 1T to 10T CT specimens valid linear elastic fracture toughness K_{Ic}, stress intensity at initiation K_{Qi}, resp. precracked Charpy-type (ASK-AN-425) linear elastic K_{Icd} and equivalent energy K_d^* values, steel similar to A 508 cl. 3

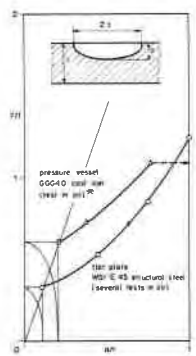

Fig 10 Measured fatigue crack growth shapes in flat plate specimens and a cylindrical pressure vessel with longitudinal crack orientation (the latter by courtesy Sulzer Bros. Ltd., Mr. W. Stumpp)

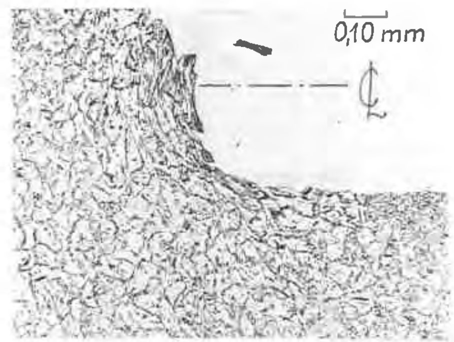

Fig. 8 Very strongly deformed limited volume at the front of the blunted fatigue precrack. Half δ_i value is about 0,24 mm. Stable crack growth started from here