



WORKING MATERIAL

Methodology for Pressurized Thermal Shock Evaluation.

**Proceedings of the IAEA Specialists Meeting held in Esztergom, Hungary
5 - 8 May 1997.**

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FOREWORD

This IAEA Specialists Meeting on "Methodology for Pressurized Thermal Shock Evaluation" was held on 5-8 May 1997 in Esztergom, Hungary

The International Organizing Committee for the meeting consisted of:

- | | | |
|---|---------------|--|
| - | F. Gillemot | Head of the Organizing Committee |
| - | V. Lyssakov | Scientific Secretary of the Meeting |
| - | L.M. Davies | LMD Consultant, UK, Chairman of the Meeting |
| - | J.G. Blauel | Fraunhofer Institut für Werkstoffmechanik, Germany |
| - | M. Brumovsky | Nuclear Research Institute, Czech Republic |
| - | Y. Dragunov | OKB "Gidropress MAEI, Russian Federation |
| - | J. Gadó | Atomic Energy Research Institute, Hungary |
| - | S.-Y. Hong | Korea Electric Power Corp., Rep. of Korea |
| - | K.B. Yoon, | Chung-Ang University, Rep. of Korea |
| - | Z. Kiss | NPP Paks, Hungary |
| - | D. Miannay, | IPSN/DES/SAMS, France |
| - | L. Maróti | Atomic Energy Research Institute, Hungary |
| - | C.E. Pugh | ORNL, USA |
| - | C. Rieg | EDF/Septen, France |
| - | S.T. Rosinski | EPRI, USA |
| - | W. Server | Consultant, USA |
| - | I. Suzuki | JAPEIC, Japan |
| - | L. Vöröss | Nuclear Safety Authority, Hungary |

The meeting was held within the scope of activities of the International Working Group, recognizing that the importance of the PTS phenomena and advances in the subject require regular information exchange in this field.

The purpose of the meeting was to provide an opportunity to exchange information as well as new results in research and development, concentrating on the total PTS calculation and including PTS evaluation and application in RPV life time and integrity assessment. The papers presented at the meeting covered problems of thermohydraulics, RPV temperature-stress fields calculations, fracture mechanics approach to integrity assessment as well as discussions on PTS modeling, general procedures for RPV life assessment and mitigation methods other than RPV annealing.

The Chairman of the meeting was Eur. Ing. Academician L.M. Davies. The meeting was attended by approximately 70 participants from 18 countries and 3 international organizations. The meeting programme was divided into 9 sessions, concluded by a round table discussion.

The Programme of the meeting and the list of participants are presented in the Appendices Section.

The Session Chairmen provided summaries of discussions during their sessions and, where

appropriate, drew up on any conclusion and recommendation that had been openly discussed at the round table sessions on 7-8 May 1997. Sub-sessions for round table discussions were led by Messrs. W. Server and D. Miannay and Messrs J. Blauel and C. Pugh.

The meeting ended with general consideration of the topic and elaboration and adoption of conclusions and recommendations.

Special thanks were given by the participants to the Local Organizing Committee for the excellent preparation of the meeting.

Opening Session

5 MAY, MONDAY

Opening Ceremony

Gillemot, F.; Davies, L. M.; Vöröss, L.; Kiss, Z.; Lyssakov, V.

Gueorguiev, B. Lyssakov, V. Davies, L. M.	IAEA	IAEA Activities in the Field of NPP Life Management
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IAEA Specialist's Meeting on
PRESSURIZED THERMAL SHOCK
Esztergom, Hungary, 5-8 May

OPENING CEREMONY

1. Acad. L. Myrdinn Davies the meeting Chairman opened the Specialist's Meeting on the behalf of the IAEA IWG LMNPP.

2. Dr. Lajos Vöröss - head of the Hungarian Nuclear Safety Authority- greeted the participants. He wished successful work and a pleasant stay in Hungary for the participants.

3. Dr. Zoltán Kiss -chief engineer of NPP Paks- expressed his best regards to the participants and described the NPP Paks.

4.) Dr. Ferenc Gillemot -the head of the International Organizing Committee- welcomed all participants on behalf of the host institute, the Atomic Energy Research Institute of Hungary and its director Dr. János Gadó.

He emphasised the role of the IAEA IWG LMNPP in increasing the safety of the nuclear facilities in his country and for the technical knowledge on the field of Life Management and Ageing assessment collected by Hungarian specialist's in course of many meetings, training courses, co-ordinated research programmes etc.

He thanked the efforts of the International and Local Organizing Committee members in preparation the meeting, and finally he introduced the participating members.

5.) Mr. Viacheslav Lyssakov the Scientific Secretary of the IWG LMNPP described the activities of the working group by presenting the paper:

B. Gueorguiev, V. Lyssakov, L. M. Davies: "IAEA Activities in the field of NPP Life Management".

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Chairman's Opening Remarks Good morning and welcome.

I would like to thank our Hungarian hosts for arranging this IAEA Specialist meeting

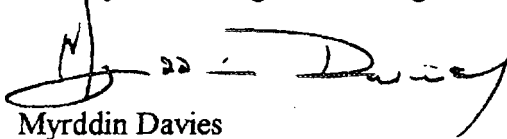
It is a pleasure to be here in Hungary again and to be working with many long-standing friends.

The subject that has brought us together for this IAEA Specialist Meeting is that of Pressurised Thermal Shock to reactor pressure vessels. There has been a lot of activity in this area, particularly in the US.

PTS encompasses many disciplines, but the aim is to assess structural integrity. Robert Gerard from Belgium has shown that there are differences in approach in the world. But that feature is not necessarily a 'bad' thing if the results lead to progress. However it will be useful to draw conclusions and make recommendations for the international community from this Specialist meeting. Session Chairmen will be expected to summarise the main conclusions from their session and to note any recommendations. In the final session we can see if there is an overlap and also to see if a more general consideration leads to any further recommendations.

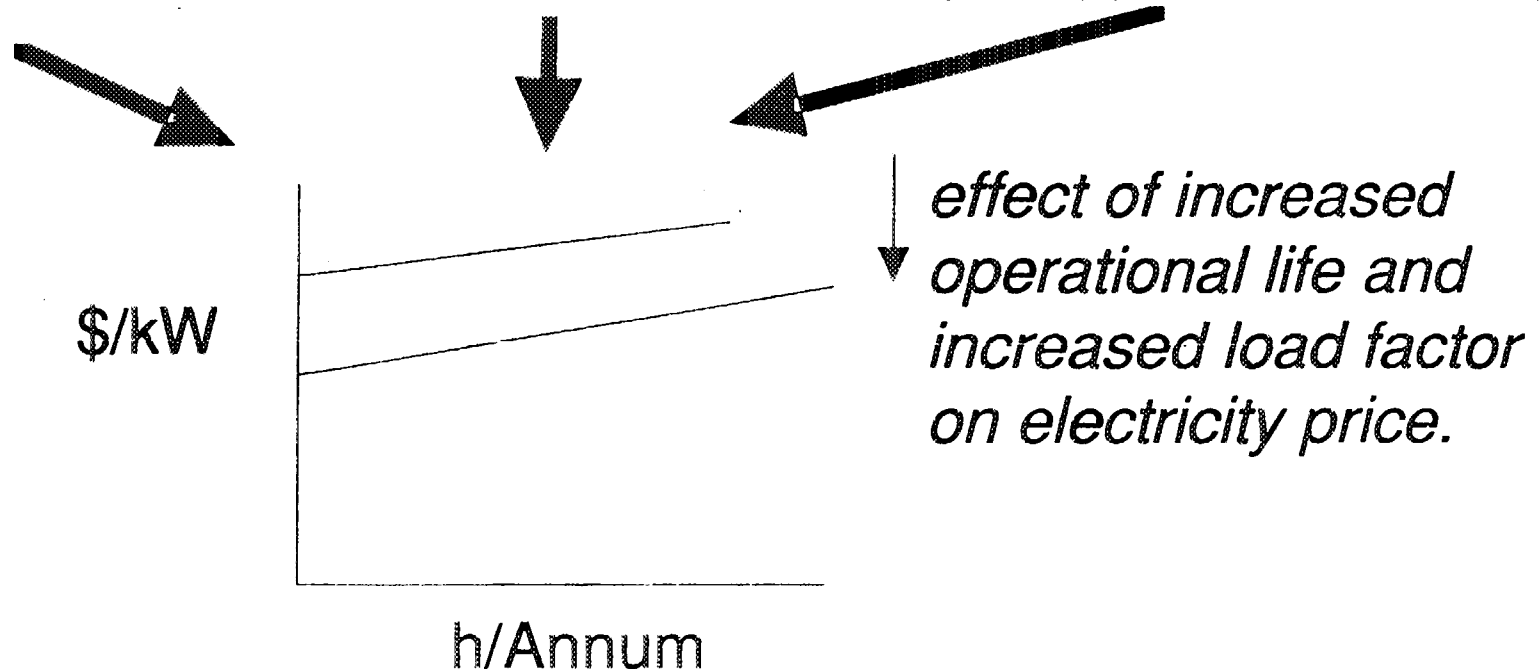
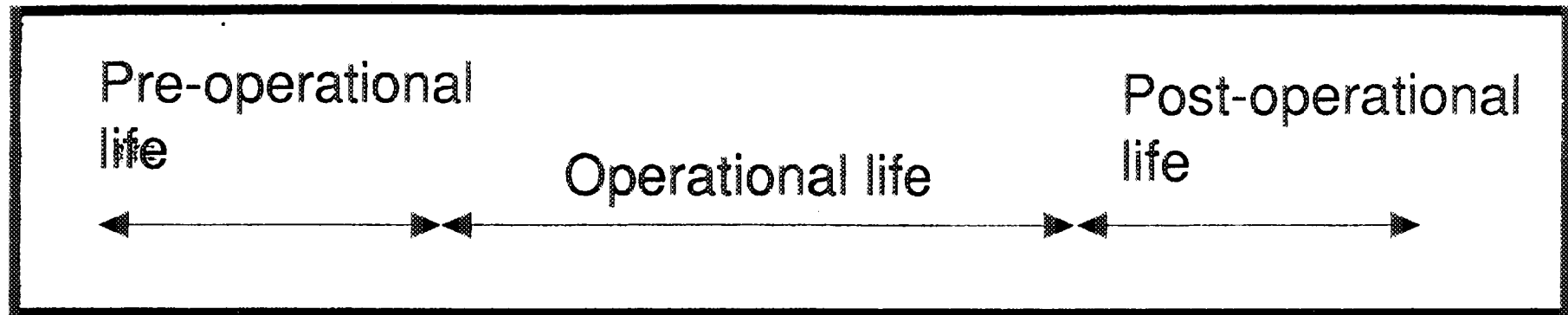
I should perhaps make a comment about a particular aspect of terminology. For this I have described *plant life* in Figure 1 as "green field to green field". The analogy with human life is obvious. We will be talking about *operational life* at this meeting. This is the period where the pre- and post-operational costs are recovered. These costs include, for example, the authorization, design, procurement, manufacture and construction costs. It also includes plant shutdown, storage/reprocessing costs, decommissioning, waste disposal etc.. It follows that if the operational life is extended and the pre- and post- costs already accommodated then the price of electricity should decrease and this is a significant motivation for this work. In some cases the converse may be true where *operational plant life assurance* may be the objective.

I trust you have a good meeting.



Myrddin Davies

PLANT LIFE is 'green-field' to 'green field'



Remember ALL costs are recovered from the operational life



IAEA Activities in the Field of NPP Life Management

B. Gueorguiev, V. Lyssakov, L.M. Davies

INTRODUCTION

Nuclear energy contributes largely to the electricity supply in the world today. In order that the expansion of nuclear power continues to be competitive and an environmentally benign and viable option for electricity generation, the continued demonstration of safe and reliable operation of existing NPPs during their design life time is imperative. Thus, NPP life time management is an important task for nuclear power plants.

The susceptibility and tendency of NPPs to undergo performance and property changes during their operational lifetime, causes a growing concern regarding technical and economic aspects of operating them reliably and safely.

Changes in physical and mechanical properties of NPP structures and components due to ageing and degradation play an important role in lowering safety margins and therefore in the achievement of the reliable operation of the plant as a whole.

According to the IAEA PRIS (Power Reactor Information system) as of December 1996 there are 442 NPPs in operation around the world out of which 183 have reached over 15 years of operation. Figure 1 shows the distribution of two of the most wide spread reactor types, PWR and WWER, by age.

More than 30 countries have nuclear power plants in operation or under construction and in 14 countries the share of nuclear generated electricity exceeds 30%. Future energy and electricity consumption will depend on a number of factors. Population growth, industrial development and enhancement of the quality of life together with re-capitalization of electricity generating plant will be driving forces for increasing demand.

The major goal of every national nuclear programme is to safely and economically operate the NPPs during their design life. The second goal for utilities/operators could be to extend design life to the optimum technical and economical life using life management programmes with scientific and technological backgrounds.

Though the NPP life management programme can have some general features it should be taken into account that every NPP has a unique design and therefore specific feasibility study related to a concrete plant, should be performed within the adopted PLIM strategies.

There have been difficulties with the terminology used in these studies. Fortunately, the term PLEX (Plant Life Extension) is now rapidly disappearing from use. While there are some national programmes which look at the period beyond the licensing life, generally plant lifetime is now taken as the technical life of the plant. But this may be overly simplistic approach because the current list of 'life' definitions includes:

- Technical
- License
- Re-license

- Economic
- Political
- Financial
- Design

The assessment of 'lifetime' usually involves more than one of these definitions. In this presentation 'lifetime' means the technical life of the plant.

The IAEA has established programmes in the field of Nuclear Plant Lifetime in the Division of Nuclear Power and the Fuel Cycle (NEPF) and also in the Division of Nuclear Safety. In the Division of NEPF the International Working Group on Life Management of Nuclear Power Plants carries out its activities within the IAEA Project A2.03 "Nuclear Power Plant Life Management". Activities under this project have produced a wealth of information by organizing specialists meeting, preparing technical publications on related topics and arranging co-ordinated research programmes with good results. The most recent development is a database which has been developed and is being maintained.

IWG-LMNPP

Its main objective is to provide Member States with information and guidance on design, materials, testing, maintenance, monitoring and mitigation of degradation with regard to major components, with the aim of assuring high availability and safe operation of NPPs.

Results of the IWG activities are being revised during the IWG regular meetings almost every two years. Future tasks could be easily reoriented towards serving more directly the needs of the end-users and with a view of assisting major decisions concerning main plant components refurbishment/replacement and cost effectiveness assessment of continued operation versus earlier retirement.

Attention to emerging technology issues is continuing through promotion of specialists meetings, technical committee meetings and co-ordinated research programmes as advised by the IWG-LMNPP members at its regular meetings. Within the scope of the Project A2.03, experts' advice and consultancy is also explored by the Secretariat.

In accordance with the Terms of Reference for the IWG-LMNPP which were revised at the last IWG regular meeting in September 1995 and approved by the Director General, the scope of the IWG activities include the following aspects:

Design
 Material
 Fabrication
 Monitoring, testing, inspection and databases of their results
 Information on service and test conditions
 Degradation mechanisms, their significance and mitigation
 Assessment and means of plant life management

Within these areas certain topical priorities have been set up. These are as given below (as approved at the IWG meeting 30.08 - 01.09.1995):

1. RPV Integrity

- radiation damage
- fracture mechanics
- optimization of surveillance programmes
- annealing and other mitigation methods
- material databases
- surveillance databases
- PTS analysis
- inspection procedures validation/qualification
- monitoring processes of material degradation and plant operation
- pressure tubes integrity.

2. Reactor Internals Operation and Integrity

- corrosion, IASCC
- wear
- NDE and inspection qualification
- irradiation ageing
- replacement and repair.

3. Primary Circuit Integrity and Operation

- corrosion/erosion and water chemistry
- fatigue
- material ageing and its monitoring
- monitoring of loads and water chemistry
- inspection procedures validation/qualification
- LBB concepts and leak monitoring
- repairs.

4. Steam Generator Life Management

- corrosion and water chemistry
- wear/mechanical problems (internals)
- inspection procedures validation/qualification
- monitoring
- replacement/repair.

5. Secondary Circuit

- erosion/corrosion
- water chemistry (optimization and monitoring).

Operating experience has shown that NPPs life time is affected by components degradation and ageing and therefore highlighted the need to develop the methodology allowing for an improvement in the understanding of these processes.

Better understanding in turn will provide a possibility to manage ageing effects in a timely and planned way and also to create a strategy in the NPP Life Management with the aim to achieve economic viability of the plant while observing necessary safety and operational margins. These processes are schematically presented in Figure 2. The IWG-LMNPP activities can be easily traced within this scheme. They mainly look at technological and phenomenological aspects of ageing and degradation processes and take into consideration practical advice on maintenance, mitigation, repair, replacement and refurbishing.

Recent considerations of the scheme by the IWG provided a recommendation that economic aspects of those countermeasures should also be a point for attention.

To give a clear picture of the IWG-LMNPP involvement in the PLIM studies it would be useful to look at the list of its activities during the recent past and for the period of 1997-1998. The latter period is adopted by the IAEA biannual programme and is under implementation now. Also included in the activities is the organization of Training Courses for developing countries. The following table shows the list of Meetings and Training Courses organized by the IWG-LMNPP from the year 1995 to approximately beginning of 1997. The proceedings of some of these meetings were published by the Agency and widely distributed.

SPM on "Cracking in PWR Pressure Vessel Head Penetrations"	Philadelphia, USA 2-4 May 1995	Working Material IWG-LMNPP 95/1
Regional Training Course on "Ageing Phenomena and Diagnostics for WWER Type Reactors"	Tmava, Slovak Republic, May-June 1995	
SPM on "Irradiation Embrittlement and Mitigation"	Espoo, Finland, 23-26 October 1995	Working Material IWG-LMNPP 95/5
IWG Meeting (regular) on Life Management in NPPs	Vienna, Austria, 30 August to 1 September 1995.	Working Material IWG-LMNPP 95/6
SPM on "Steam Generator Repair and Replacement, Practices and Lessons Learned"	Ostrava, Czech Republic, 15-18 April 1996.	Working Material IWG-LMNPP 96/1
Research Co-ordination Meeting (final) on the CRP "Management of Ageing of Reactor Pressure Vessel Primary Nozzle"	Erlangen, Germany, 15-16 October 1996.	

Research Co-ordination Meeting on the CRP on "Assuring Structural Integrity of Reactor Pressure Vessel"	Vienna, Austria, 9-11 September 1996	
Meeting of National Liaison Officers to the "International Database on Reactor Pressure Vessel Materials"	UK, 14-15 November 1996	
Interregional Training Course on "Ageing Phenomena and Diagnostics for PWR Type Reactors"	Karlsruhe, Germany, 25 November to 13 December 1996.	
Joint EC/OECD/IAEA Specialists Meeting on NDE Techniques Capability Demonstration and Inspection Qualification"	JRC Petten, Netherlands, 11-13 March 1997.	

Some future meetings also organized in the framework of the IWG-LMNPP are:

SPM on "Methodology for Pressurized Thermal Shock Evaluation"	Esztergom, Hungary, 5-8 May 1997
SPM on "Irradiation Effects and Mitigation"	Vladimir, Russian Federation, 15-19 September 1997
IWG Meeting (regular) on NPP Life Management	Vienna, Austria, 6-8 October 1997
Research Co-ordination Meeting on the CRP on "Assuring Structural Integrity of Reactor Pressure Vessel"	Vienna, Austria, 8-10 October, 1997
SPM on "NPP Condition Monitoring and Maintenance"	1998, dates and venue to be defined at the IWG meeting
SPM on "Loadings Outside the Design Base"	1998, dates and venue to be defined at the IWG meeting
SPM on "Ageing Effects in NPP Metallic Materials other than RPV and SG"	1998, dates and venue to be defined
Meeting of Liaison Officers to the International Database on "Reactor Pressure Vessel Materials"	February 1998, venue to be defined

Apart from the information exchange and collection via Specialists Meetings and Co-ordinated Research Programmes, the IWG takes part in a number of Advisory Group

Meetings and Consultants Meetings to specify further activities of the IWG.

Some items of current priority are important to the IWG-LMNPP even though they are not included in the list of top priorities. These activities are usually co-ordinated with other sections or the department of the IAEA. For example the IWG has recognized that more importance should be given to economic aspects in relation to plant management, safety and maintenance. This activity is carried out jointly with the Planning and Economic Studies Section of NEPF as well as with some other international organizations such as OECD/NEA. Another example would be power assisted equipment. In this case the Co-ordinated Research Programme (CRP) on "Management of Ageing of Motor Operated Isolating Valves" (MOV) was started jointly with the NS Department of the IAEA with primary objectives being:

- understanding of MOV ageing
- monitoring of MOV ageing
- risk and reliability analysis
- MOV qualification methods and guidelines
- guidelines for MOV maintenance.

Though it was not possible to achieve some of the goals, the work is being continued. This CRP will be completed in mid 1998.

PUBLICATIONS

In addition, the IWG also takes part in producing publications. There are several publications which are expected to be released during this year. They are:

1. TRS on CRP "Optimizing Reactor Pressure Vessel Surveillance Programmes and Their Analysis"
2. TECDOC on CRP "Management of Ageing of RPV Primary Nozzle".
3. TRS on "Nuclear Power Plant Life Management - Owner's Point of View".

This year the IAEA has also started work on the preparation of the TRS on "PTS Methodology for all Reactor Concepts".

DATABASE

Data availability is the key aspect in evaluation of the components state and therefore in the decision making process related to their life management. Data sets required for NPP life management may differ from reactor type, however it may be categorized in general as follows:

- component specification data
- operating history
- current plant state
- maintenance
- technology developments
- material properties

- relevant generic data.

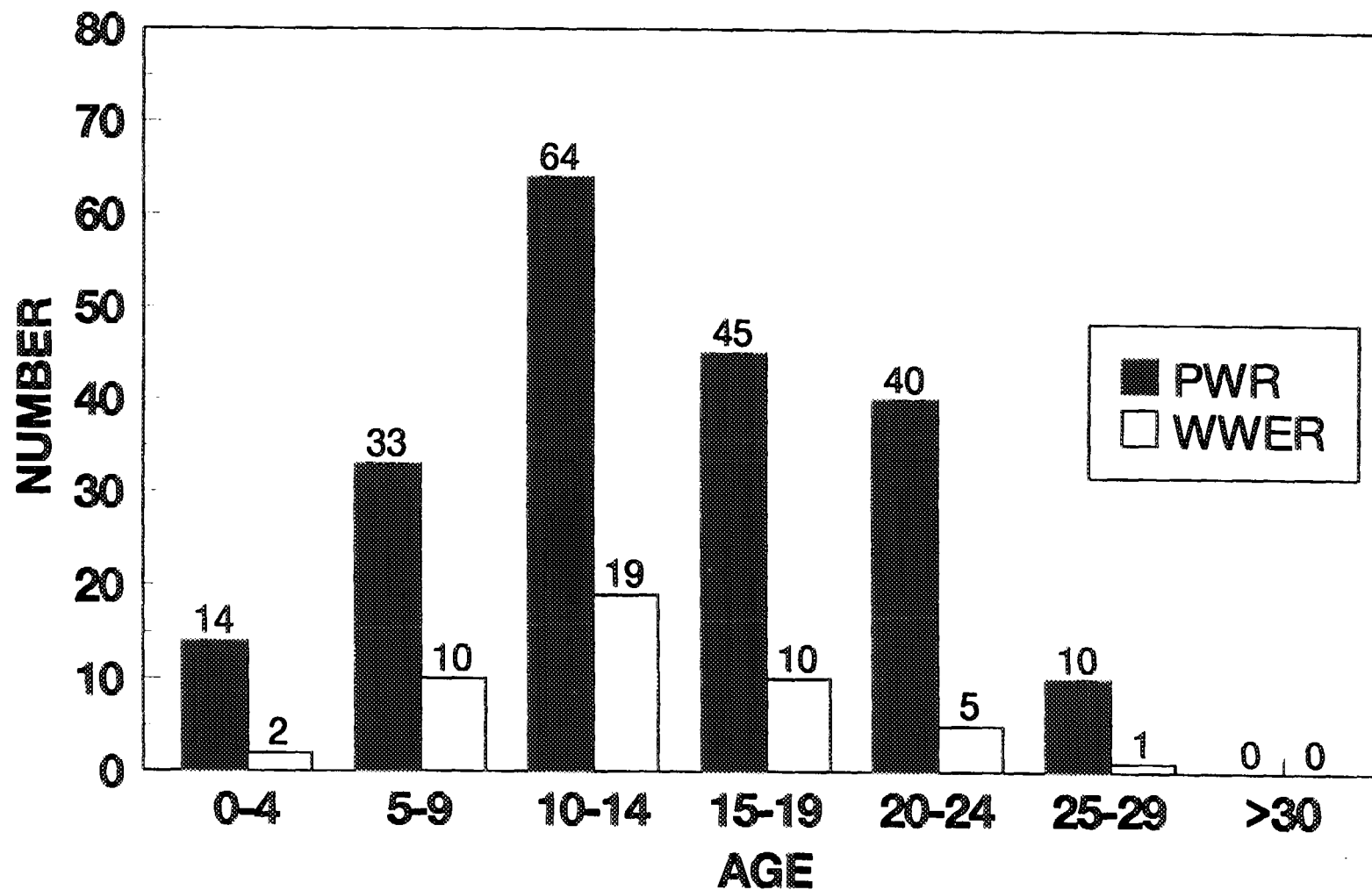
This list can be developed and extended further with regard to specific component. It has been recognized that for effective management of ageing and degradation related processes a large amount of data is needed. To supplement inadequate data, a systematic collection and screening of the information available provides a viable and powerful aid. This approach leads to the idea of the development of databases which could assist in research and decision making process.

Several years ago the IAEA started work on the International Database on NPP Life Management. This is a multi module Database (Fig. 3) first of which is called the International Database on Reactor Pressure Vessel Materials and it was completed last year. After the specification of the Database on RPV materials had been completed the IAEA developed a software and called for international participation. At present the IDRPVM comprises of 10 participants and we expect some more countries to join the Database in the near future. Current membership to the IDRPVM is shown in Fig. 4.

The next step, which is under way now is the elaboration of specifications for the Steam Generator Database and the development of the software for the Primary Piping Database.

Some results on the elaboration of the International Database on NPP Life Management were presented in the IAEA Working Material, IWG-LMNPP-95/4. This document has been revised by the Agency's consultants and its final version is expected to be issued in May-June this year. The updated version will include a detailed description of the Database structure as well as the legal framework for the Database.

Number of Reactors by Age and Type



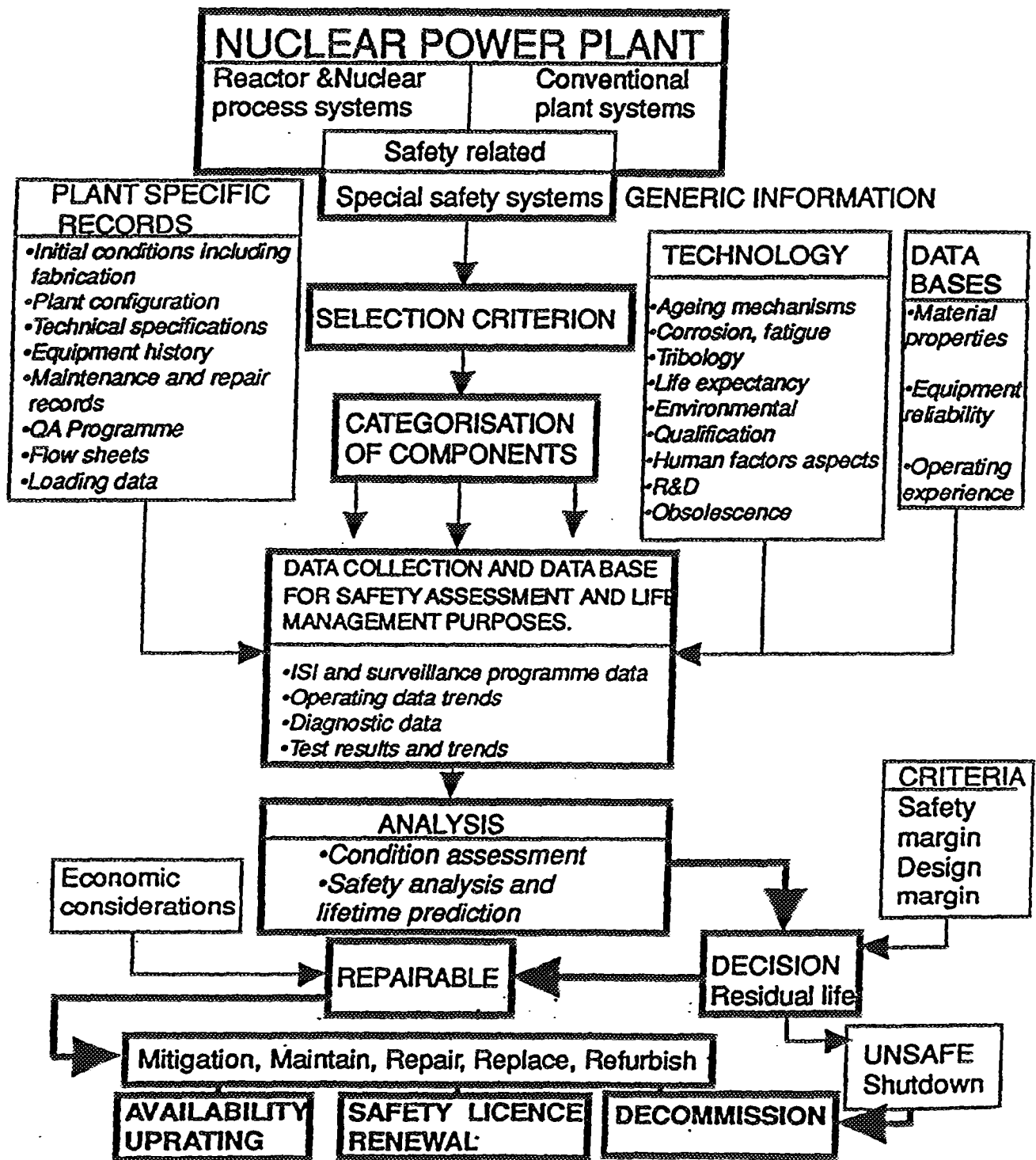
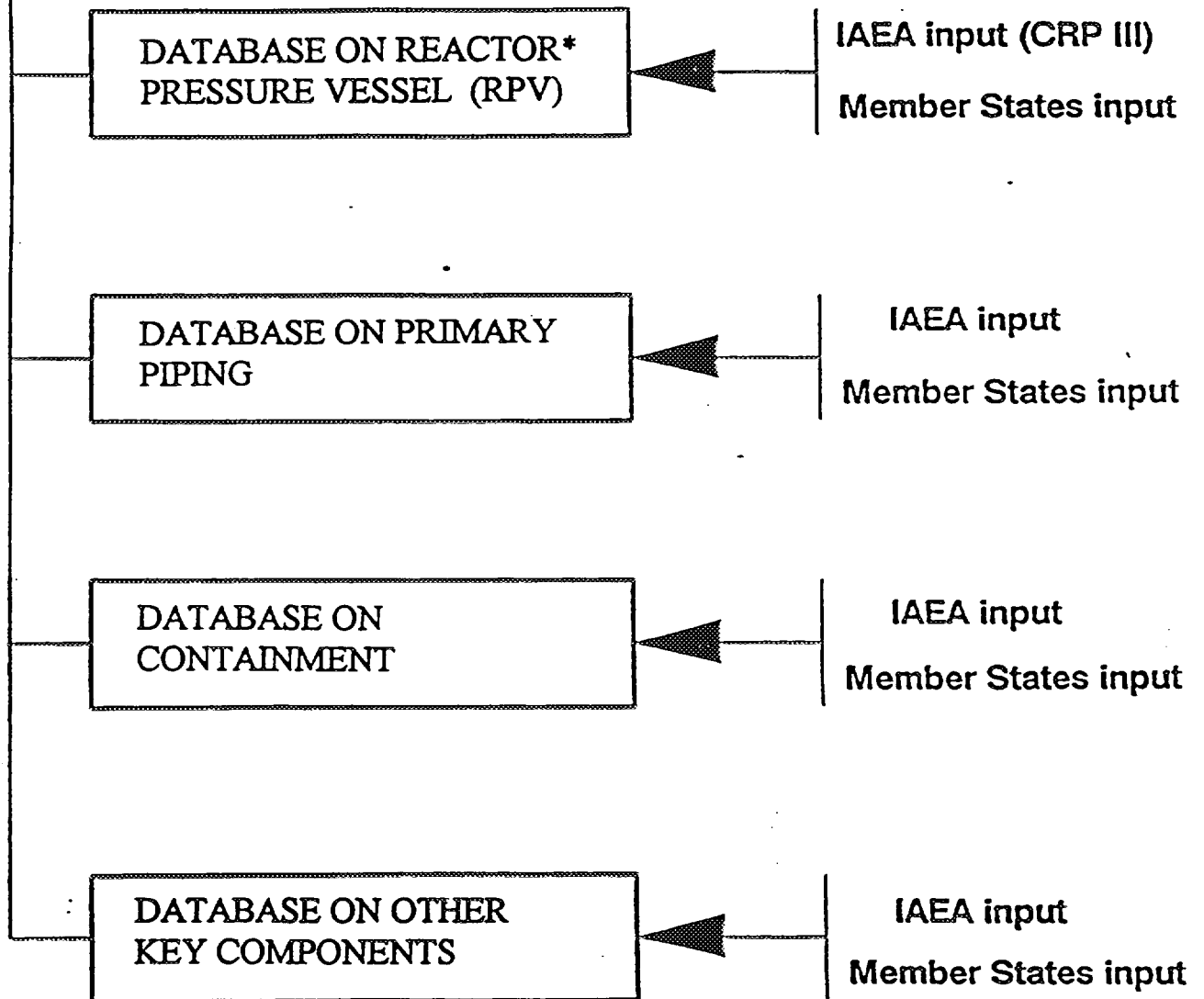


FIG. 2 Nuclear power plant life management processes

THE INTERNATIONAL DATABASE ON NPP LIFE MANAGEMENT



* at present time the database refers only to material properties data
(thermal and irradiation effects)

Fig. 3. Structure of the Database on NPP Life Management

INTERNATIONAL DATABASE ON REACTOR PRESSURE VESSEL MATERIALS

MEMBERS

1. BELGIUM
2. BRAZIL
3. FRANCE
4. HUNGARY
5. ITALY
6. KOREA
7. RUSSIA
8. SPAIN
9. UKRAINE
10. USA

FIG. 4

Session 1.

5 MAY, MONDAY

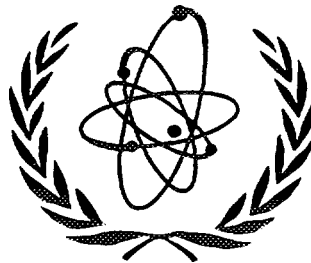
International programmes

Havel, R.	IAEA	IAEA Guidelines on the RPV PTS Analysis for WWER NPPs
Hurst, R. C. McGarry, D. Wintle, J. B. Hemsworth, B.	JRC/IAM (EC)	Status Report on the NESC I. Project

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Guidelines on PTS Analysis for WWER NPPs



**IAEA SM on PTS, Esztergom, Hungary, 1997
Radim Havel
Division of Nuclear Installation Safety, IAEA**

IAEA EBP on the Safety of WWER and RBMK NPPs

- **1990: a programme to assist the CEE countries in evaluating the safety of their WWER-440/230 nuclear power plants**
- **1992: WWER-440/213, WWER-1000, and RBMK plants in operation and under construction included**
- **objectives: to identify major design and operational safety issues; to establish international consensus on priorities for safety improvements; and to provide assistance in their implementation**
- **programme implementation: plant specific missions, reviews, topical meetings on generic safety issues, ASSETs and OSARTs**

IAEA EBP on the Safety of WWER and RBMK NPPs

- **database:**
 - **technical safety issues identified**
 - **status of implementation of safety improvements**
 - **provision of assistance to strengthen regulatory authorities**
 - **technical basis for safety related decisions by national authorities and promotes further in-depth safety evaluations, results of other relevant national, bilateral and multilateral activities taken into account**
 - **263 safety issues for WWERs (both design and operation) were identified and ranked**
 - **identification and ranking for RBMKs to be completed**
-

WWER RCS

- **reactor coolant system integrity**
 - **24 safety issues identified**
 - **15 of them ranked as of high safety significance**
 - **reactor pressure vessel**
 - **high energy piping**
 - **steam generator**
 - **reliability of the non-destructive testing**
 - **IAEA activities**
 - **topical meetings**
 - **reviews**
 - **guidelines/CRP (LBB, PTS, ISI Q, RRE)**
-

Guidelines on PTS analysis for WWERs

- **need:**
 - **topical meetings discussions**
 - **safety reviews**
 - **participants:**
 - **Eastern and Western experts**
 - **WWER NSSS designer**
 - **documents used:**
 - **NUSS Code**
 - **national standards**
-
-

Guidelines on PTS analysis for WWERs

- **objective: to provide guidance for a reasonably bounding plant specific PTS analysis**
 - **approach:**
 - **deterministic anal. of limiting sequences**
 - **realistic modelling methods for individual elements**
 - **conservative assumptions, initial and boundary condition**
 - **safety factors**
 - **probabilistic anal. complementary, for special cases**
-

Guidelines on PTS analysis for WWERs

- **general considerations**
 - **selection and grouping of transients for PTS anal. (DBA)**
 - **core cooling vs. PTS**
 - **main PTS factors pointed out**
 - **acceptance criteria**
 - **no initiation of postulated defect**
 - **static fracture toughness**
 - **other approaches allowed if justified (arrest)**
 - **PTS contribution to CDF should not be significant**
-

Guidelines on PTS analysis for WWERs

- **assumptions for PTS analysis**
 - **plant data**
 - **thermal hydraulic**
 - **structural analysis**
 - **thermal hydraulic analysis**
 - **support selection of transients**
 - **input data for structural anal. (T, HTC, p)**
 - **specific aspects also addressed**
 - **structural analysis**
 - **SIF evaluation for postulated defects**
 - **postulated defects: 1/4s or smaller**
-

Guidelines on PTS analysis for WWERs

- **material properties**
 - **Russian standards**
 - **code prediction or surveillance data**
- **integrity assessment**
 - **SIF vs. lower bound static fracture toughness**
 - **credit to WPS possible**
 - **safety factors (SIF, crack size, temp.)**
 - **presentation of results (Tk vs. Tka)**

Guidelines on PTS analysis for WWERs

- **corrective actions**
 - **flux reduction and material properties**
 - **loads**
- **computer codes (fluence, thermal hydraulic, structural analysis)**
 - **general requirement**
 - **code validation**
- **quality assurance**
- **Appendices: examples**



Status Report on the NESC I Project

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^{*}European Commission Joint Research Centre Petten, The Netherlands

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ABSTRACT

The Spinning Cylinder Project is the first joint international collaboration of the Network for Evaluating Steel Components (NESC) and is based around a large scale spinning cylinder test. This paper summarises how the test conditions were defined and analysed, and how a method for detecting the moment of crack growth was developed. Now that the test has been successfully completed, the complex evaluation procedures can be commenced. The results of the test will constitute an important international benchmark for the understanding and validation of fracture methodologies used by the nuclear industry to assess reactor pressure vessels against pressurised thermal shock

INTRODUCTION

On 9-10 September 1993, the global network NESC and the first NESC project were launched at the HSE's Laboratory in Sheffield UK [1]. On 20 March 1997, the project reached a key milestone with the spinning cylinder test at AEA Technology's facility at Risley [2], Fig 1. A simulated pressurised thermal shock was applied to a cylinder of reactor pressure vessel material with defects specified by the international community. At least 50 organisations world-wide have participated in this benchmark project.

The A508 class 3 steel cylinder was manufactured by Sheffield Forgemasters, welded from two halves by MAN Germany, clad internally with stainless steel by Framatome, and implanted with both through- and sub-clad defects by Framatome, MPA (Germany) and JRC / AEAT [3]. The cylinder has been circulated to key centres in Europe where it has been subjected to a rigorous inspection [4]. Teams from 7 countries as far afield as Russia and the USA have reported their findings to the Reference Laboratory at JRC Petten. The inspection phases will effectively validate current non-destructive testing practice world-wide. Stress and fracture teams from Europe and the USA predict ductile and cleavage growth in at least one of the defects during the test.

The reactor pressure vessel (RPV) of a PWR reactor is a component critical to the safety of the reactor itself. A PTS transient is the event which, although unlikely, poses the greatest challenge to the integrity of the RPV, particularly when the vessel has been aged by irradiation [5]. Validation of the methodologies used for demonstrating safety in this event is the principal objective of this NESC project. The entire process of structural integrity is addressed through a holistic approach where the individual capabilities and interactions of materials, non-destructive examination (NDE) and fracture analysis are assessed with their proper context.

This paper describes how the test conditions have been set, the predictions of defect behaviour by structural analyses, and the development of a method for detecting crack growth. Finally, a preliminary assessment of the success of the test itself is given.

DEFINITION OF THE TEST CONDITIONS

The loading conditions for the test were dictated by the need to generate a high enough thermal shock to produce crack growth and the capacity of the spinning cylinder rig. The conditions for crack growth required a high crack driving force to be generated at the same time as the crack front was cooled to a temperature sufficiently low for cleavage fracture to be a strong possibility. A high crack driving force could be generated using a high rotational speed and a severe thermal shock.

The severity of the thermal shock required the maximum difference between the initial temperature of the cylinder and the temperature of the water quench and a high heat transfer coefficient (HTC). However, as it was also necessary to reduce the temperature of the crack front to a low value at the maximum crack driving force, the initial cylinder temperature had not to be too high. The lowest temperature of the quench water was influenced by practical considerations. Although the NESC Task Groups considered using an antifreeze solution cooled to below 0° C, it was agreed that the new features this would introduce would require a further development programme of rig engineering and heat transfer trials and would in any case not be representative of a realistic PTS transient. Hence, it was decided to use the lowest practical quench water temperature of 5° C, generated by cooling the water in the quench tank using a standard industrial cooler.

The initial cylinder temperature was determined as a result of a series of sensitivity analyses carried out by NESC Task Group 3 [6]. A range of temperatures between 265° C and 300° C was considered as being representative. The analyses showed that the temperature of peak crack driving force could be lowered to 80° C by using T_0 of 265° C but as this also reduced the crack driving force compared with using a high T_0 , there was no clear advantage in using a lower T_0 . Eventually it was decided to use an initial cylinder temperature of 290° C on the grounds that a slightly higher crack driving force would promote more ductile crack growth before cleavage and was more consistent with the values used in previous spinning cylinder and thermal shock tests.

The capacity of the spinning cylinder rig was determined in a series of rig trials [7]. It was necessary to accelerate the cylinder from an initial speed at the start of the quench so as to reach the maximum speed at the time of the peak crack driving force due to the thermal shock. In order to achieve a high heat transfer coefficient, a high flow rate quench was required. The trials established that the capacity of the rig to meet these requirements was ultimately limited by the power of the 400 kV electric motor and that the drag forces created by the quench water hitting the moving cylinder surface imposed a limit to the maximum speed obtainable.

A balance therefore needed to be struck between the flow rate necessary to produce a high heat transfer coefficient and the maximum speed and acceleration. After a series of trials at different flows, it was found that a flow of 727.5 l/min would produce a heat transfer coefficient of 10,000 W/m² °C. This was determined by measuring the temperature profile through the cylinder wall as a function of time and equating the integrated heat loss through the wall to that transmitted to the quench across the inside surface of the cylinder. A series of analyses by NESC Task Group 3 showed that the thermal shock conditions were relatively independent of HTC for values of HTC above 10,000 W/m² °C but below this value both the severity and the cooling of the shocks would be significantly reduced [7].

The maximum acceleration and rotational speed obtainable at a quench water flow rate of 727.5 l/min were determined by running the motor at maximum rated electrical power. Attempts to exceed the rated power resulted in the control system being unable to function properly and motor overheating. Controlling the motor power to just within the rated value was achieved by progressively lowering the acceleration demand as the speed increased. The maximum speed

obtainable was 2300 rpm when the power needed to overcome the drag forces of the quench equalled that of the motor and no further acceleration was possible.

The initial cylinder rotational speed at the start of the quench was determined by considering the calculated time of the peak thermal crack driving force from the analysis and the limiting speed time profile. From this, it was agreed that an initial cylinder speed of 2100 rpm at the start of the quench would enable the rig to approach its maximum speed while still slightly accelerating at the time of peak thermal crack driving force. The requirement for an increasing rotational load generating the primary stress was based on warm pre-stressing considerations. From this work all the necessary test conditions were defined.

PRE-TEST STRUCTURAL ANALYSIS

One of the key objectives of the project is to compare structural analyses of the behaviour and growth of the defects during the spinning cylinder test from the use of different analysis methods and as-measured materials properties [8] and defect dimensions [9]. This is being accomplished by NESC Task Group 3 where analyses by some 25 different organisations are being undertaken. The approaches differ widely ranging from comprehensive three dimensional elastic plastic finite element calculations to simplified elastic approaches where approximations are made to estimate the stress distribution and crack driving force. The Rousselier and Beremin local approaches to ductile and cleavage fracture are also being applied [10].

The teams are being asked to make their best prediction of the amount of ductile tearing prior to cleavage, the time and location of cleavage initiation, and the extent of cleavage propagation prior to crack arrest. Results to date indicate that most teams are predicting both ductile and cleavage growth of the major defects but by differing amounts. The results support the conclusions of the comprehensive test design analyses carried out by Oak Ridge National Laboratory and others [11], Fig 2.

The analysis data has been collected before the test by the Reference Laboratory (JRC Petten) for post test evaluation by the Network. Issues for evaluation will include the effect of constraint and scale on ductile growth and cleavage initiation compared with small scale deep and shallow crack data, the effect of the cladding on the mode of growth, the margins in terms of temperature and toughness between the observed initiation and that assessed using code or regulatory methods, and the validity of simplified methods and 2-D analyses. The evaluation will be aimed at identifying the methods which perform best drawing generic conclusions from the test data.

DETECTION OF CRACK GROWTH

One of the key tasks of the instrumentation was to detect the moment of cracking during the test and hence the time of initiation after commencement of the quench. In previous spinning cylinder tests AC potential difference methods had been used to detect crack growth [12]. However, these had not proved very satisfactory in that the change in output due to crack growth was small when the crack tunnelled beneath the surface and could not be easily distinguished from signal noise. NESC Task Group 4 concluded that the application of ACPD to a cylinder with stainless steel cladding would not be successful because of the problems of signal noise and tunnelling under the 4 mm cladding thickness.

Instead, it was initially decided to try to measure crack growth by detecting the change in crack opening using strain gauges placed across the mouth of the open crack. This technique had been previously successfully applied by IVO to measure crack growth in the Prometey PTS tests [13]. A laboratory trial by IVO had shown that an Ailtech Type SG 325 high temperature demountable strain gauge welded over a length of 8 mm at the ends could give a continuously increasing output up to a strain of over 10%. However, when this approach was evaluated for the NESC cylinder, it was found that the crack opening predicted during the test exceeded the working range of the HEAT gauges supplied which were likely to fail before any growth occurred.

Therefore, a different approach was adapted based on detecting the change in strain in initially uncracked material beyond the ends of the defect as a result of the crack tunnelling beneath them. Finite element calculations showed that the peak hoop strain in uncracked material would be about 0.4% over a 12 mm gauge length, but that this would increase by nearly an order of magnitude to 3-4% due to crack tunnelling. There was good confidence that the gauges supplied would survive the test over uncracked material and that the moment of crack growth would be detected by a rapid discontinuous change in strain or by gauge failure.

Gauges were symmetrically mounted beyond the ends of the principal through- and sub-clad defects at distances of 5, 15 and 40 mm from the defect tips along the line of the defects. These gauges were also end weld in order to accommodate the changes in strain between the 45° slip planes through the cladding. Trials had shown that 8 mm of spot welding at each end was sufficient to avoid flange failure leaving a 10 - 12 mm free gauge length in the centre. Since the opening of the through-clad defects and the change in strain over the sub clad defect were of interest to validate finite element models and for comparison purposes, gauges were also placed across the centre of these defects, although it was recognised that these gauges would probably fail before growth occurred. A further single gauge 40 mm beyond one end of the second through-clad defect completed the total complement of 19 gauges, Fig 3.

The gauge connecting wires were routed up the inside surface of the cylinder, across the support plate and through the drive shaft to a slip ring unit above the gear base. The outputs from the slip ring were connected to a multi-channel data logger and graphical screen displays for on-line monitoring during the test. Calculations predicted that the surface hoop strain would increase rapidly from its value at the start of the quench rising to reach a plateau. If no defect growth occurred, the strain would gradually reduce as the transient proceeded. Defect growth would be indicated by a step change in the strain gauge output which would be clearly detectable. From the time of initiation of rapid growth, the loading and temperature conditions can be determined by calculation. These are the conditions against which the various predictive methodologies will be validated.

THE SPINNING CYLINDER TEST

The actual test on the NESC cylinder was executed at AEAT, Risley on March 20th 1997. In front of over fifty invited spectators representing most of the organisations participating in NESC, the specified conditions for the test were followed exactly. These consisted of heating the cylinder to 290°C and increasing the rotational speed of the cylinder up to 1800 rpm at which point the drive motor was switched to full power to provide acceleration up to 2400 rpm whilst the quench was fired at 2100rpm and continued for 12 minutes, a quench temperature of 3°C was obtained by cooling the water in a quench tank using an industrial cooler. The preliminary evaluation of the output of the strain gauges indicated a fracture event at one end of the main through clad defect between 3 and 4 minutes into the quench. Full evaluation of all the data is continuing for all the defects and gauges

but this early interpretation of the data gives a strong indication that the test has been successful and that the professed goals of the NESC I Project are likely to have been met.

CONCLUDING REMARKS

On 20 March 1997 the steel cylinder prepared by the Network for Evaluating Steel Components has been successfully tested in the AEA Technology Spinning Cylinder Facility. The test follows three years preparation during which the cylinder has been manufactured with defects, inspected by seven national teams in a blind trial, characterised by materials testing, and whose behaviour during the test has been assessed by over 25 organisations. After the test, a programme of evaluation will begin during which the cylinder will be re-inspected to determine the capability to detect changes in the defects and fracture mechanics predictions will be evaluated against the test results. The project represents a milestone of international collaboration in the field of nuclear safety research in structural integrity and the full evaluation of the large scale spinning cylinder test combined with the inspection and structural mechanics assessments will raise the understanding of the complex issues to improve standards of structural integrity assessment.

ACKNOWLEDGEMENTS

The authors would like to gratefully acknowledge the contributions given to the project by all the NESC participants, the financial support of the NESC sponsors UK Health and Safety Executive and the CEC/JRC, and finally the excellent help of numerous colleagues at JRC Petten and AEA Technology.

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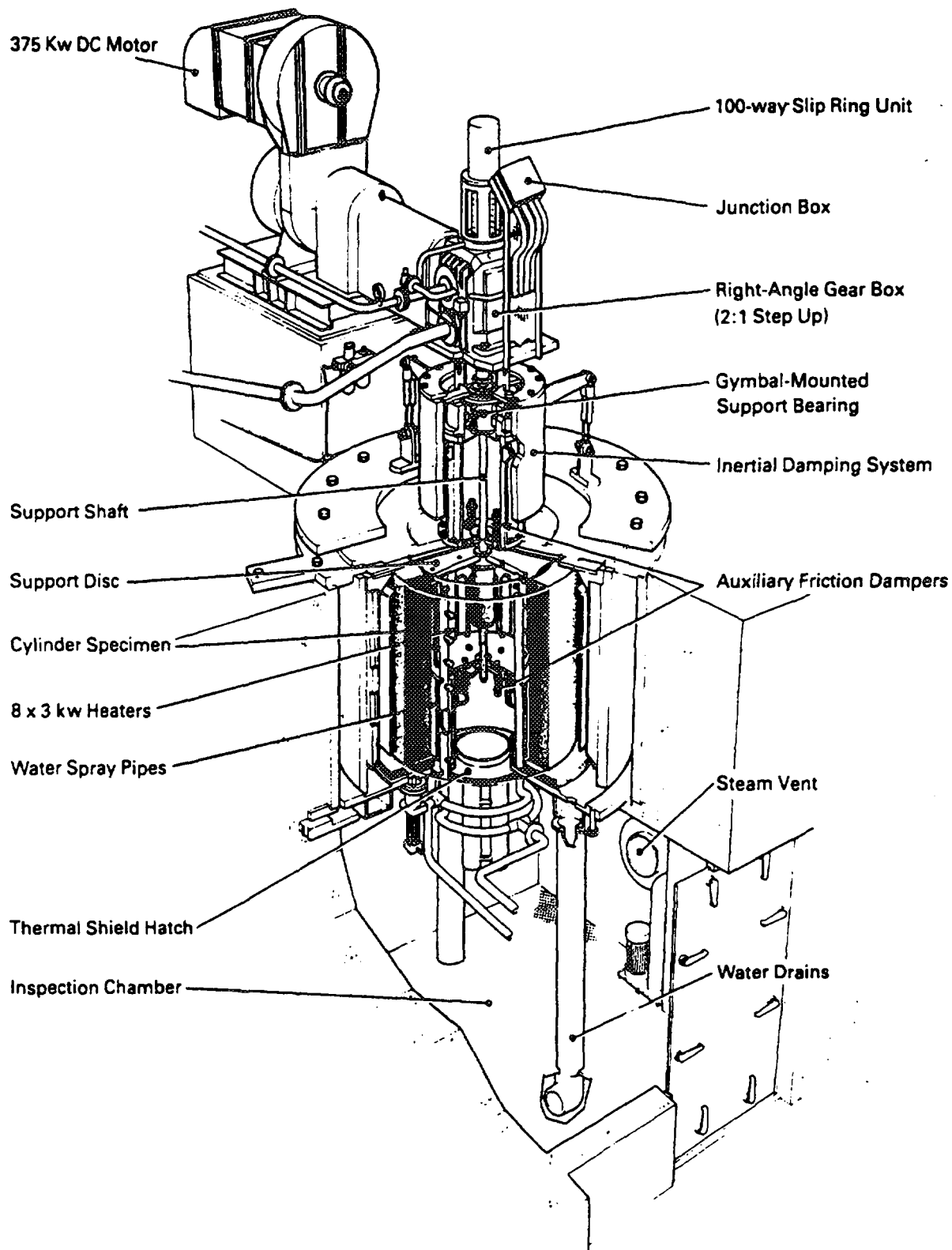


Figure 1. AEAT Spinning Cylinder Facility

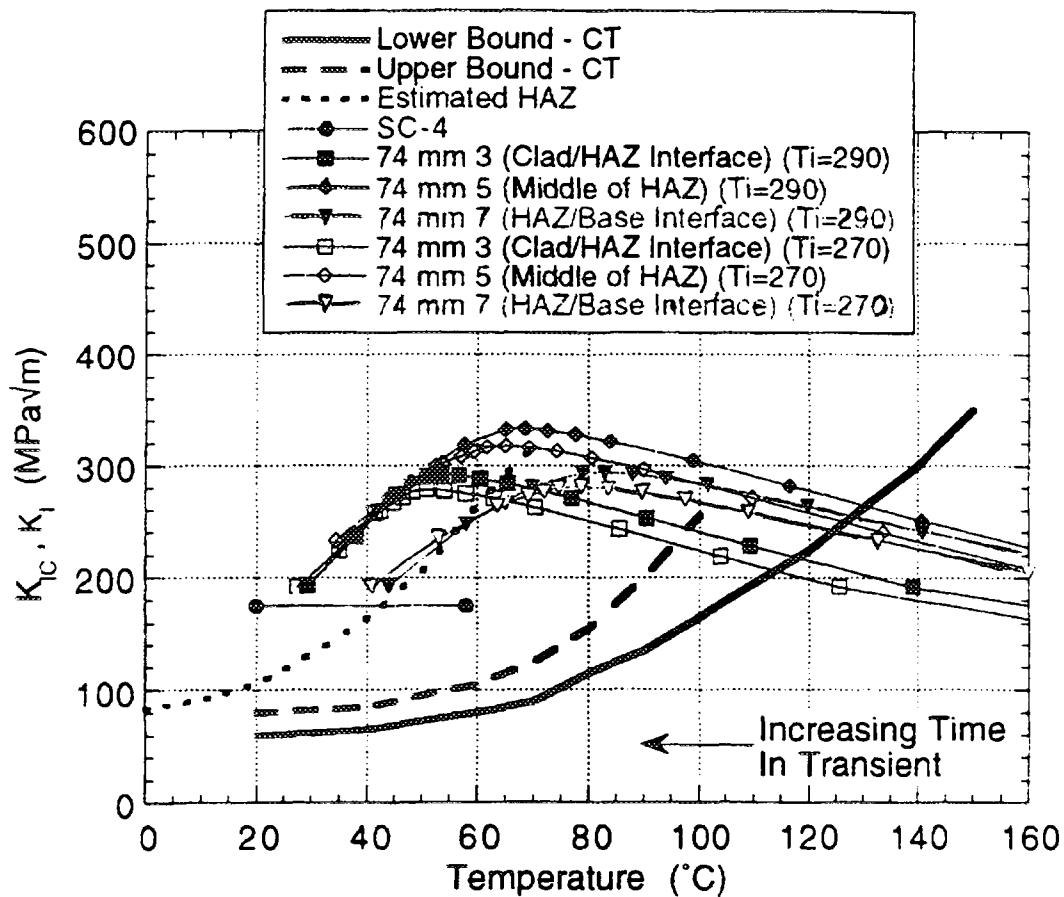


Figure 2. ORNL Test Design Analysis

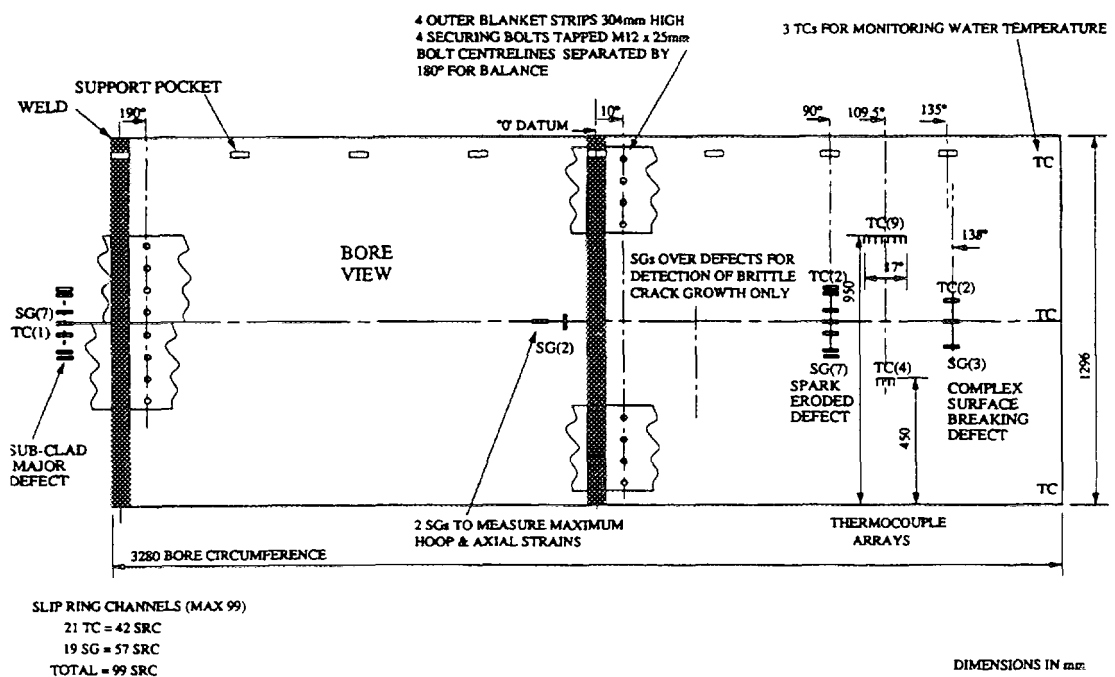


Figure 3. Strain Gauge and Thermocouple Instrumentation Plan

IAEA Specialist's Meeting on
PRESSURIZED THERMAL SHOCK
Esztergom, Hungary, 5-8 May

Session 2.
5 MAY, MONDAY

Invited lectures 1

Blauel, J. Nagel, G.	Germany	Pressurized Thermal Schock Evaluation of RPV Stade
Bishop, B. A. Carter, R. G. Gamble, R. M. Meyer, T. A.	USA	Alternative Method for Performing Pressurized Thermal Shock Analysis
Hong, S.Y. Jang, C. Jeong, I. S. Jin, T. E.	Korea	Plant Specific PTS Analysis of Kori Unit 1
Moinereau, D. Faidy, C. Valeta, M. P. Bhandari, S. Guichard, D.	France	Evaluation of Fracture Mechanics Analyses Used in RPV Integrity Assessment Regarding Brittle Fracture

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PRESSURIZED THERMAL SHOCK EVALUATION OF RPV-Stade

J.G. Blauel*
G. Nagel**
D. Hertlein***
L. Hodulak*
D. Siegele*

* Fraunhofer-Institut für Werkstoffmechanik, Freiburg D
** PreussenElektra AG, Hannover D
*** Siemens/KWU, Erlangen D



IAEA Specialists Meeting on Methodology for
Pressurized Thermal Shock Evaluation,
Esztergom, Hungary, 5.-8. May 1997

PRESSURIZED THERMAL SHOCK EVALUATION OF RPV-Stade

1. Introduction
2. Thermal shock analysis
thermohydraulics, temperatures and stresses, crack tip
field parameters, cladding influence, methodology of
fracture mechanics assessment
3. EOL safety evaluation for RPV Stade
initial conditions and input data, fracture toughness, load
path diagrams, warm prestress effect, crack arrest,
remaining load carrying capacity
4. Summary: The multibarrier safety proof for RPV Stade at EOL

Content of KTA 3201.2

1. Anwendungsbereich
2. Allgemeine Grundsätze
3. Lastfallklassen und Beanspruchungsstufen
4. Einwirkungen auf die Komponenten
5. Konstruktive Gestaltung
6. Dimensionierung
7. Allgemeine Analyse des mechanischen Verhaltens
 - 7.7...7.6 Belastungen, Beanspruchungen, Verformungen,...
 - 7.7 Spannungsanalyse
 - 7.8 Ermüdungsanalyse
 - 7.9 Sprödbbruchanalyse (Brittle fracture analysis)**
 - 7.10...7.13 Stabilität, Flansche, Ratchetting,...
8. Komponentenspezifische Analyse des mechanischen Verhaltens
9. Festigkeitsnachweise und Dokumentation

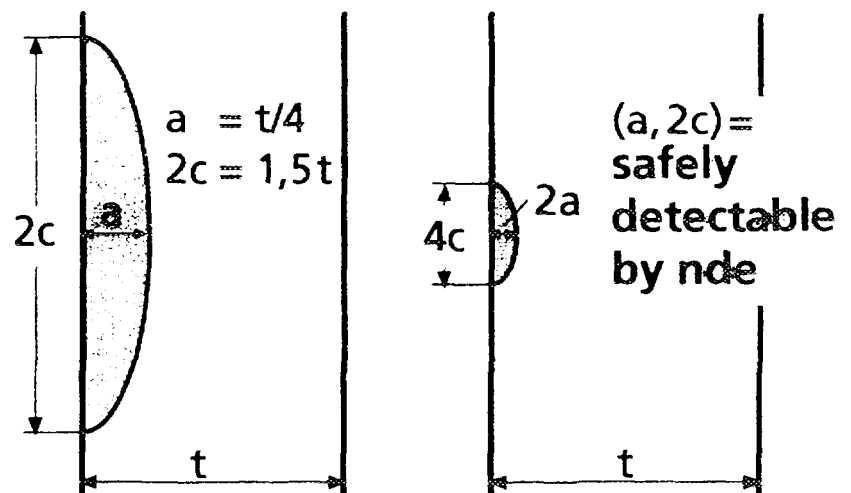
Anlagen	A	zur Dimensionierung
	B	zu den Rechnerischen Methoden
	C	zur Sprödbbruchanalyse (concerning brittle fracture analysis)
	D	Literatur

Brittle fracture analysis acc. to KTA 3201.2-7

Load cases:	Design	(O)
	Operation	(A), (B)
	Testing	(P)
	Emergency	(C)
	Faulted and Upset	(D)

Defect postulate:

Load cases: O, A, B, P C, D



Exclusion of initiation:

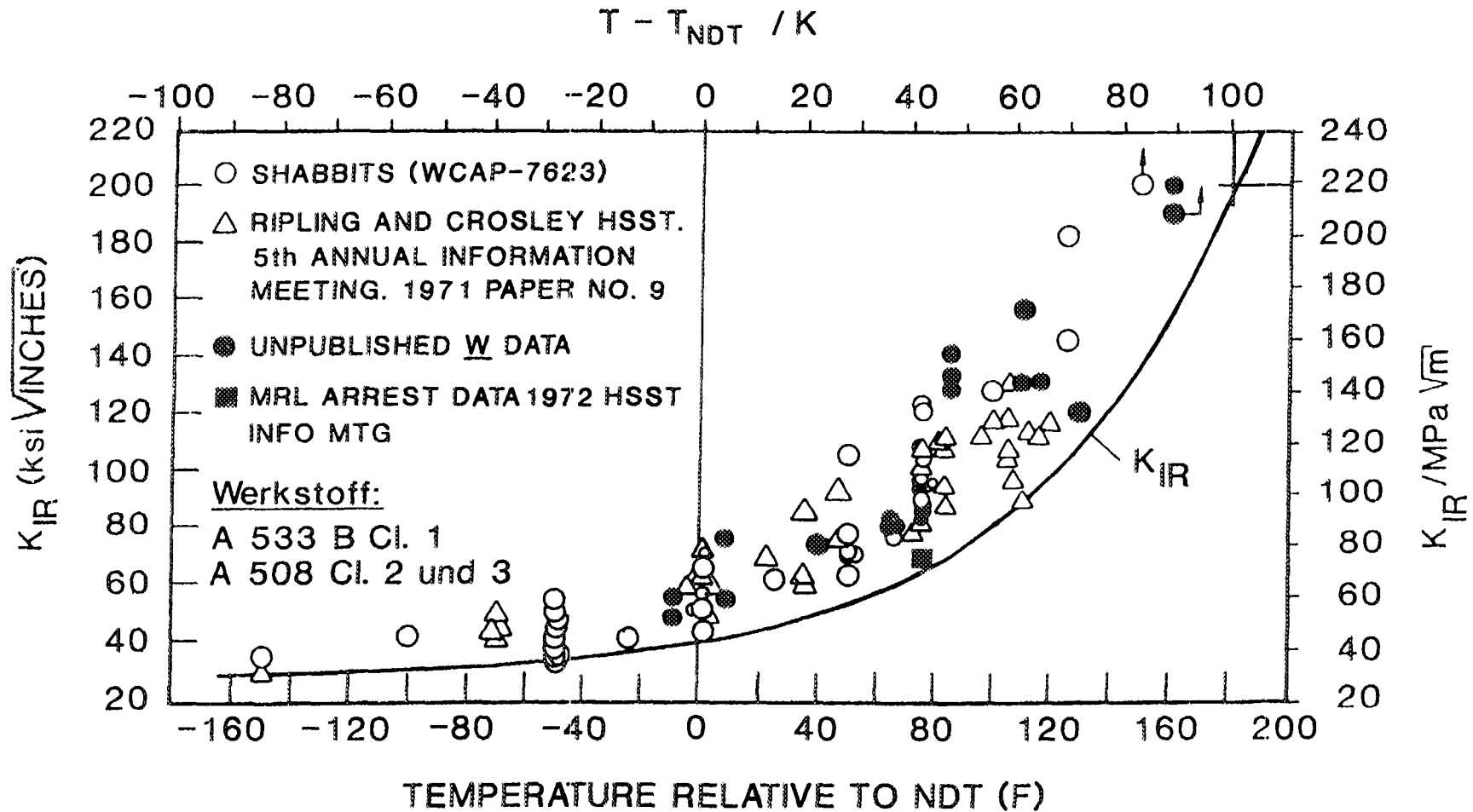
$$K_{\text{appl}} = K_I(a_o, c_o) \leq K_{Ic}(RT_{\text{NDT}}, T)$$

Crack arrest:

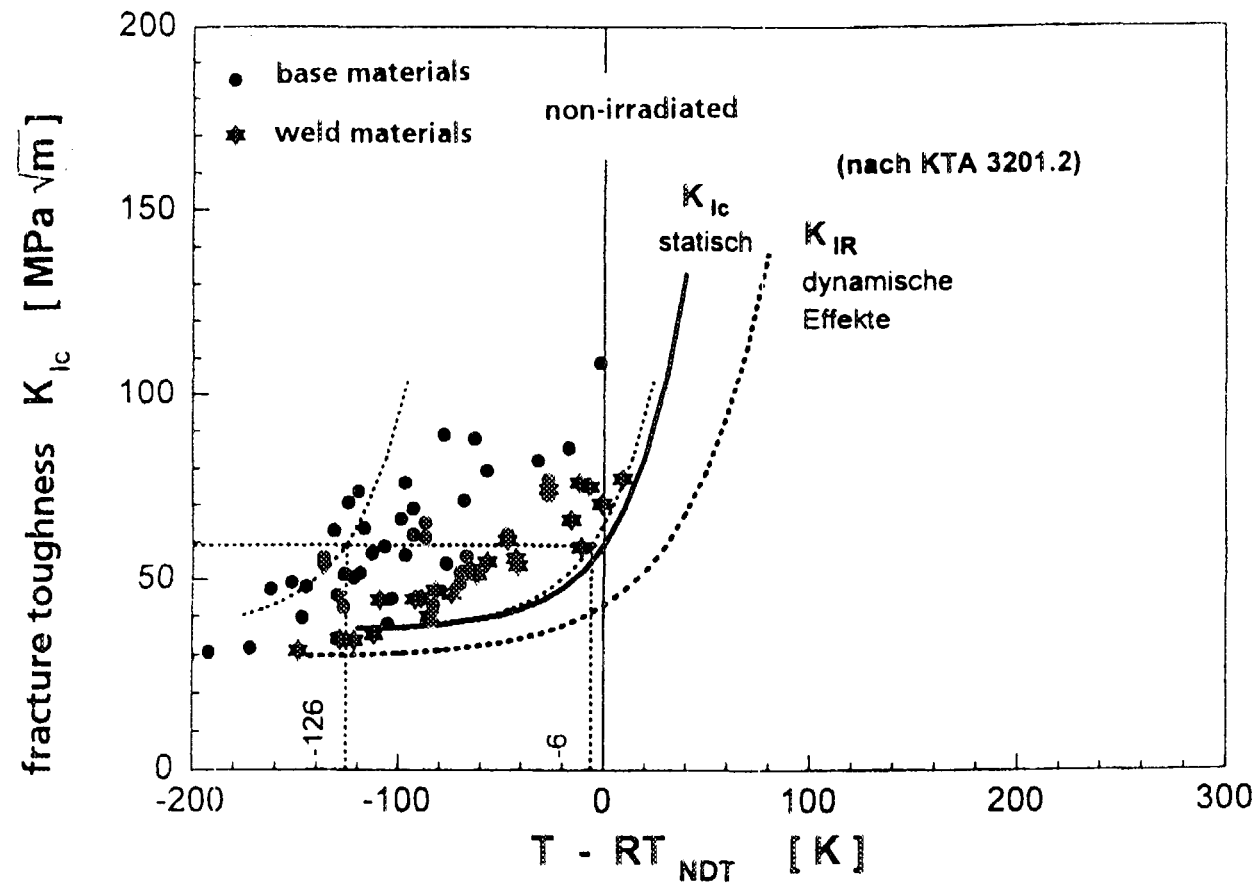
$$K_{\text{appl}} = K_I(a_{\text{arr}}, c_{\text{arr}}) \leq K_{Ia}(RT_{\text{NDT}}, T)$$

for $a_{\text{arr}} \leq 0,75t$

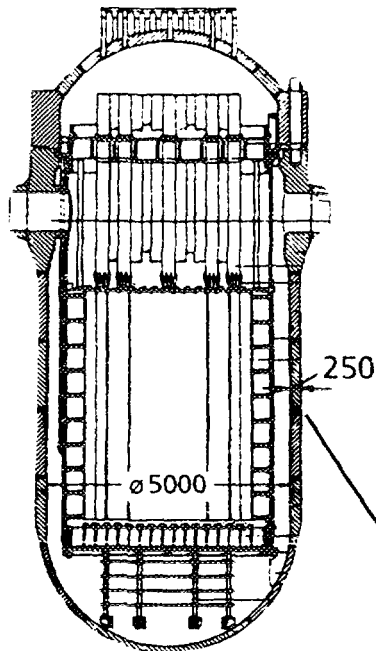
K_{IR} -reference curve acc. to WRC Bulletin 175 (1972)



FKS RPV materials and normalized K_{Ic} , K_{IR} -curves (Roos 1996)



RPV-Safety analysis acc. to KTA-rules



- Thermohydraulic-analysis

$$T_{KM}(t, z, \varphi), \quad WÜZ(t, z, \varphi), \quad p(t)$$

- FE-analysis

$$T_w(r)$$

$$\sigma^t(r, t)$$

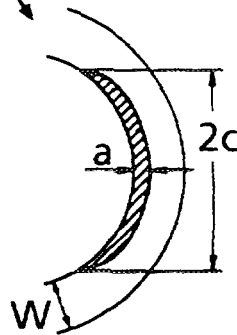
res. stresses

$$\sigma^e$$

$$\sigma^p(t)$$

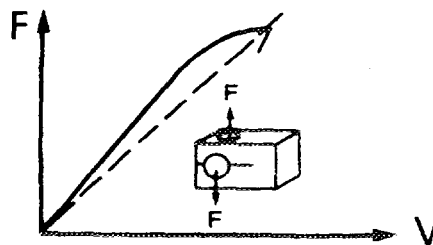
$$\sigma^{ges}(r, \varphi, z, t)$$

- Crack tip loading analysis



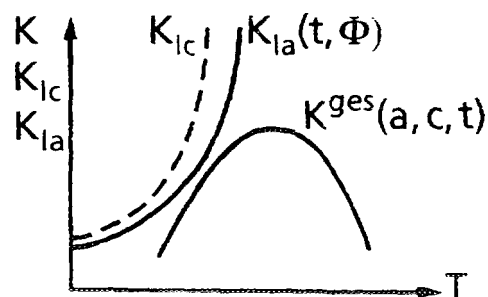
$$K^{ges} = K^{ges}\left(a, c, \frac{a}{W}, \Theta, \sigma^{ges}(t)\right)$$

- Toughness evaluation



$$K_{Ic} = K_{Ic}(T, n)$$

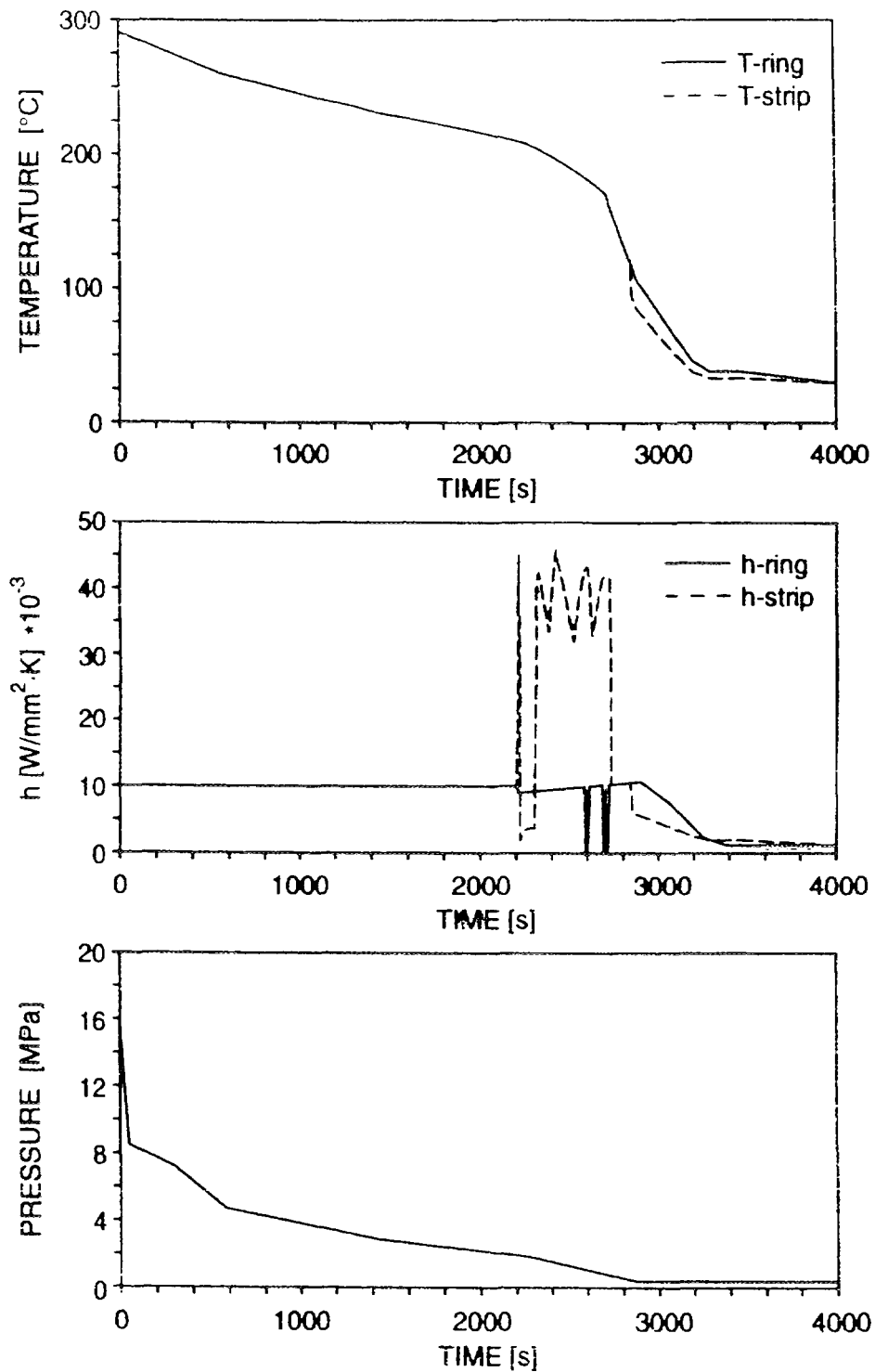
- Assessment



$K^{ges} \leq K_{Ic}$
$K^{ges} \leq K_{Ia}$ $a \leq 0,75W$

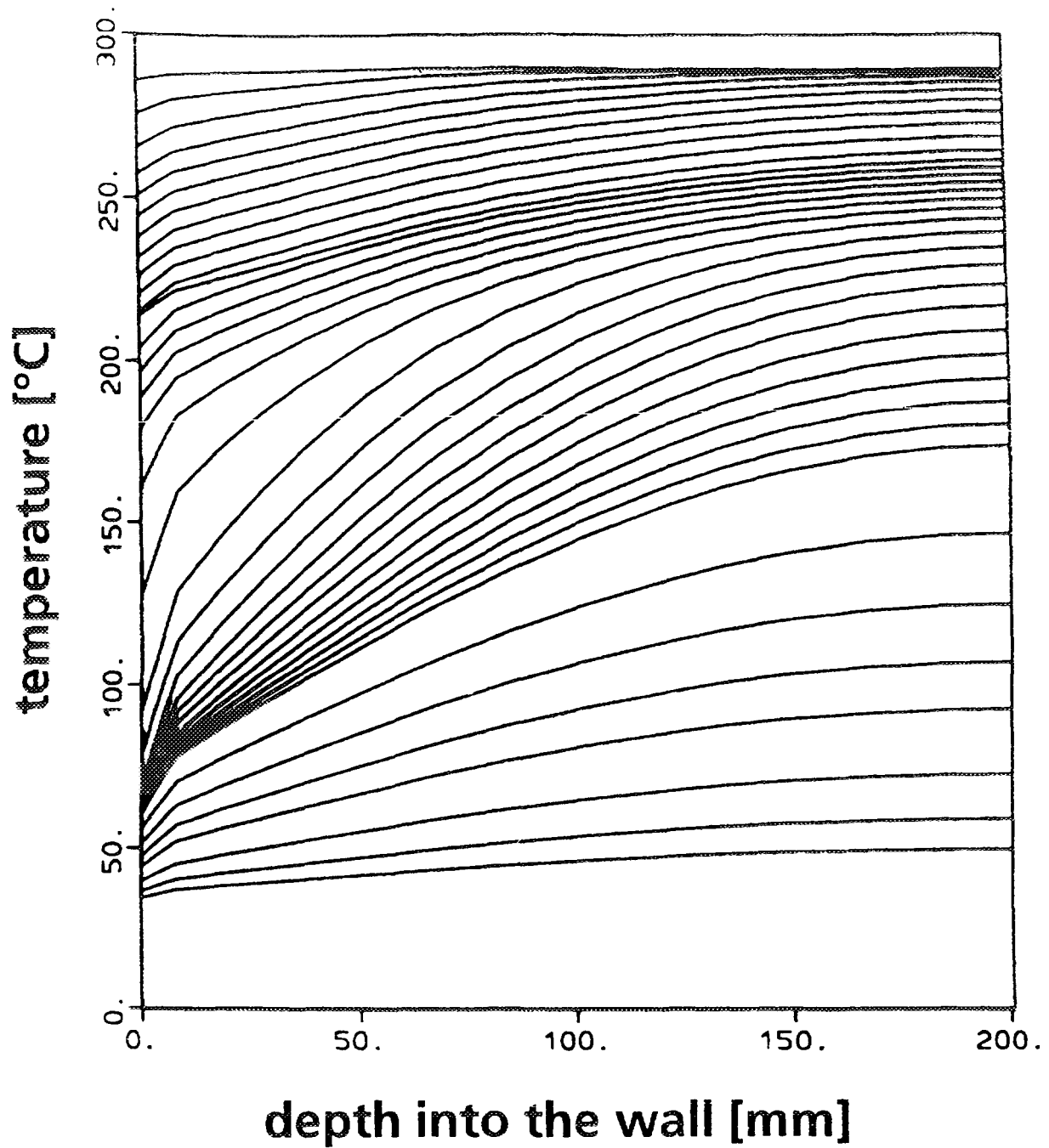
bbl-wl 97004/

LOCA-transient »KKS-TCM 120 cm² (h)« KWU (1991)



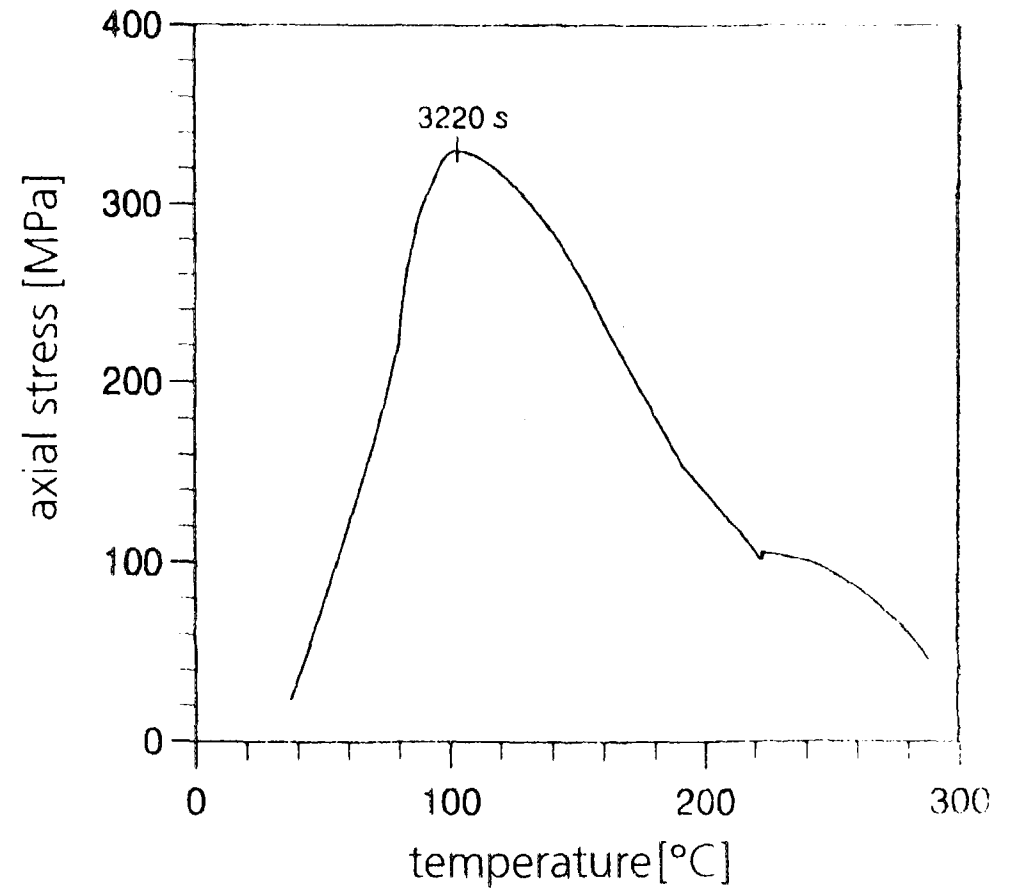
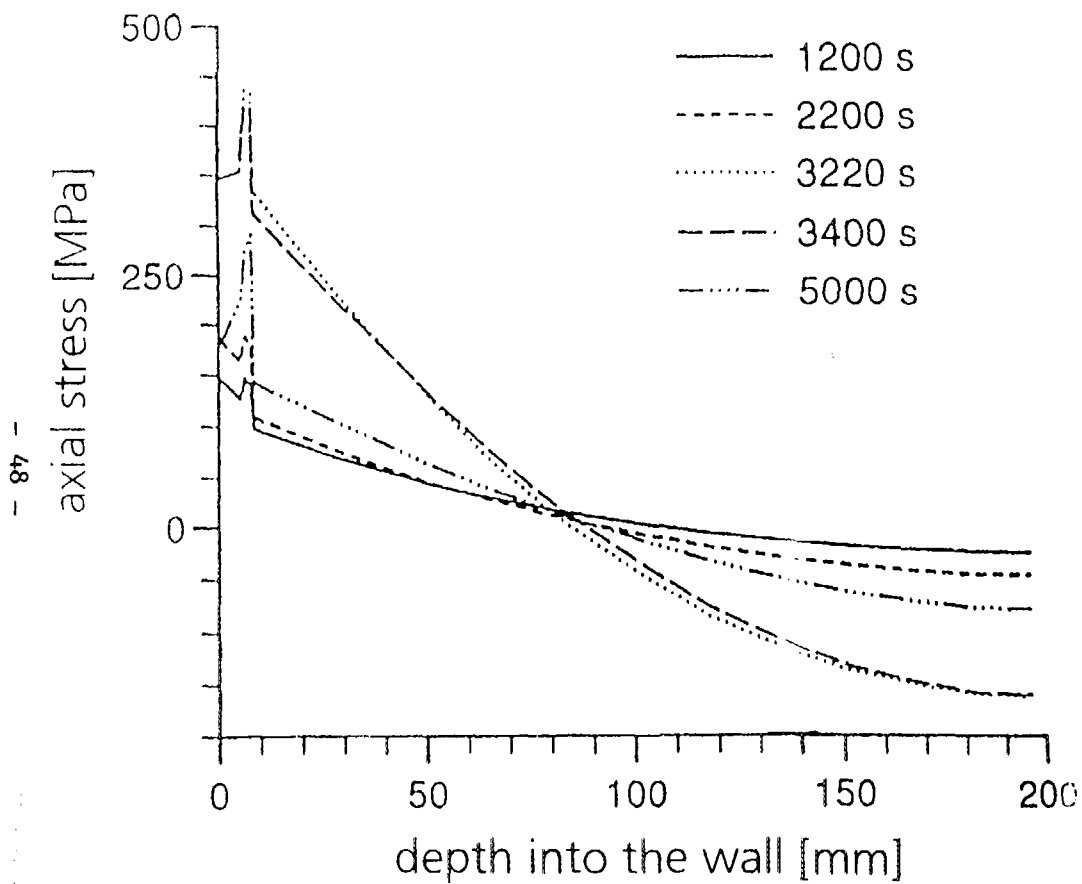
Fraunhofer Institut
Werkstoffmechanik

Temperatures in RPV wall for TCM 120 (h)

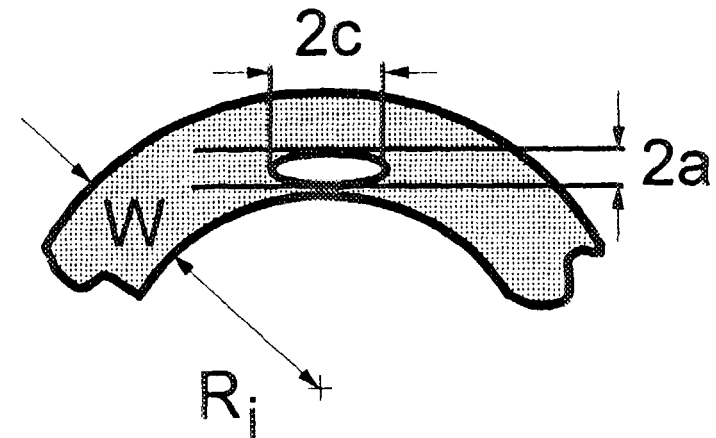
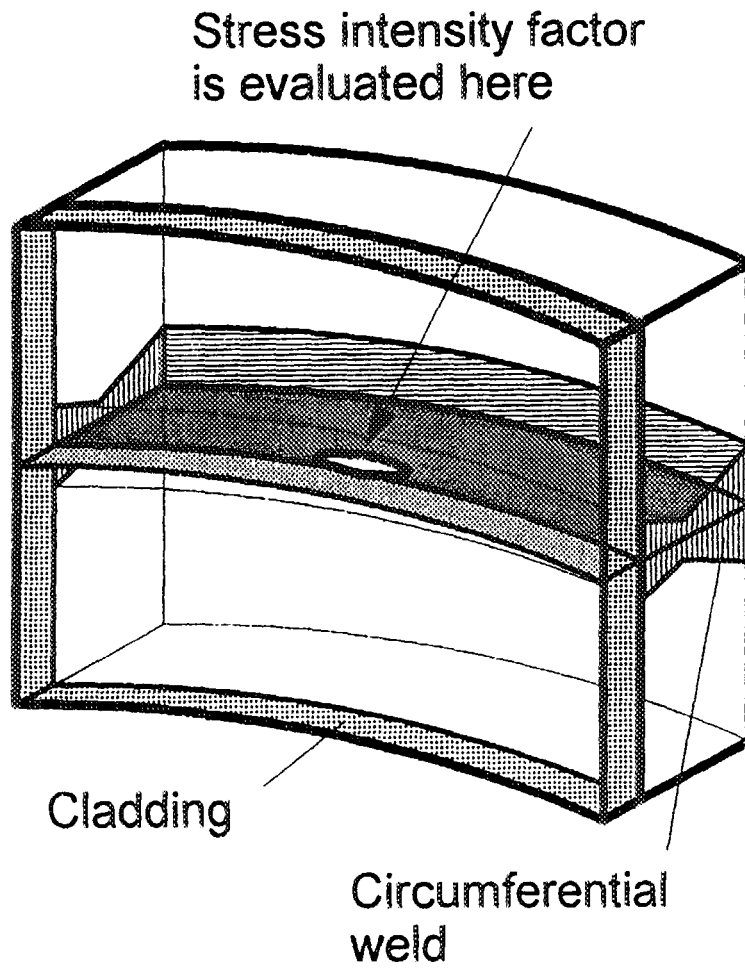


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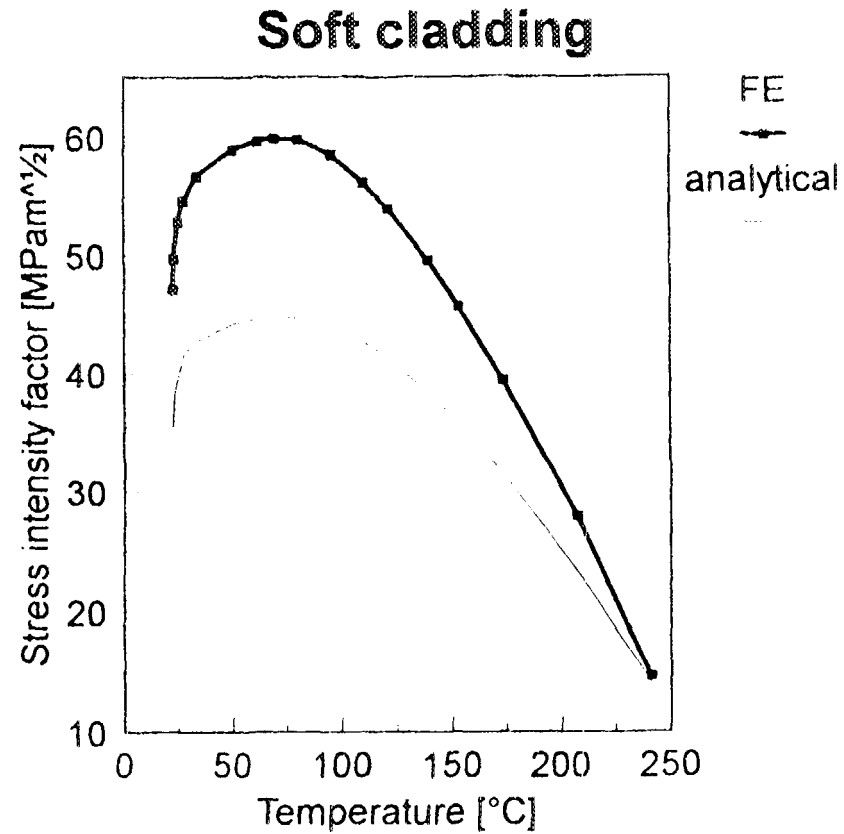
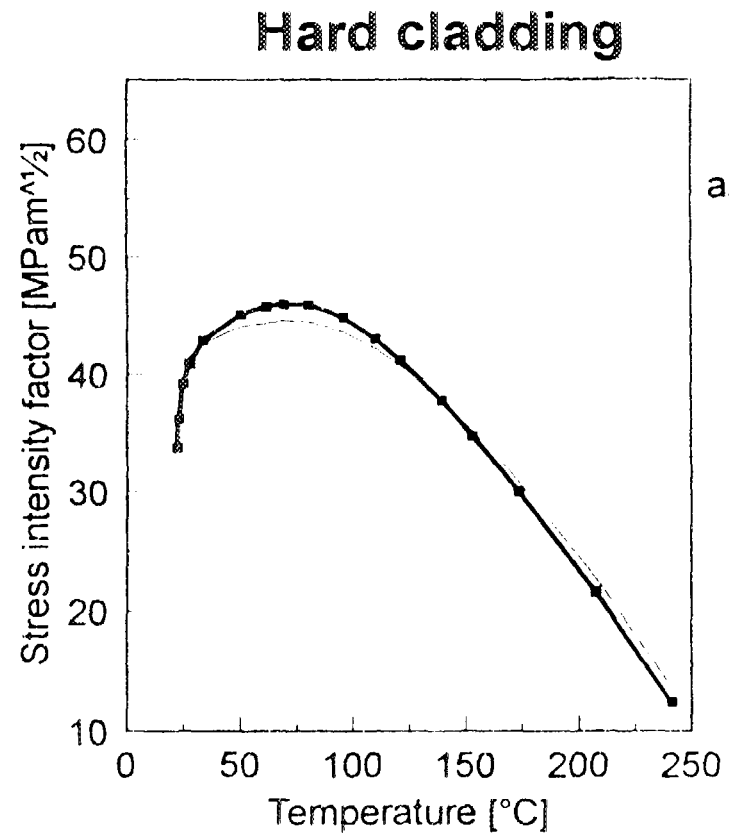
Axial stresses σ_z in RPV wall for LOCA-transient TCM 120 (h)

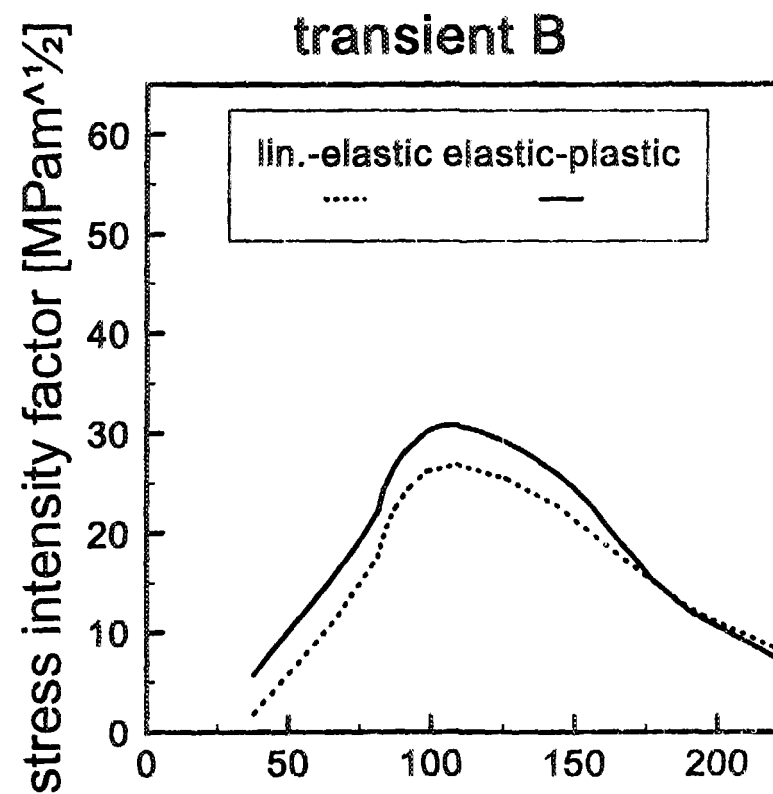
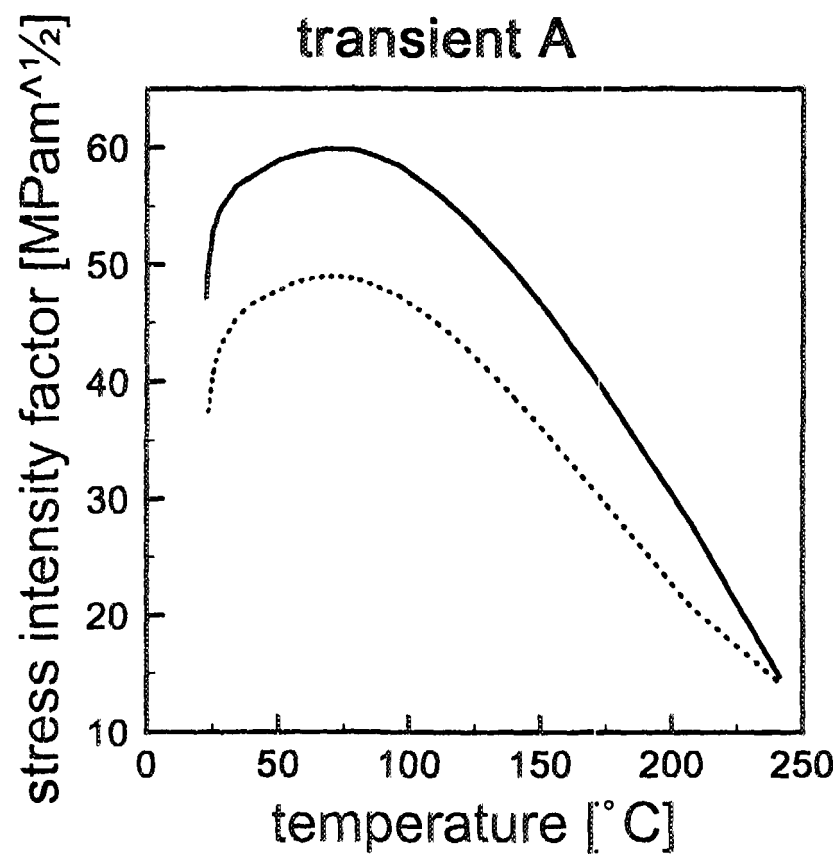


Crack under cladding

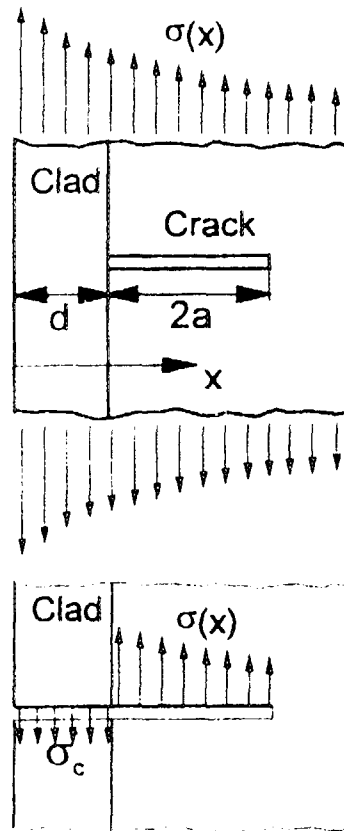


Underestimation of the stress intensity factor by linear elastic analytical calculation





Analytical model for crack under integer cladding

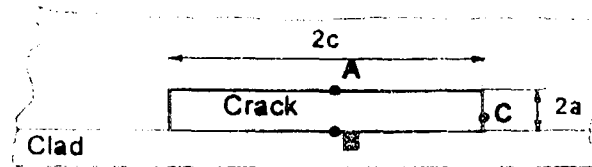


$$K_o = \sqrt{\frac{2t}{\pi}} \int_0^{\frac{a_m}{t}} \frac{R_i + x}{R_i + a_m} \frac{\sigma(x) m\left(\frac{x}{t}, \frac{a_m}{t}\right)}{\sqrt{\frac{a_m}{t} - \frac{x}{t}}} d\left(\frac{x}{t}\right)$$

$\sigma(x)$ = el.-pl. "undisturbed" stresses for $x \geq d$

$\sigma(x) = \sigma_c$ for $x < d$

$$a_m = d + 2a$$



$$K_a = K_o \cdot (1 + 0.24 a/c) / Q$$

$$K_c = K_o \cdot (1 + 0.19 c/a) / Q \cdot \sqrt{a/c} \cdot \sigma_i / \sigma_m$$

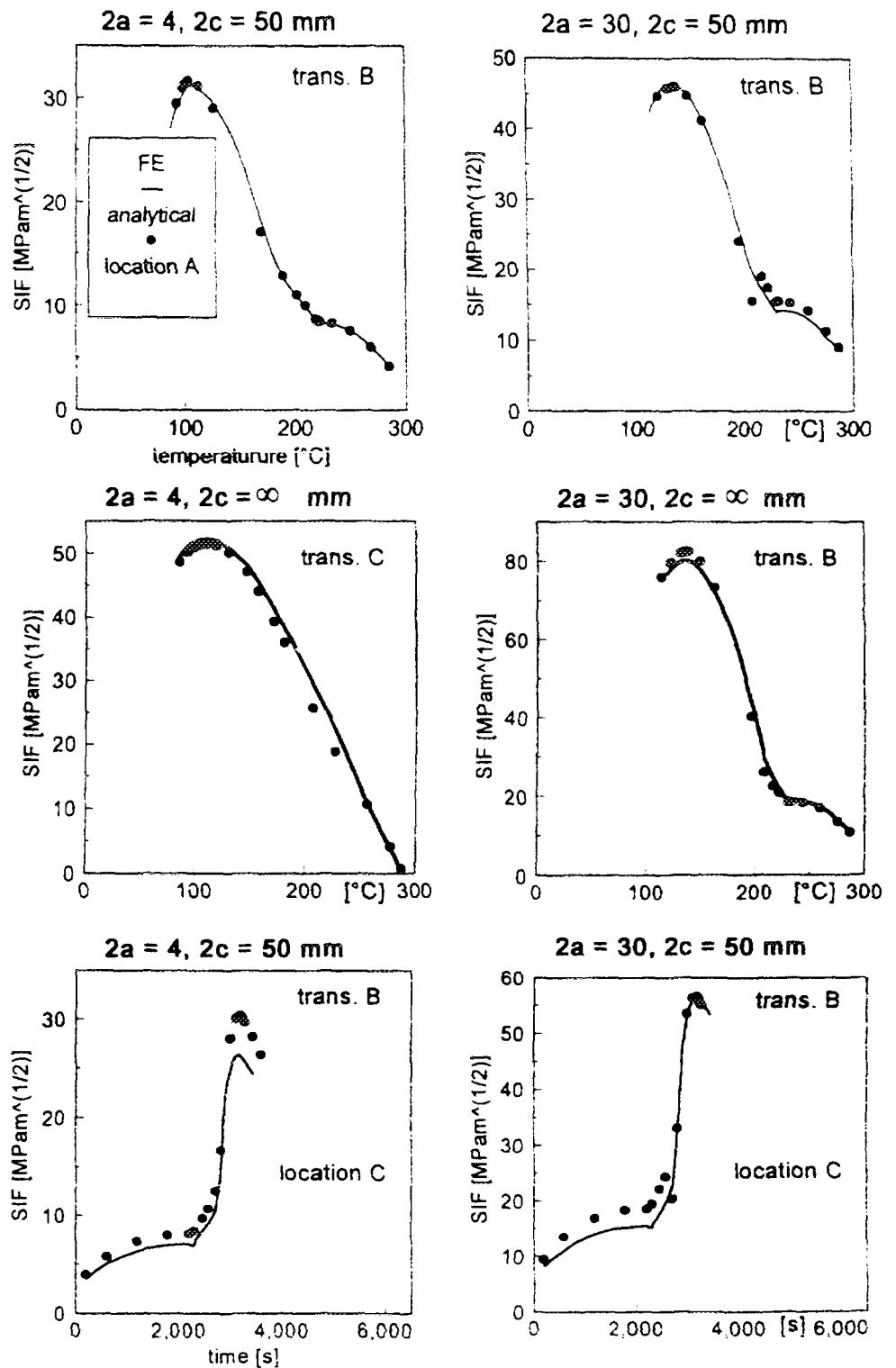
$$Q = 1 + 1.464 (a/c)^{1.65}$$

σ_i = stress at the interface cladding - base material

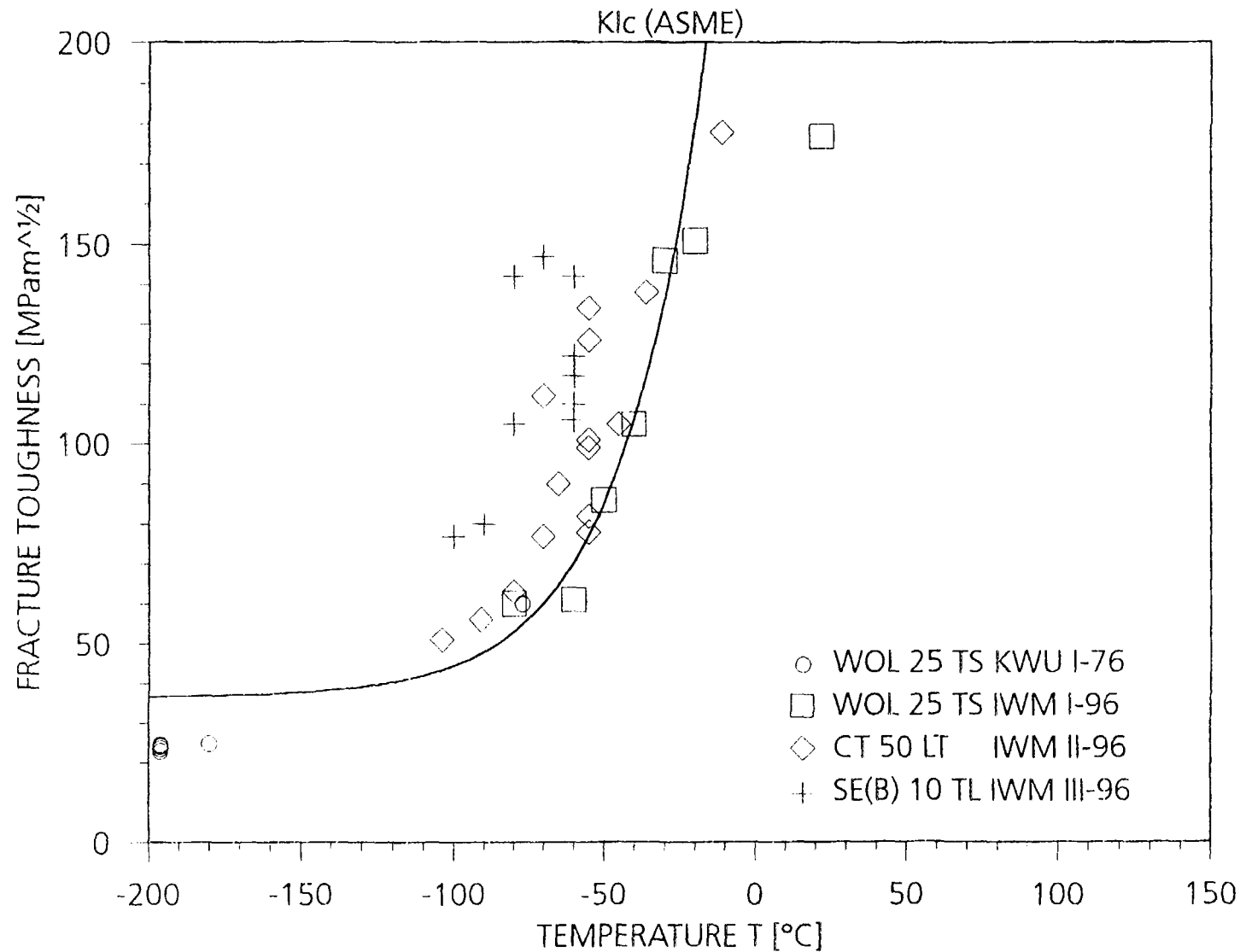
σ_m = mean stress over the crack depth

σ_c = closure stress (Hodulak, Siegele, 1995)

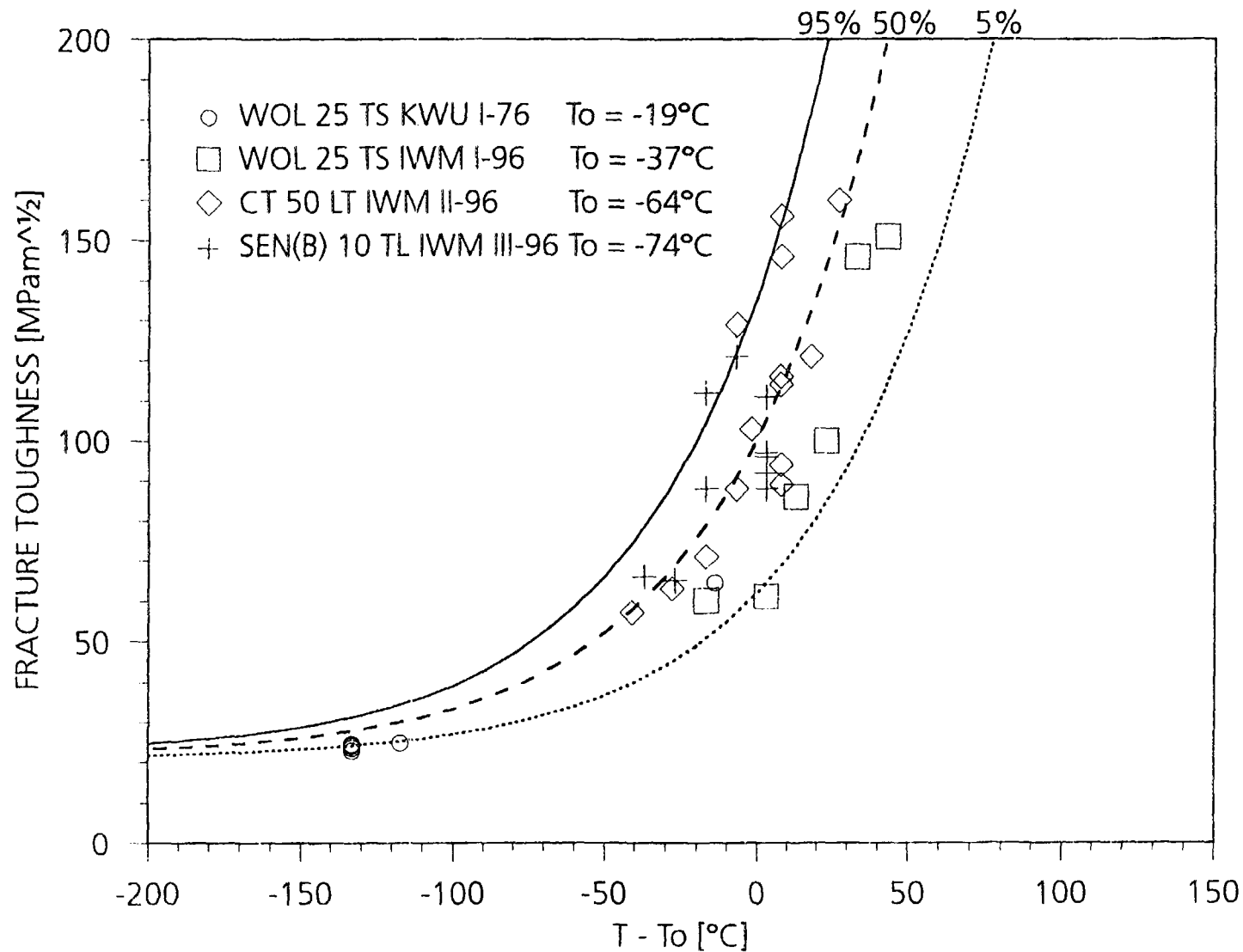
SIF's for subclad crack



KKS-WM, AP 609 · unirradiated · KWU I - 76 and
IWM I, II, III - 96 · ASME K_{Ic}: RTK_{Ic} = -71°C

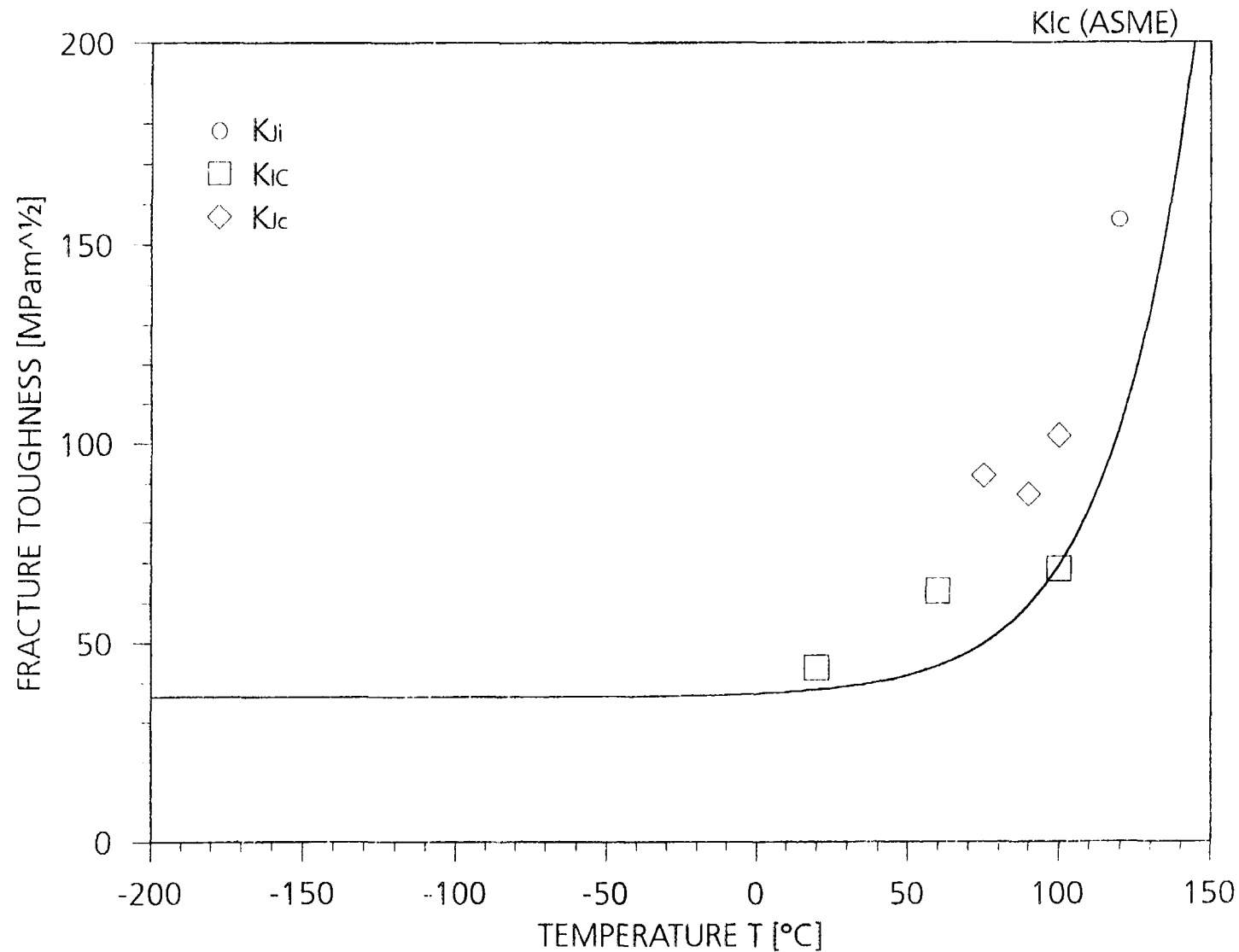


KKS-WM, AP 609 · unirradiated Master curve ($B = 25\text{mm}$) · $T_0 = -63^\circ\text{C}$

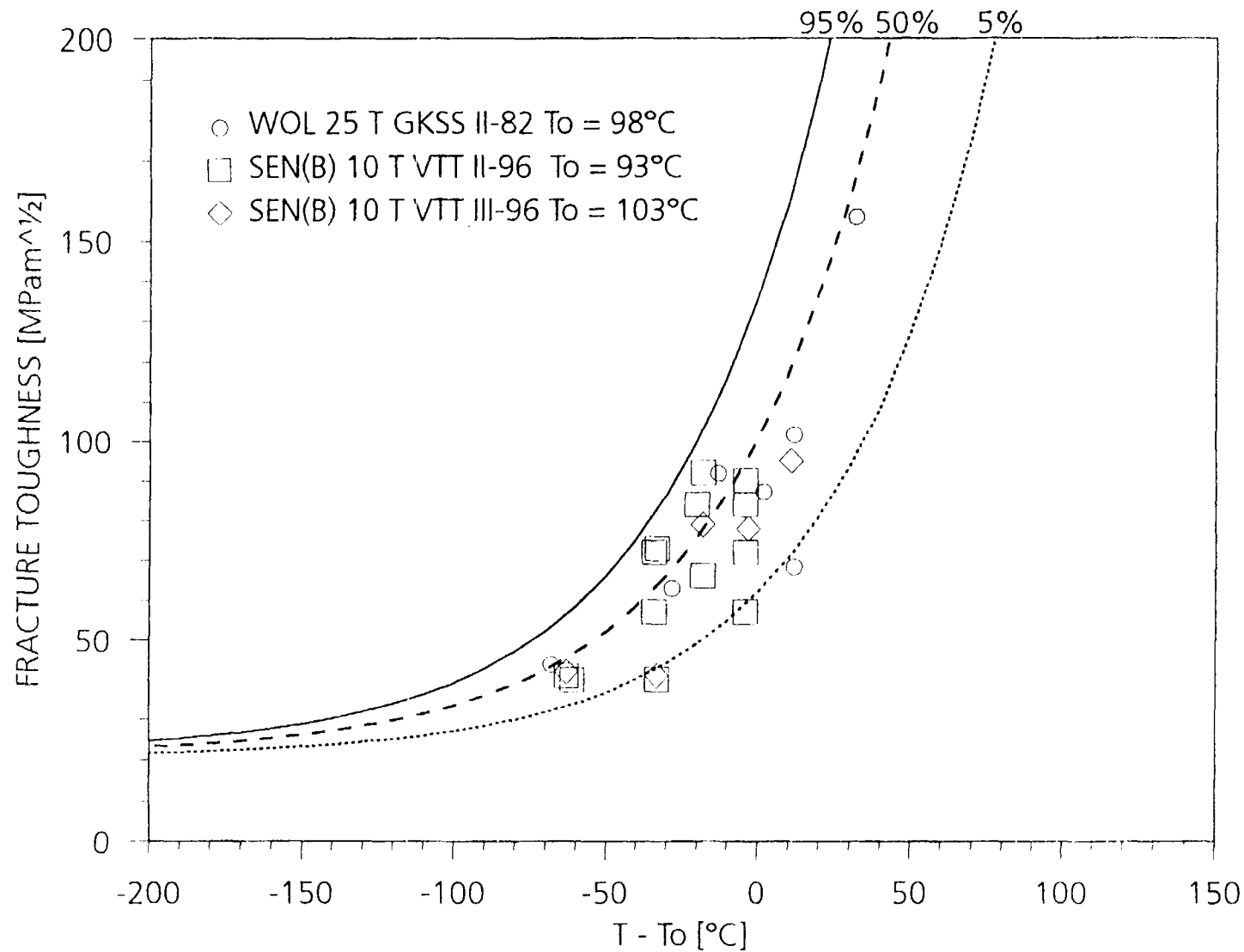


KKS-WM · GKSS II - 82 · 1,7E19 n/cm²

ASME K_{Ic}: RT_{K_{Ic}} = 90°C



KKS-WM · GKSSII-82, VTTII, III-96 · $1,7E19 \text{ n/cm}^2$
 Master curve ($B = 25\text{mm}$) · $T_0 = 96^\circ\text{C}$

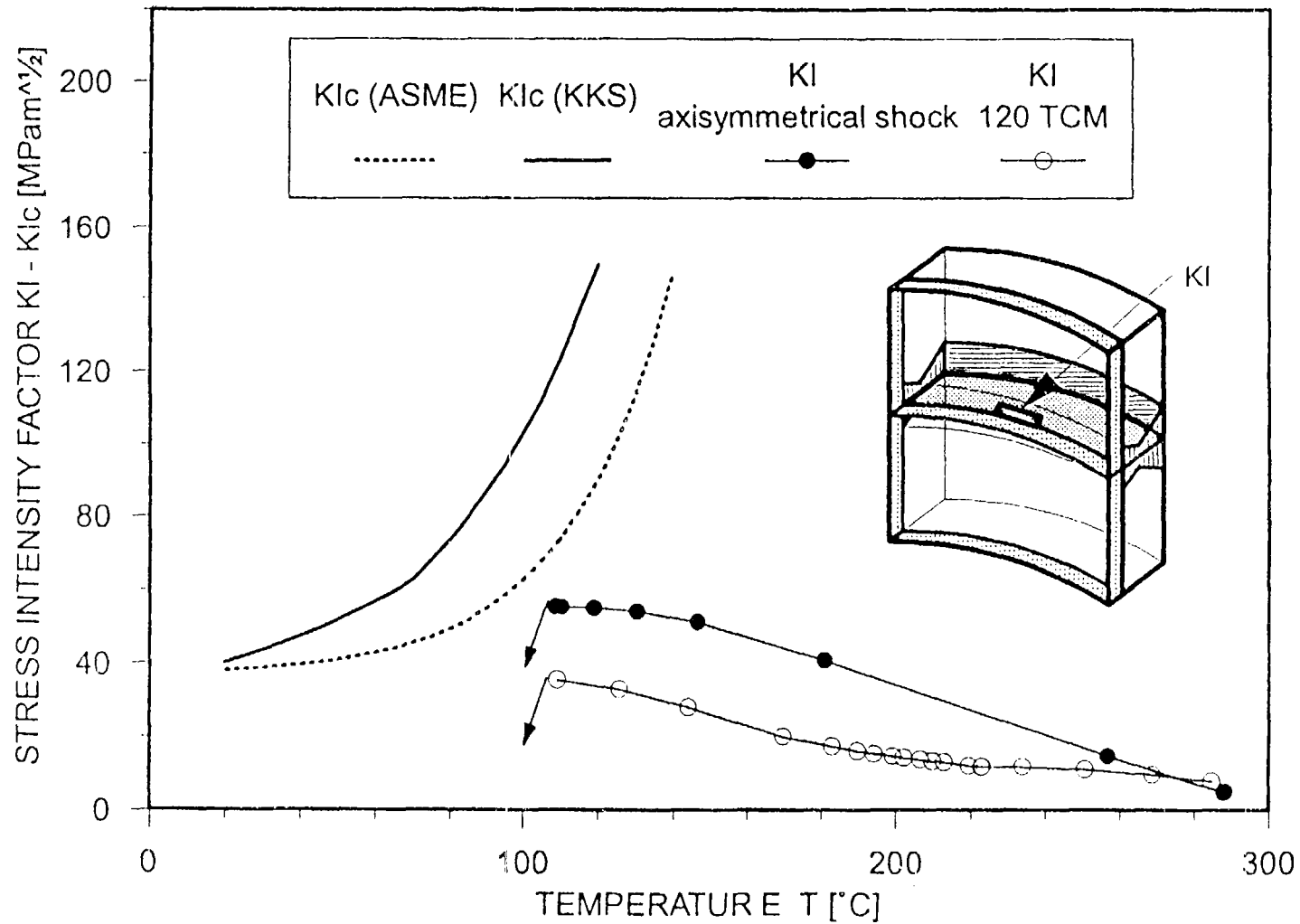


KKS-transients

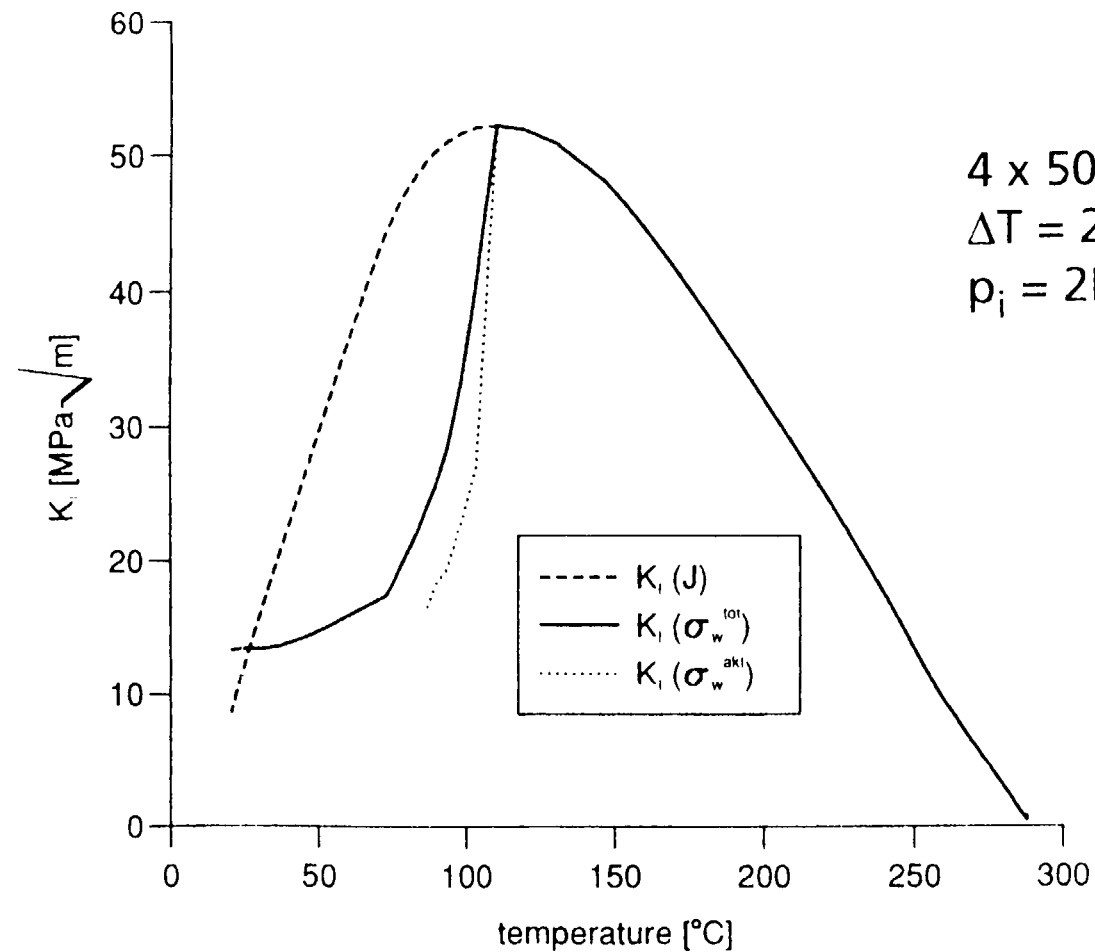
KIc for weld, $\Phi = 1.7E19 \text{ n/cm}^2$, defect: $2a = 4 \text{ mm}$, $2c = 50 \text{ mm}$

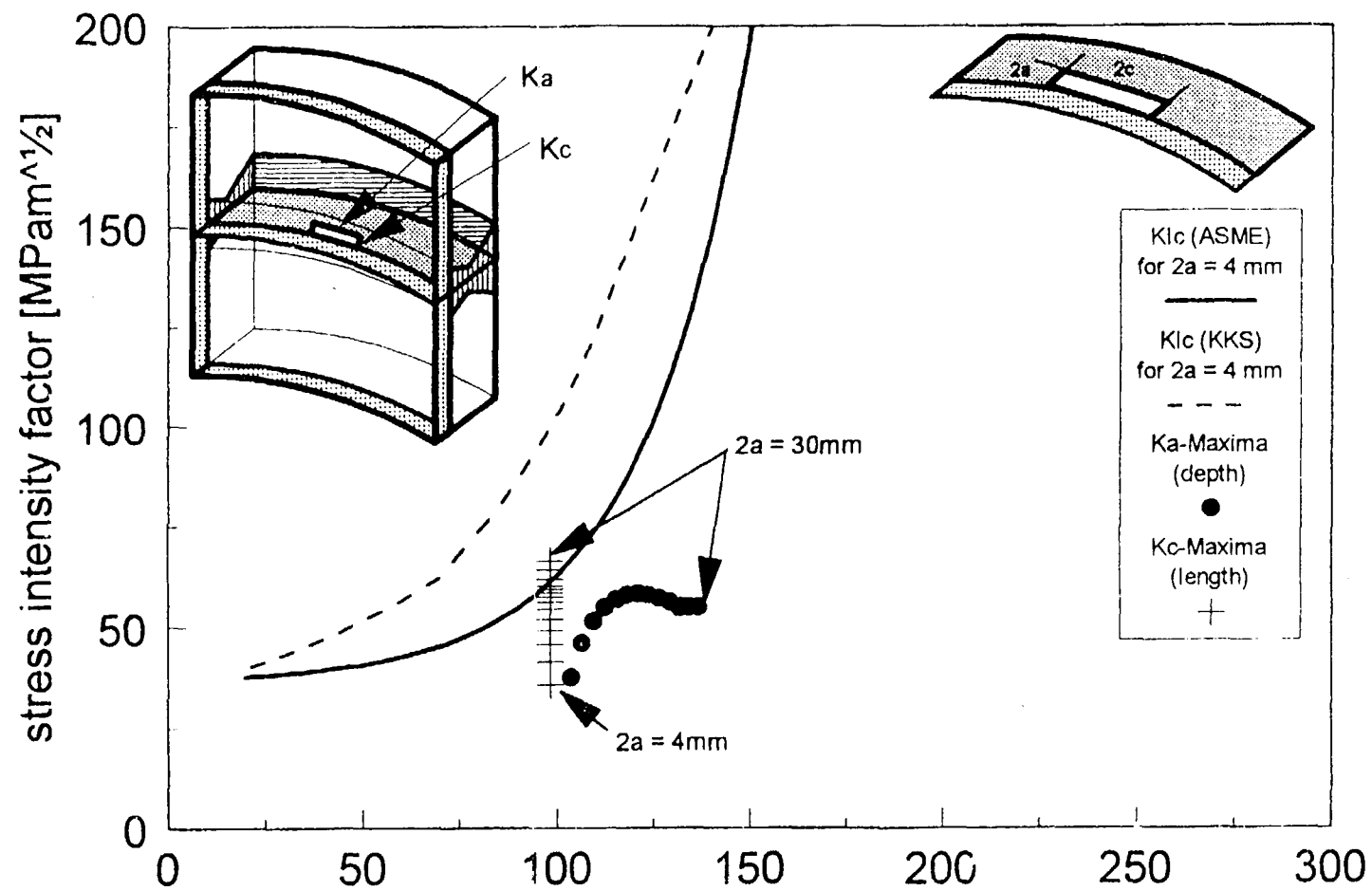


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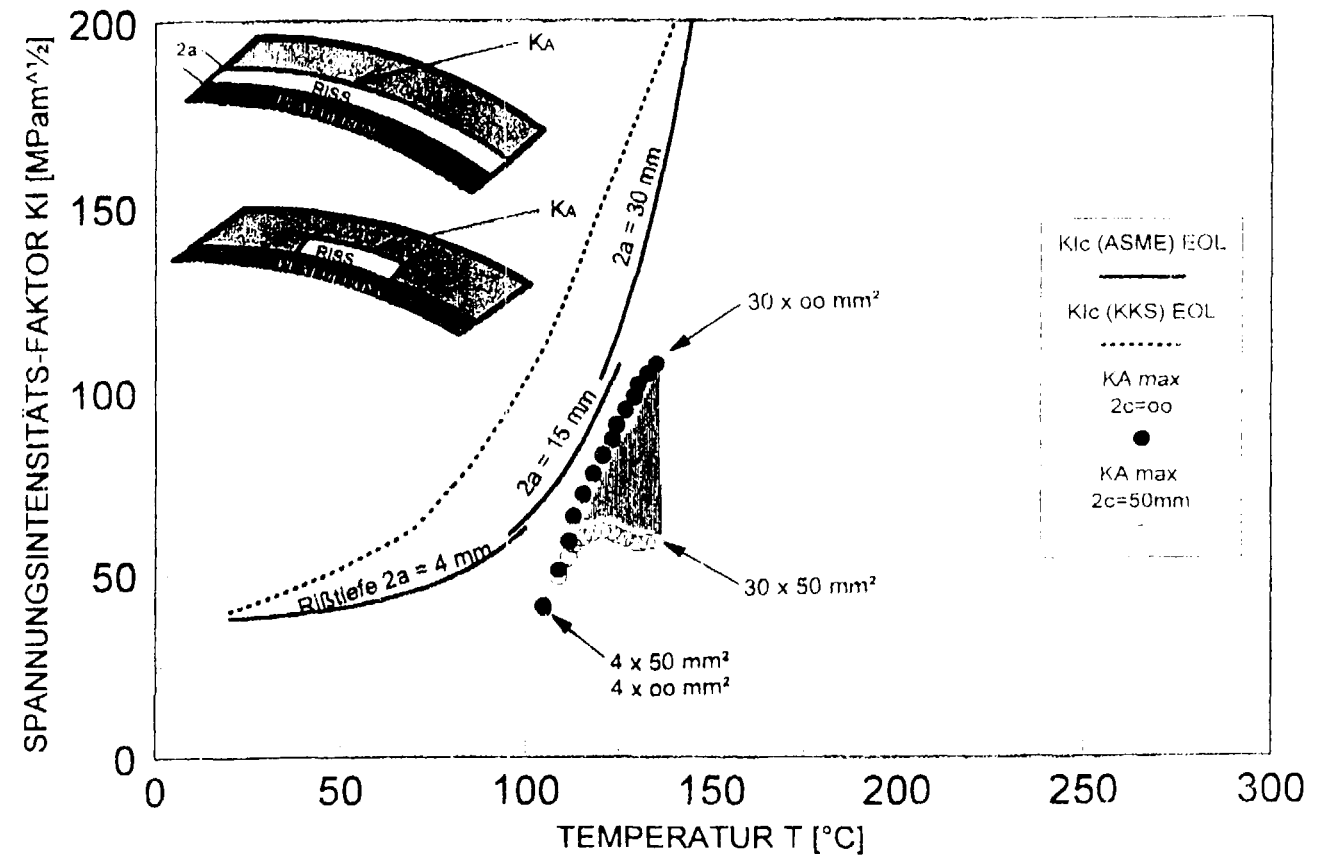


Evaluation of Weibull stresses for LOCA transient

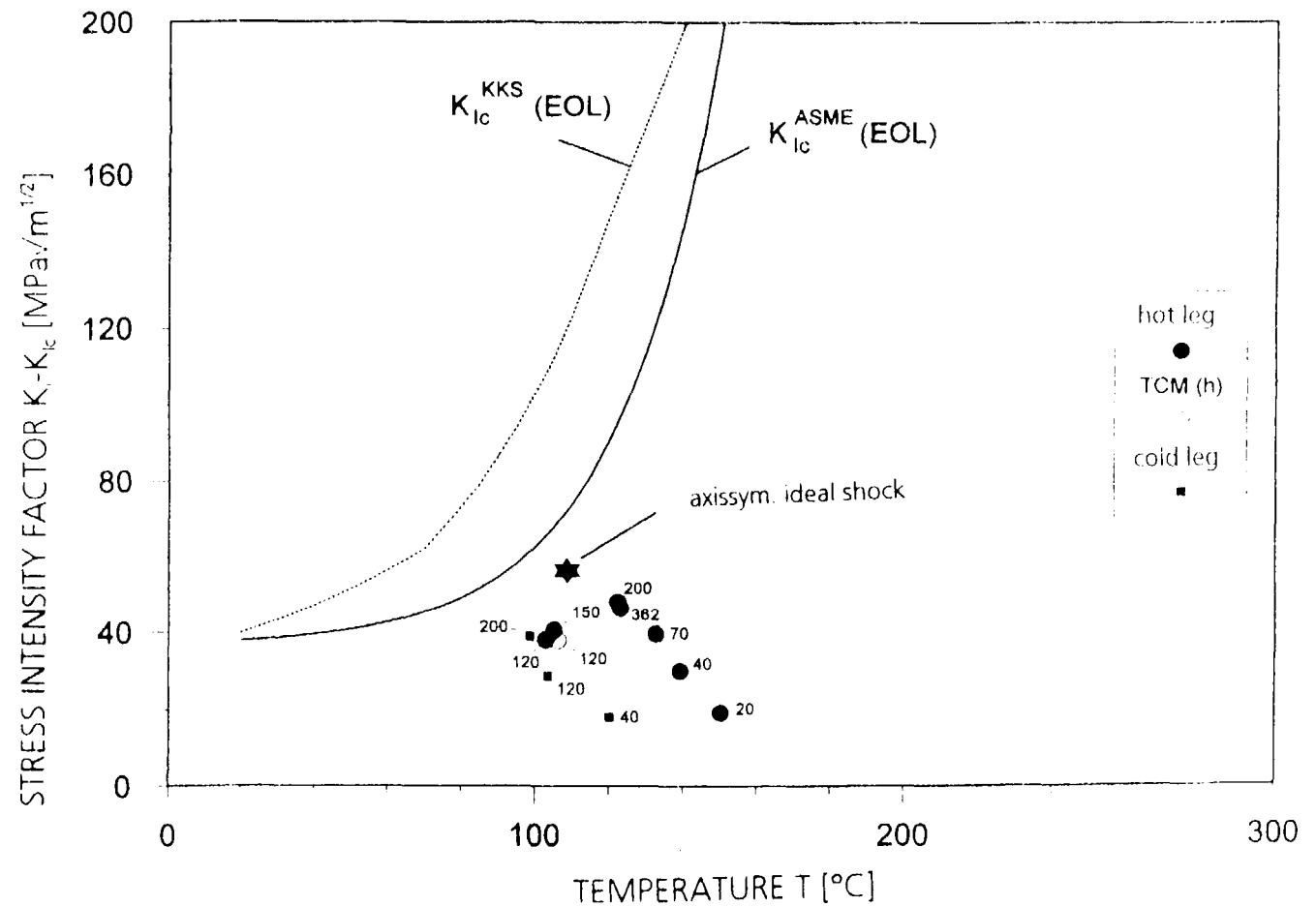




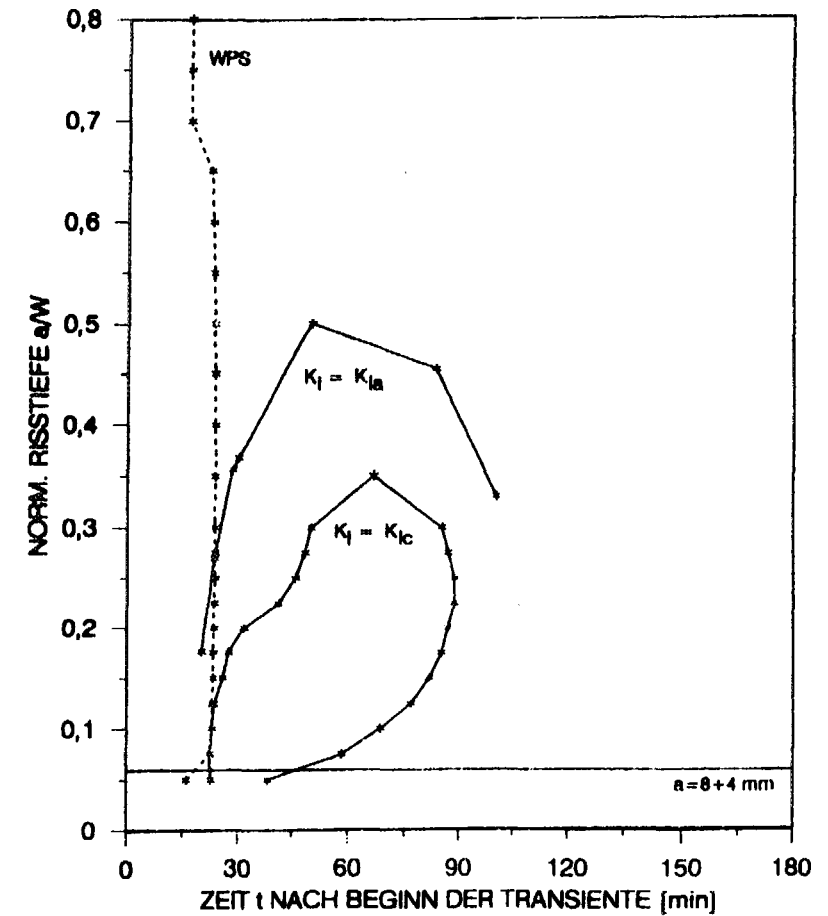
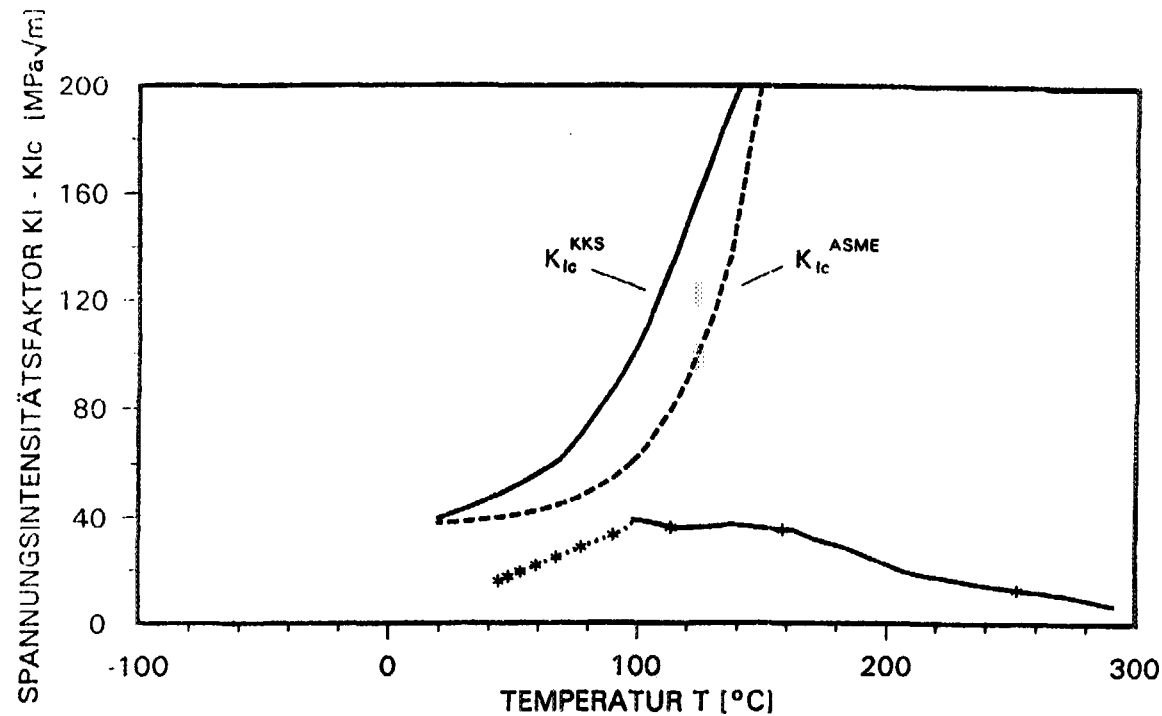
Safety margin for subclad cracks under »TCM 120 cm² (h) «



Load path maxima for KKS LOCA-transients (4x50 mm² subclad crack)

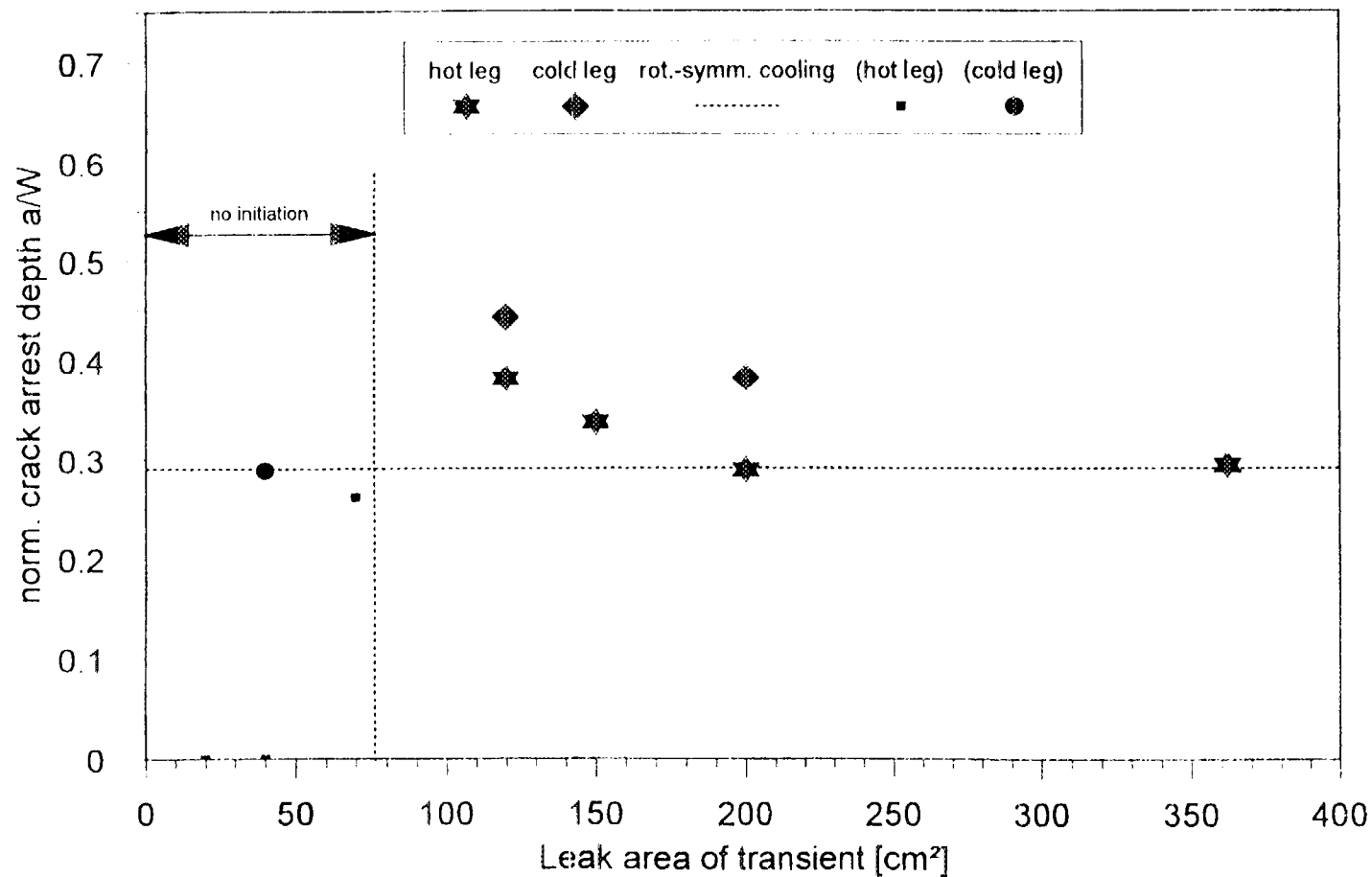


KKS-transient 200 cm² (h) Initiation- and Arrestdiagram for EOL

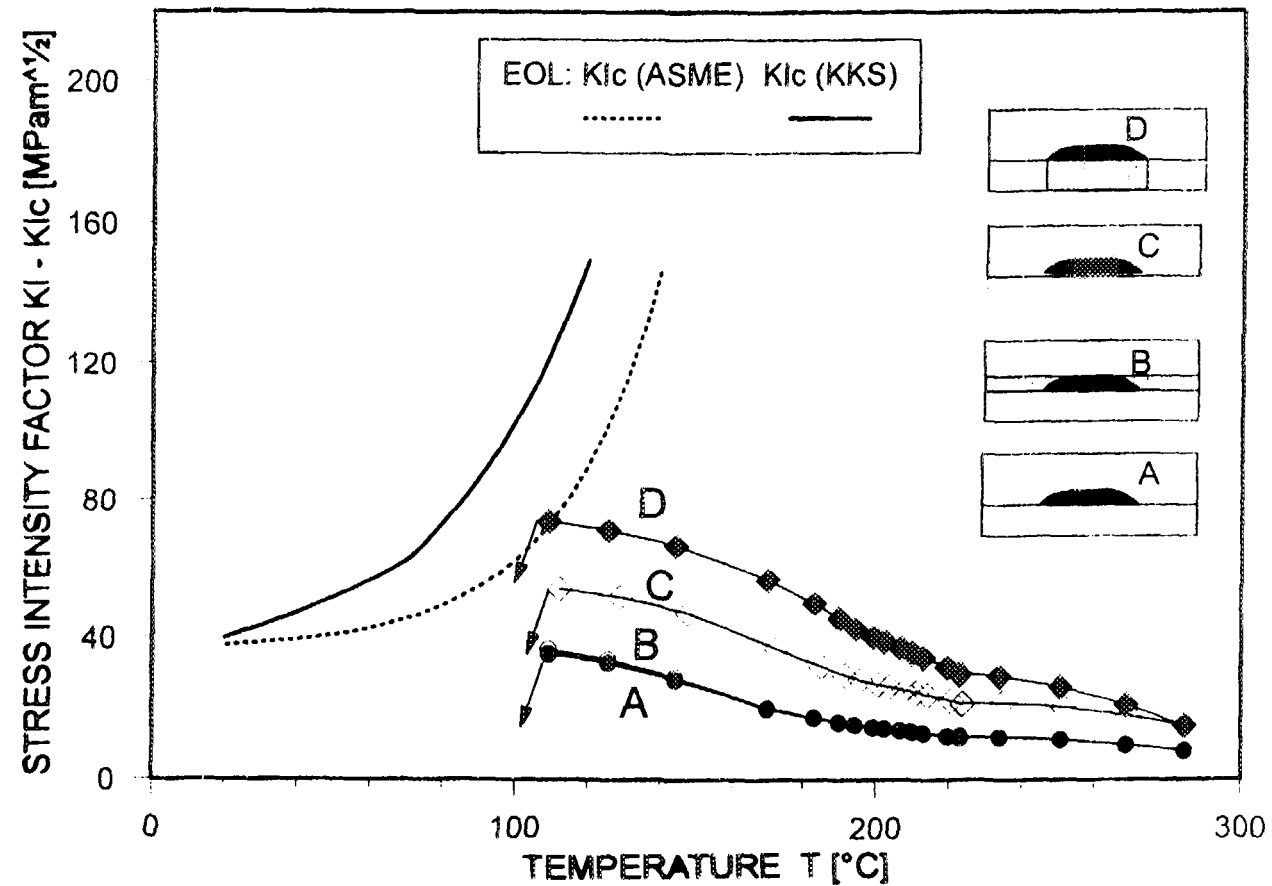


KKS LOCA-transients

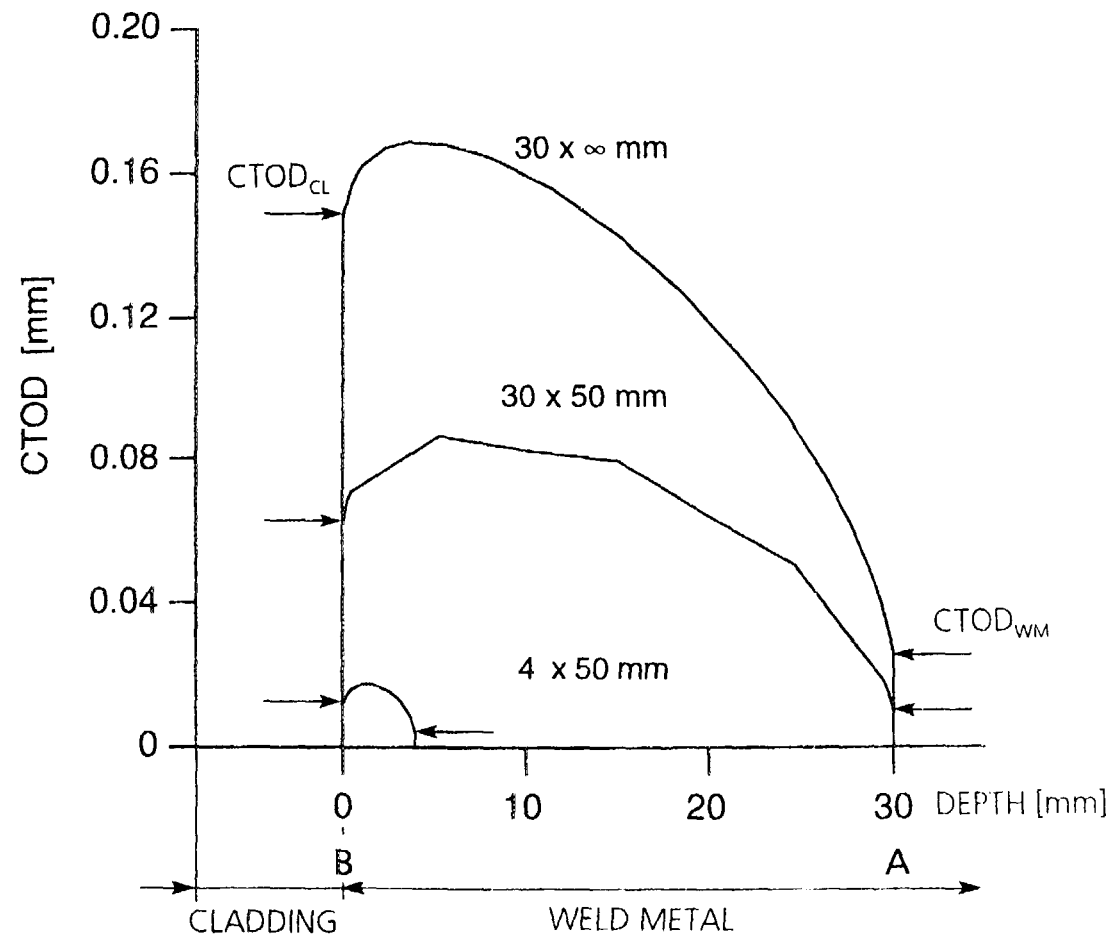
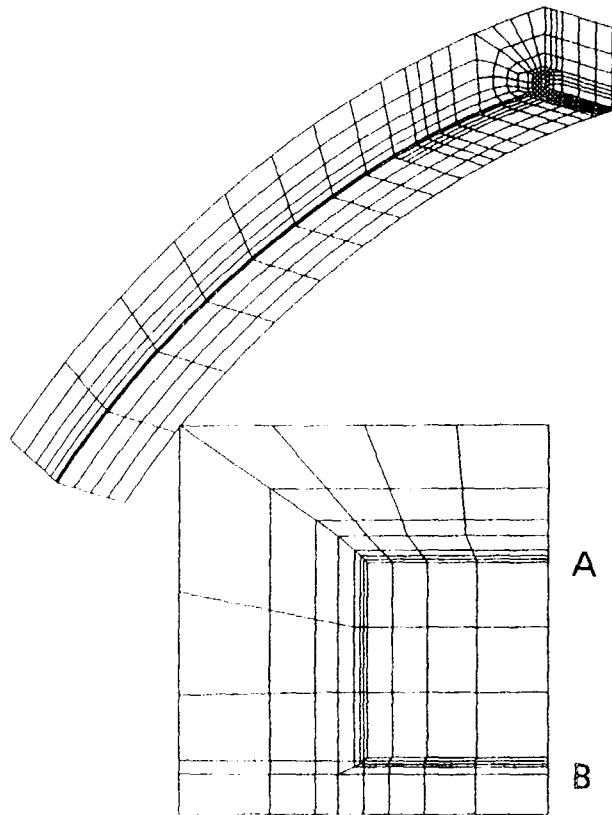
Latest arrest for postulated ∞ through clad cracks
EOL WM-toughness, WPS



KKS-transient TCM 120 (h) postulated crack: 4 x 50 mm², EOL

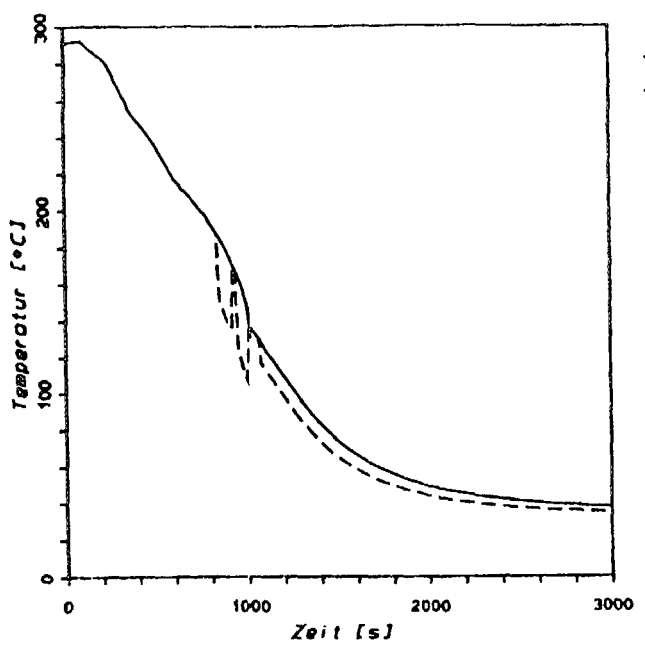


Crack tip opening displacement CTOD for subclad cracks under TCM 120 (h)

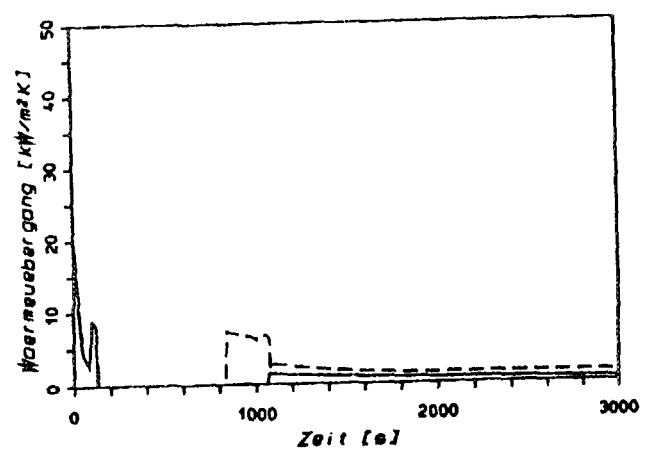
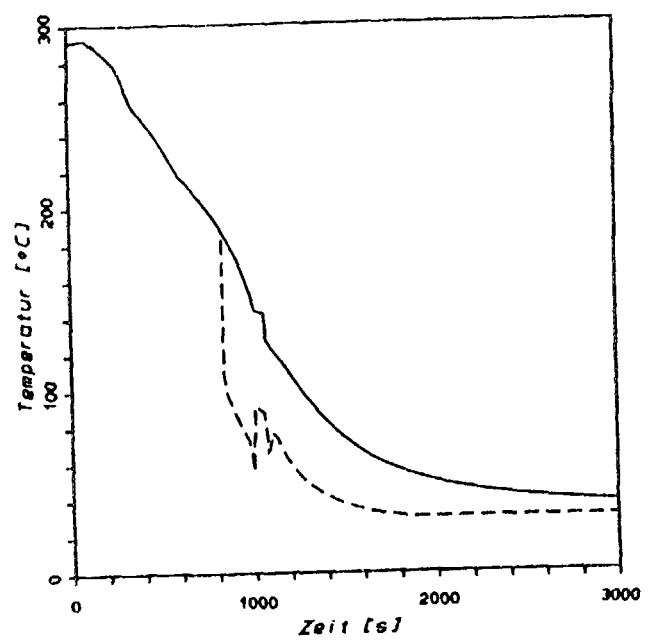
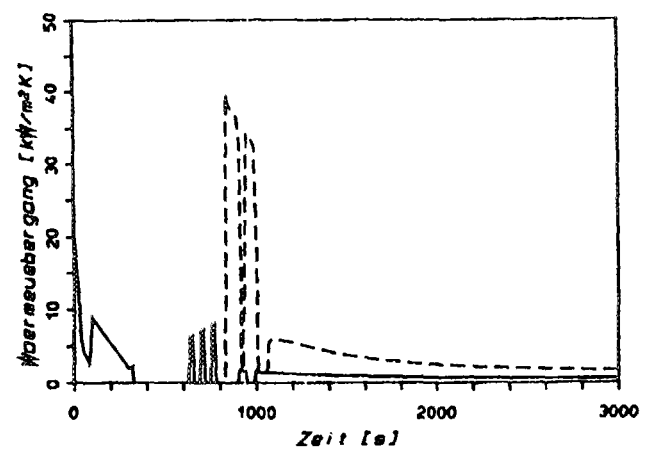


LOCA-transient »KKS-200 cm² (h)«

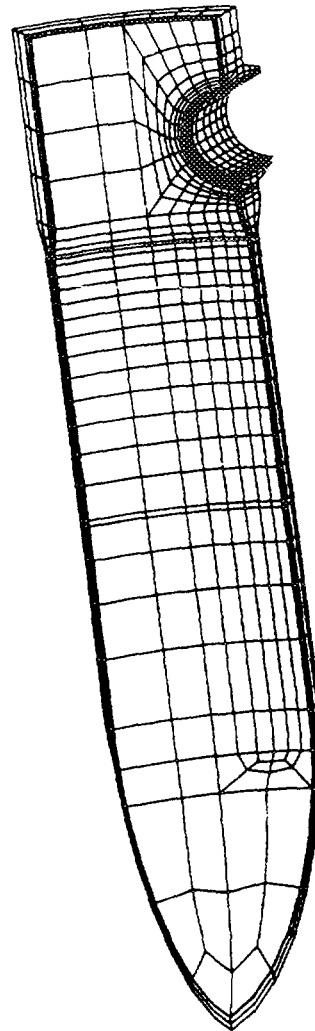
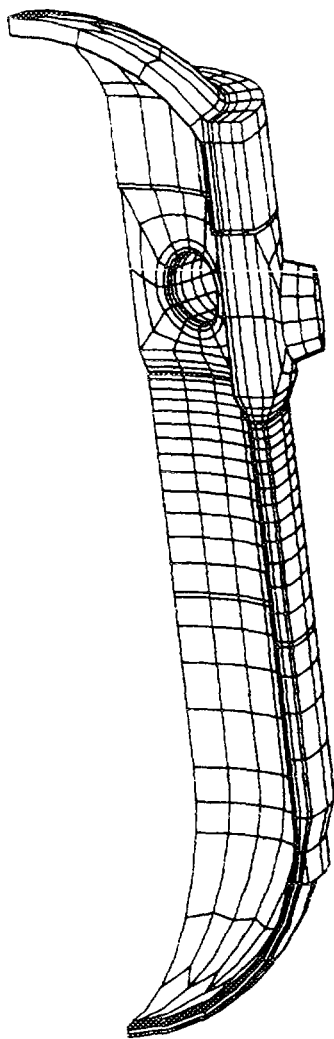
— Ring
- - - Strip



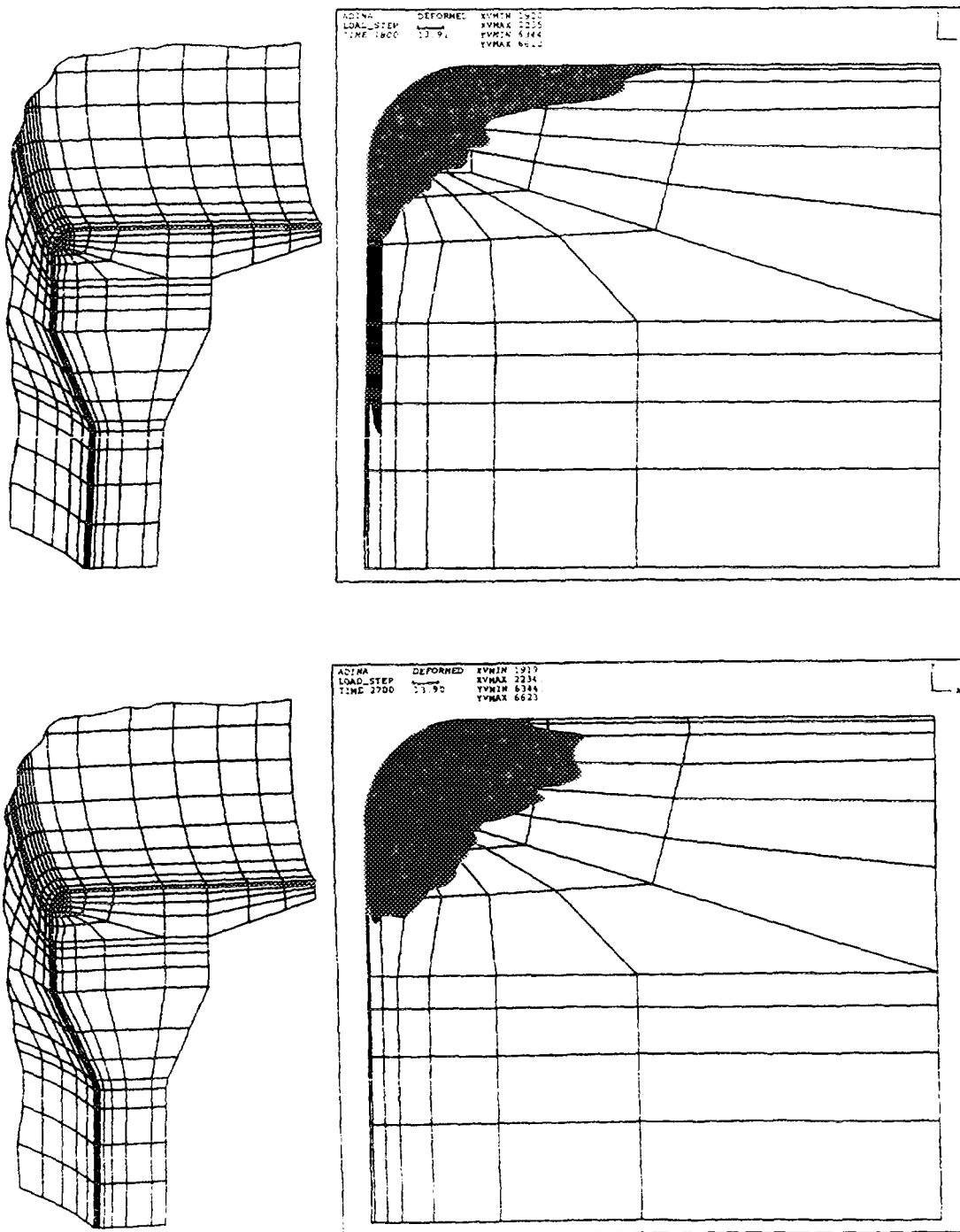
— Ring
- - - Strip



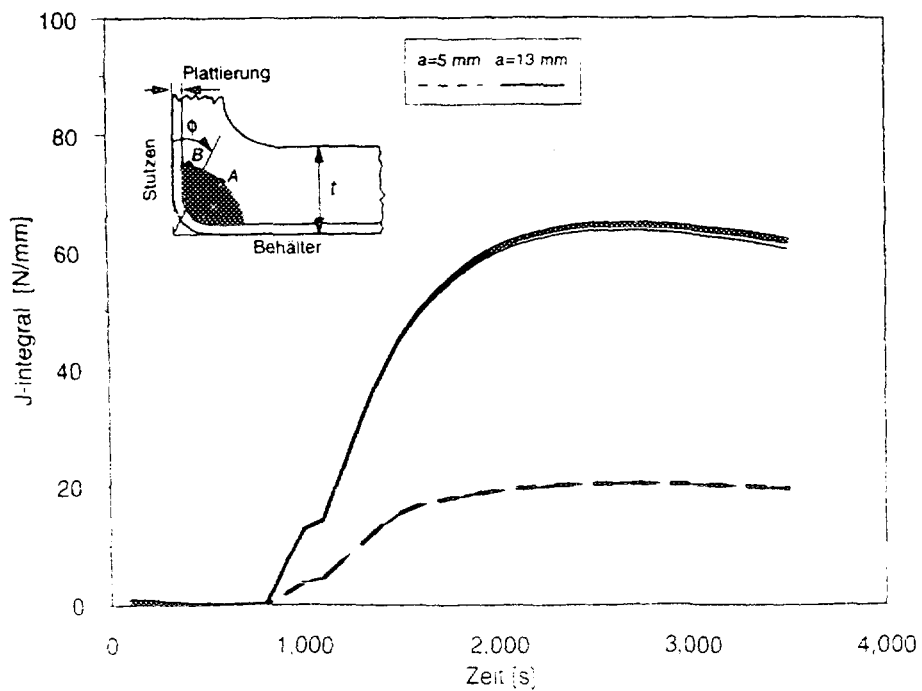
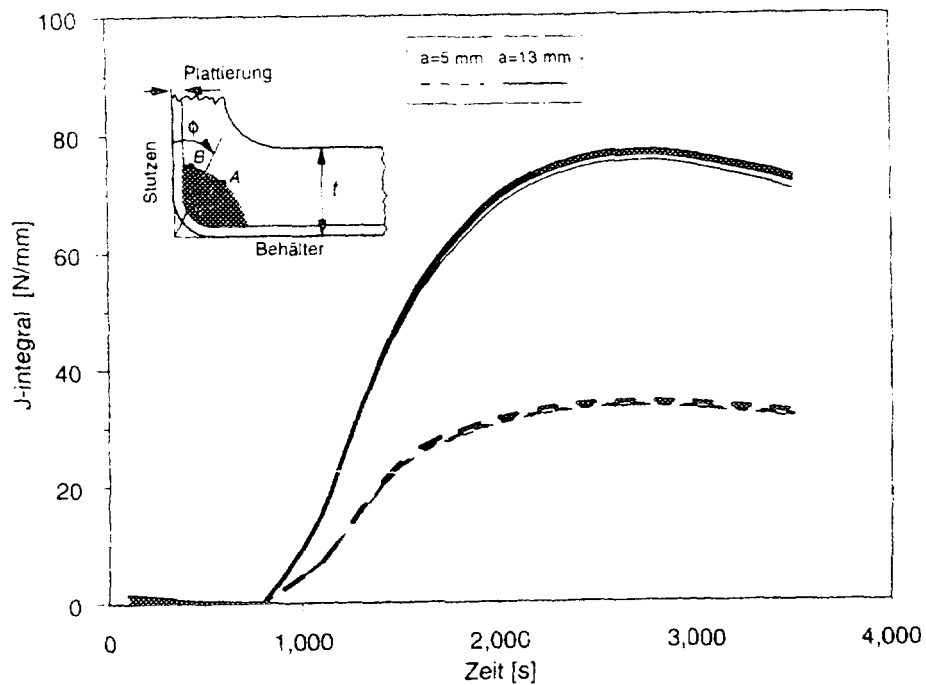
FE-models of RPV Stade for nozzle analysis



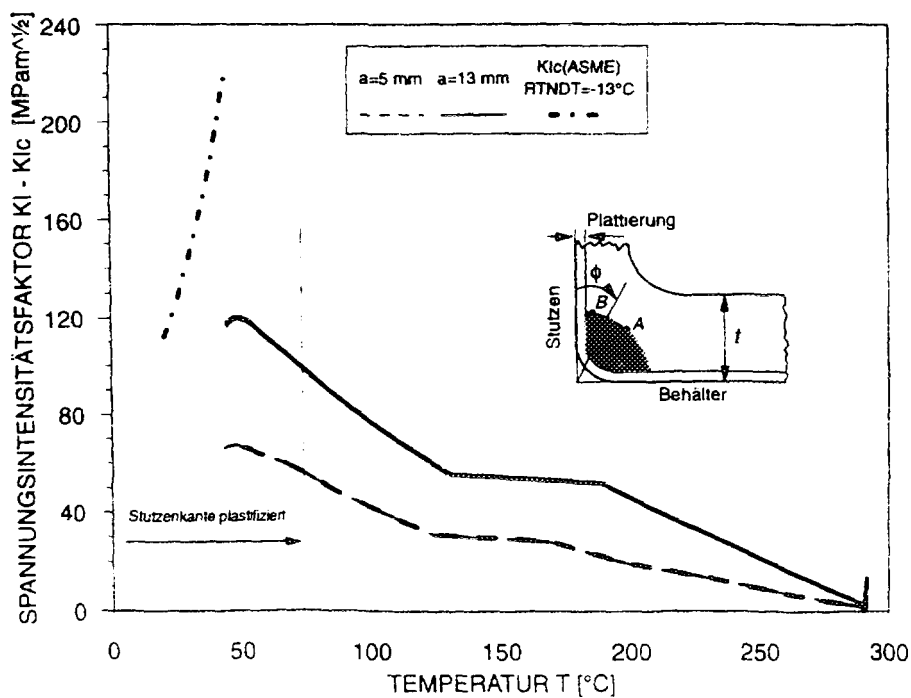
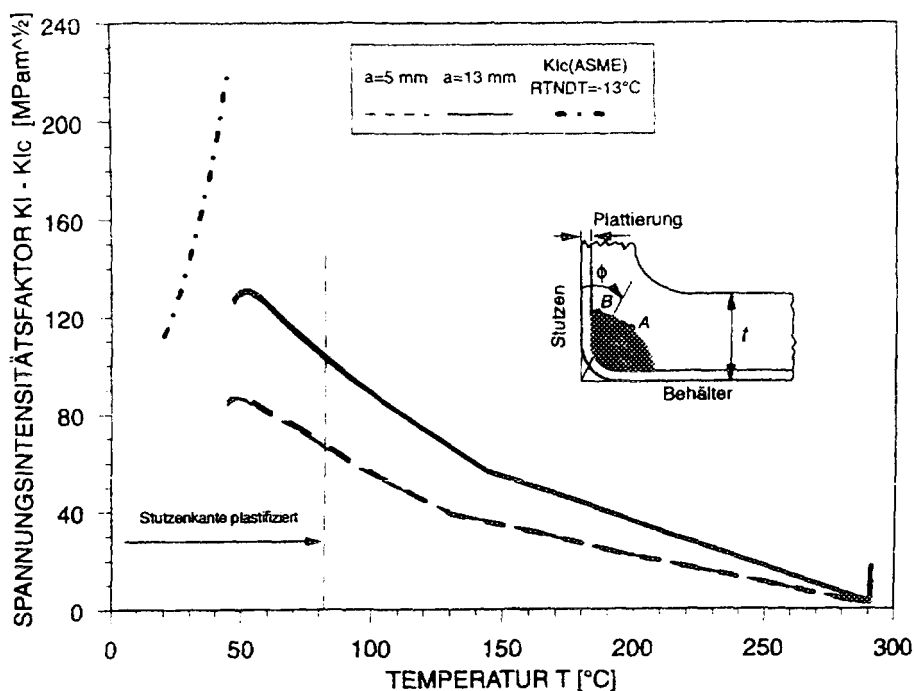
Plastic zones at nozzle corner for LOCA »200 cm² (h)«



Crack tip loading $J(t)$ for nozzle corner cracks during LOCA-transient »KKS-200 cm² (h)«



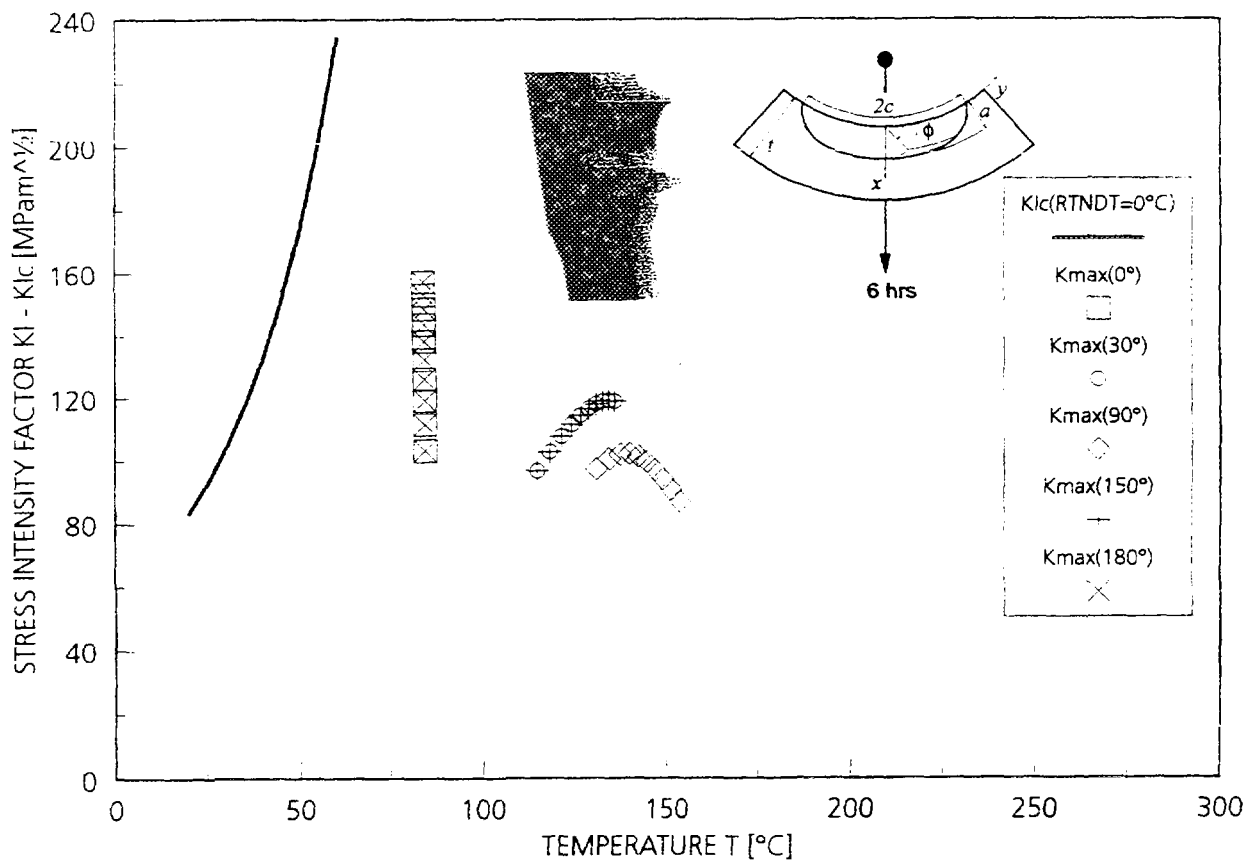
Crack tip loading $K(T)$ for nozzle corner cracks under LOCA-transient »KKS-200 cm² (h)« at EOL



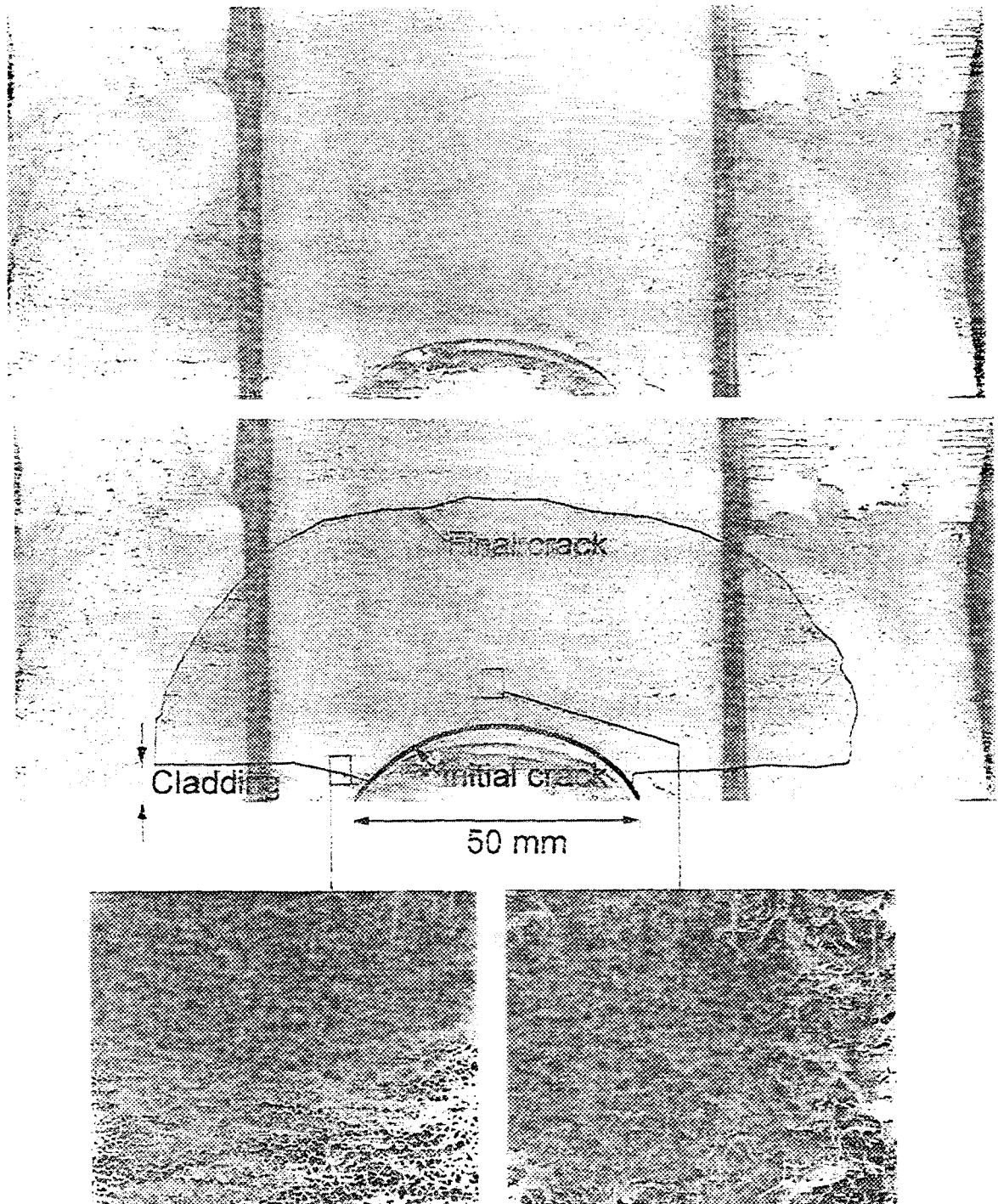
KKS • LOCA-transient »150 cm² (h)«, EOL

maxima of load paths for circumferential (through cladding)

cracks in nozzle pipe at position 6 hrs (HKL moment, res. stresses)



Fracture surface of plate 2.



SUMMARY

- Strong **effect of cladding** on the behaviour of near surface defects in RPV
- **Analytical methods** verified by FE provide accurate SIF's for through clad and subclad cracks
- **Cost effective** analysis possible for relevant spectrum of defect postulates and LOCA-transients, incl. **strip/plume** cooling
- Weibull-stress evaluation quantifies the beneficial effect of **Warm Prestressing (WPS)** on exclusion of crack initiation and finite depth crack arrest
- **Small specimen approaches** (damage mechanics/local approach and master-curve) suited to verify and complete data base of unirradiated and irradiated RPV materials



UPDATED KKS SAFETY ANALYSIS FOR EOL

based on

latest **nde status** and relevant defect postulates

UPTF calibrated, plant specific **thermohydraulics**

reassessed and completed material **toughness data**

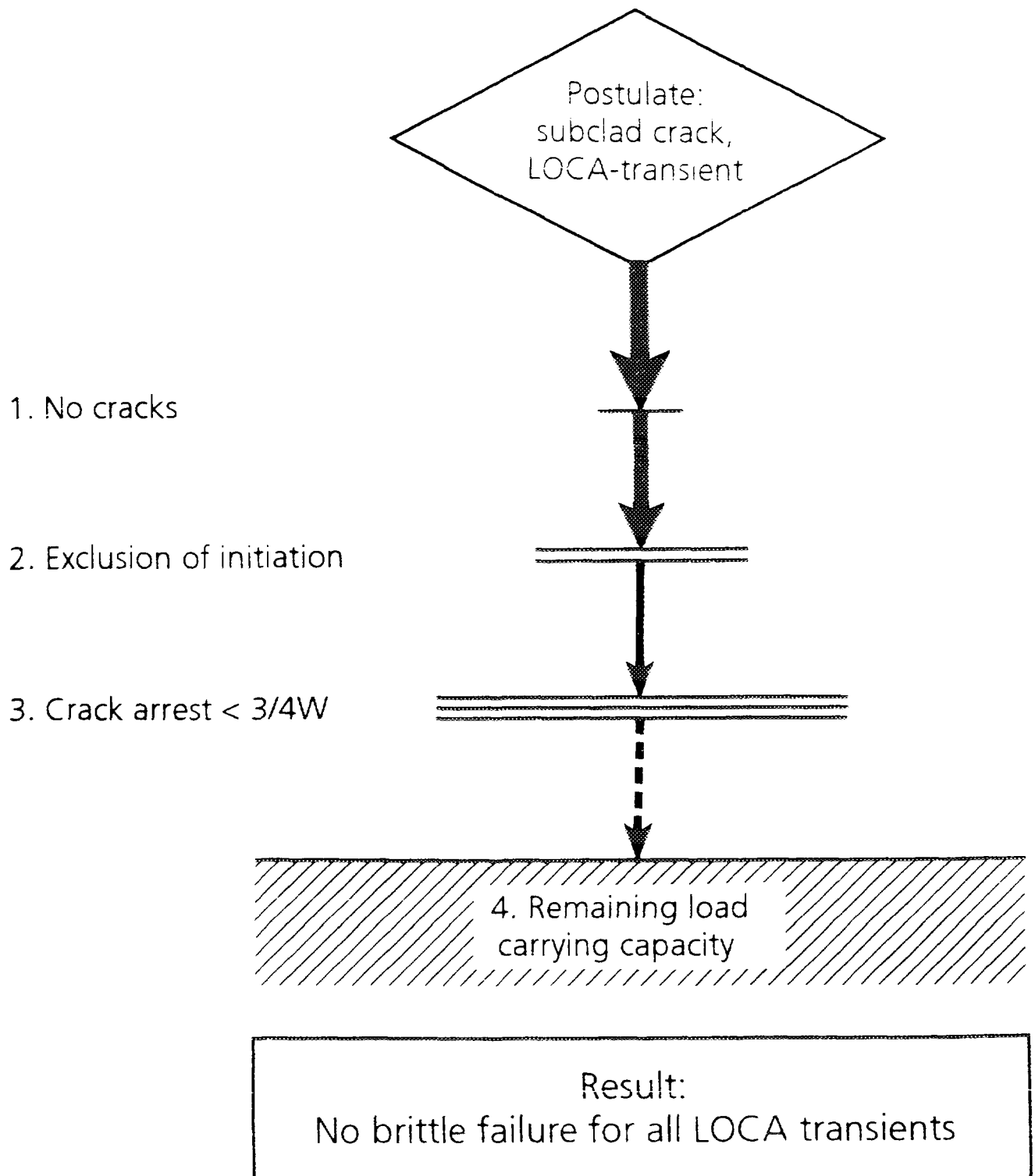
advanced 3D elastic-plastic **fracture mechanics**

UPDATED KKS SAFETY ANALYSIS FOR EOL

has approved

- High margin of **safety against initiation**
in the core weld (1. KTA/ASME criterion)
- Hypothetical initiation of infinitely long surface cracks in the belt line region will lead to final **crack arrest within less than 50 %** of the wall thickness (2. KTA/ASME criterion)
- More than two times normal operation pressure as remaining **load carrying capacity; leak-before-break**
- Crack growth on the **cladding side** excluded for relevant crack sizes
- Exclusion of initiation of postulated defects in **nozzle regime**

Multibarrier-Safety-Concept for RPV Stade



dated: 11/06/20



Fraunhofer Institut
Werkstoffmechanik



AN ALTERNATIVE METHOD FOR PERFORMING PRESSURIZED THERMAL SHOCK ANALYSIS

by

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ABSTRACT

In 1987, the NRC issued Regulatory Guide (RG) 1.154, which provides an analytical methodology to determine if adequate margins against reactor pressure vessel failure exist during Pressurized Thermal Shock (PTS) events when the vessel is predicted to exceed the PTS screening criteria. The RG 1.154 methodology is a complex analysis procedure involving probabilistic risk assessment (PRA), thermal-hydraulic and probabilistic fracture mechanics (PFM) evaluations. Following the shutdown of the Yankee Rowe Plant in 1992, the NRC committed to revising the PTS criteria and regulatory guidance in RG 1.154 with the goal of providing a definitive approach and analysis methodology. In 1995, EPRI initiated a program aimed at developing an alternative approach that would simplify the PFM analysis procedure and be economically efficient to implement without requiring the resolution of a substantial number of technical issues.

The fundamental concept of this alternative approach is that there is a relationship between the calculated probability of crack initiation (POI) and calculated critical crack depth (a_c). The critical crack depth is defined as the smallest flaw depth at which initiation would first occur during the specified PTS event. This is because a_c and POI are both influenced by the same parameters, such as RT_{NDT} , fracture toughness (K_{IC}), and the transient temperatures and pressures. POI was computed using PFM techniques and a_c was computed using deterministic fracture mechanics for a wide range of representative PTS transients.

Using this relationship, acceptable PTS transient frequency was then correlated with critical crack depth for a mean frequency of 5×10^{-6} per reactor year, which is specified as an acceptable frequency of significant flaw extension in RG 1.154. This correlation establishes the acceptability (or unacceptability) of the PTS event and can also be used to simply evaluate mitigative changes in transient frequency and/or severity that might result from a combination of plant operational, equipment, or system modifications.

This paper describes how POI and acceptable PTS frequency were correlated with a_c and summarizes several example applications, including evaluation of potential plant modifications. Plans for an industry supported pilot-plant application of the alternative PFM method for RG 1.154 are also discussed.

Keywords: Reactor Vessel, PTS, Probabilistic Fracture Mechanics, Risk Analysis

BACKGROUND

Reactor pressure vessels (RPVs) are generally considered to be the most critical component in the nuclear plant aside from the reactor core. The vessel is also the one major component that may limit the useful life of the nuclear plant because it is the heart of the nuclear steam supply system and, if it had to be replaced, an extraordinary amount of time and money would be required.

Reactor vessels undergo an aging phenomenon, called embrittlement, that is caused by the exposure of the RPV material to high energy neutron flux from the core. The area most likely to be affected by neutron embrittlement is the beltline region of the RPV due to its proximity to the core. Embrittlement is characterized by a gradual reduction in the RPV's fracture toughness and an increase in the material's strength. If this reduction in toughness were to continue to progress, and a crack existed, fracture of the vessel might be predicted to occur under postulated events such as pressurized thermal shock (PTS). For this reason, the U.S. Nuclear Regulatory Commission (NRC) established the PTS rule and defined screening criteria on reference nil-ductility transition temperature (RT_{NDT}) for PTS (RT_{PTS}) of 270°F for axial welds, plates and forgings and 300°F for circumferential welds. If the RT_{PTS} is predicted to exceed the screening criteria during the licensed operating period, an analysis must be performed to justify operation beyond the screening criteria.

In 1987, the NRC issued Regulatory Guide (RG) 1.154 [1], which provides a methodology to determine if adequate margins against failure exist during PTS events when the RT_{NDT} is predicted to exceed the PTS screening criteria. The methodology consists of a very complex analysis procedure involving probabilistic risk assessment (PRA), thermal hydraulic and fracture mechanics evaluations. In addition to determining the probability of vessel failure, this methodology also was intended to be used as a tool to evaluate mitigative actions that would reduce the risk of vessel failure. These actions include fluence reduction or changes in plant operations, equipment and/or systems.

Yankee Atomic Electric Company was the first organization to formally use RG 1.154 to justify operation of the Yankee Rowe vessel beyond the PTS screening criteria. In 1990, the NRC requested Yankee to perform a bounding PTS analysis because of concerns regarding the embrittlement of the RPV. The NRC reviewed the analysis and concluded that the calculated probability of vessel failure could vary considerably because of the inputs and assumptions used in the fracture mechanics analysis. After an extensive research and analysis program, the uncertainty associated with demonstrating the acceptability of vessel integrity following a PTS event ultimately contributed to the permanent shutdown of the plant. Subsequently, the NRC [2] committed to revise the criteria and guidance in RG 1.154 with the goal of providing a definitive approach and analysis methodology that would resolve the uncertainties associated with the technical issues. In 1995, the Electric Power Research Institute (EPRI) initiated a program aimed at developing a simplified method for evaluating PTS. Ultimately it is EPRI's intent to have this approach incorporated in the revised version of RG 1.154 either as a preferred method of evaluation or as an alternative to the NRC methodology.

RG 1.154 [1] requires the following steps in a vessel PTS risk analysis (see Figure 1): 1) A PRA is used to determine the probability of occurrence for the most severe PTS-type transients. This will result in a ranking of PTS event frequencies. 2) A thermal hydraulic analyses is performed to establish the pressure and temperature time histories for the various transient events. 3) A probabilistic fracture mechanics (PFM) analysis is performed to determine the conditional probability of vessel failure for each of the transients. 4) The summation of the product of event frequencies and conditional probability of vessel failure (risk) must be shown to be less than a through-wall crack penetration mean frequency of 5×10^{-6} per reactor year.

The selection of the input values and assumptions in the PFM analysis, which the industry benchmarking study report by EPRI [3] showed can have a significant effect on the calculated vessel failure probability, has been debated for several years. Issues concerning flaw size, the number of flaws and their distribution within the pressure vessel are just a few examples. EPRI decided to focus the effort on developing an alternative approach that would simplify the PFM analysis procedure and be economically efficient to implement without requiring the resolution of a substantial number of technical issues. It is important to point out that the selection of input values used in the probabilistic and deterministic analyses does require concurrence from the NRC. Therefore, irrespective of which analysis method is selected for RG 1.154, the inputs and assumptions must be well defined.

DEVELOPMENT OF METHOD

The fundamental concept of this alternative approach is that there is a relationship between the calculated probability of crack initiation (POI) and calculated critical crack depth (a_c). The critical crack depth is defined as the smallest flaw depth at which initiation would first occur on the flaw boundary during the specified PTS event. This is because a_c and POI are both influenced by such parameters as RT_{NDT} , fracture toughness (K_{IC}), and the transient temperatures and pressures, to name a few. For example, as the transient severity increases (higher pressures and lower temperatures), a_c becomes smaller and POI becomes larger because the stresses and resultant crack driving forces at the inner surface of the pressure vessel are larger, thus causing smaller crack sizes to initiate and propagate through the vessel wall. The opposite scenario is also true. Similarly, as RT_{NDT} increases, fracture toughness decreases resulting in lower values of a_c . Given this fact, it was determined that a_c is actually a very good measure of transient severity relative to the degree of embrittlement in the vessel. The development of the alternative PFM method using this concept is described in an EPRI interim technical report [4] and summarized below.

There were three major objectives defined for the development of the alternative PFM method including: (1) the procedure should be a well defined, easy to use deterministic computational method, (2) the uncertainties associated with probabilistic fracture mechanics method used to develop the correlation should be reduced, and (3) the procedure should be correlated to the results obtained by the NRC for the PTS screening criteria [5].

The calculated value of critical crack depth was selected as the deterministic parameter for the alternative PTS method. This is analogous to the use of RT_{NDT} in the PTS screening criteria. However, critical crack depth is a more robust parameter in that it accounts for all the major irradiation and material related parameters, in addition to the effects of transient specific temperatures and loads. The 1995 version of the FAVOR Code from ORNL [6] was selected as the computational tool to calculate critical crack depth. FAVOR was selected because it is relatively easy to use, can model any specified transient temperature and pressure time history, and automatically calculates critical crack depth as a function of aspect ratio at various locations along the crack boundary.

RT_{PTS} is used in the critical crack depth calculation because it is an already determined, well defined value. This reduces the uncertainty associated with variation in irradiated material toughness properties and alloy content. To ensure that the effect of variable changes or implementation of mitigative measures would not be masked by combinations of conservatively combined variables, the mean fracture initiation toughness (in combination with the more conservatively defined RT_{PTS}) was used to define the material resistance to crack initiation.

The 1995 version of the FAVOR Code [6], which was benchmarked with other industry PFM analysis codes [3], was also used to calculate the vessel failure probability. This reduced the uncertainty because the models (e.g. stress intensity factor correlations) were the same for both the deterministic and

probabilistic analyses. Several analysis conditions that were also selected to reduce the uncertainty associated with PFM analysis included the following. First, the probability of crack initiation was used in the correlation rather than the probability of failure. This eliminates the uncertainty associated with crack arrest phenomena and through wall variations in material conditions that affect vessel failure predictions. Second, a randomly sampled aspect ratio was used in the probability of initiation computations. This reduces the error and uncertainty associated with selecting a single aspect ratio that an EPRI study [7] shows does not adequately model the fracture initiation conditions for a broad spectrum of transient conditions (EPRI, 1995a). Third, a flaw tolerance approach is used where only one flaw located at the vessel inner surface is used to determine initiation probability. This reduces the uncertainty associated with the number and location of the postulated flaws.

The alternative PTS method was also developed so that there is a connection with the NRC PTS risk study [5] using the results from the evaluation of the extended high pressure injection (HPI) transient. According to the FAVOR baselining of the NRC PTS risk study by ORNL [8], this limiting transient would be characterized as having a final temperature of 125°F, an exponential decay rate of 0.05 min.⁻¹ and a maximum pressure of 2,250 psi. The failure probability in the NRC PTS risk study for the HPI transient and a mean surface RT_{NDT} of 210°F (RT_{PTS} = 270°F) was approximately equal to 6x10⁻² for six flaws (one in each longitudinal weld). Because cladding effects were included in the fracture mechanics model used for the alternative PTS evaluation procedure but were not included in the NRC analysis, an adjustment to the FAVOR input was needed to match the calculated failure probability. The variable selected for adjustment was the flaw distribution because it cannot be easily measured on a plant specific basis, has a relatively high degree of uncertainty, and is selected rather arbitrarily from the results of various studies that report widely differing results. The flaw distribution was adjusted until the probability calculated by FAVOR for one flaw was equal to the value obtained for one flaw in the NRC PTS risk study [5]. The cumulative flaw distribution that provided a benchmarking with the NRC PTS risk study results was then used in the PFM analysis performed to define the correlation between critical flaw critical flaw depth and probability of crack initiation.

Six representative PTS transients, ranging from mild to severe pressure-temperature conditions, were used to establish the correlation between POI and a_c . Table 1 defines the PTS transients and frequency ranges that were used in the development of the alternative PFM method. The temperature history for a postulated PTS transient was stylized to fit an exponential decay form and the pressure was assumed to be constant with time:

$$T(t) = T_f + (T_0 - T_f) e^{-\beta t} \quad (1)$$

$$P(t) = P_{max} \quad (2)$$

where: $T(t)$ = Coolant temperature (°F) at any time t (minutes),
 T_0 = Initial temperature, normally 550 °F,
 T_f = Final temperature (°F),
 β = Exponential decay rate (min.⁻¹)
 $P(t)$ = Pressure (psi) at any time t (minutes) and
 P_{max} = Maximum pressure (psi) of concern.

To define a credible set of postulated PTS transients for the development of the alternative PFM method, the bounding plant-specific results of the Westinghouse Owners Group (WOG) PTS risk study [9] were used. This was done because the transient severity parameters and failure probabilities in this study were consistent with those already used in the NRC PTS risk study [5]. The limiting PTS transient with the highest failure probability that was selected in each frequency range in Table 1 could be characterized by

low final temperatures and moderate (lower) pressures. However, there were also transients that induced comparable vessel failure probabilities but had lower event frequencies. To broaden the range of applicability, these transients were also included just in case their plant-specific frequencies were increased into the frequency range of concern. These transients in Table 1 are characterized by moderate (higher) final temperatures and high pressures.

Calculation of the critical depth required selection of the flaw aspect ratio. To determine the aspect ratio that was best suited to construct the correlation, a sensitivity study was performed to assess the effect on critical crack depth of aspect ratios of 2:1, 6:1, 10:1 and infinite. The results showed that crack initiation consistently occurred at the same location (surface) for the range of transient conditions in Table 1 when the 2:1 aspect ratio was used. Consequently, a 2:1 aspect ratio was selected to construct the correlation between critical crack depth and probability of crack initiation.

The critical crack depths at a 2:1 aspect ratio and initiation probabilities for one longitudinal and one circumferential flaw were calculated using the benchmarked cumulative flaw distribution for each transient in Table 1 and for three different values of RT_{PTS} (270, 280 and 290°F for longitudinal flaws and 300, 310 and 320°F for circumferential flaws). The resulting initiation probabilities as a function of critical crack depth are shown in Figure 2. These are called *probability index curves* because the critical crack depth is not a measured value but is calculated depending upon the severity of the transient and degree of embrittlement. Since the initiation probability also depends upon these same factors, calculated critical crack depth can be used as an index to calculate initiation probability directly. Equations were derived from a least-squares curve fit of the data shown in Figure 2. For one longitudinal flaw, the relationship between probability of initiation POI_l and calculated critical crack depth a_c is:

$$POI_l = \text{EXP}(6.558707 a_c^2 - 14.55218 a_c - 2.711547) \quad (3)$$

For one circumferential flaw, the relationship between probability of initiation POI_c and calculated critical crack depth a_c is:

$$POI_c = \text{EXP}(7.087541 a_c^2 - 16.1988 a_c - 2.733123) \quad (4)$$

What this means to the utility user is that equations (3) and (4) or Figure 2 can be used to estimate vessel probabilities of initiation based only on the deterministically calculated value of critical crack depth. Detailed probabilistic fracture mechanics (PFM) analyses are not required because the probability index curves of Figure 2 were derived from the results of one comprehensive PFM analysis that is generic to all reactor vessels and that has been benchmarked to the NRC's PTS risk study [5].

EXAMPLE APPLICATION

To apply the alternative PFM method, the risk contribution of a plant-specific PTS event can be compared with the PTS risk limit by using the following equation:

$$\text{Risk}_i = F_i * POI_i(a_c) \quad (5)$$

where: Risk_i = the risk contribution for PTS transient i.
 F_i = frequency per year for PTS event i and
 $POI_i(a_c)$ = initiation probability as a function of critical crack depth.

Figure 3 graphically shows the transient frequencies that give different risk contributions as a function of the deterministically calculated critical crack depth for longitudinal flaws. A similar figure has also been

developed for circumferential flaws. In Figure 3, four regions are shown: 1 (Below dotted curve for risk of 5×10^{-8} per year) - Since each PTS transient in this region would have a risk contribution less than 1% of the risk limit, many PTS transients in this region would be acceptable. 2 (Between dotted curve and dashed curve for risk of 5×10^{-7} per year) - Each PTS transient in this region would have a risk contribution between 1% and 10% of the risk limit. Therefore, less than 10 PTS transient in this region should be acceptable. 3 (Between dashed curve and solid curve for risk limit of 5×10^{-6} per year) - Each PTS transient in this region would have a risk contribution between 10% and 100% of the risk limit. If there are several PTS transients in this region, their acceptability relative to the total PTS risk limit should be calculated using equation (3) and evaluated. 4 (Above the solid curve for the PTS risk limit) - Since each PTS transient in this region would exceed the PTS risk limit, any PTS transients in this region would be unacceptable and remedial actions would be required.

The PTS risk limit for the evaluation is consistent with that given in RG 1.154 [1] and is equal to a through-wall crack penetration mean frequency of 5×10^{-6} per reactor year. The solid curve of Figure 3 thus reveals a graphical acceptance criterion whereby calculated values of transient frequency and a_c can determine the acceptability of the transient. This is termed the *transient index curve*. Having determined a_c based on the specific transient and embrittlement conditions and knowing the frequency of the transient from the PRA analysis, the data point establishes the acceptability (or unacceptability) of the PTS event. Values that lie above the curve are unacceptable and values that lie below the curve are acceptable. If the values are unacceptable, adjustments can be made in transient frequency and/or severity through a combination of plant operational, equipment, or system modifications. The severity adjustments will directly affect the calculated value of a_c . The analyst has only to modify the inputs of the deterministic analysis in order to define the conditions that would provide an acceptable result.

Figure 4 provides a simplified vessel risk evaluation for an example plant with three PTS transients having frequencies of 3.5×10^{-6} , 2.1×10^{-5} and 1.0×10^{-3} per year and a postulated longitudinal flaw in the limiting axial weld that has an RT_{PTS} value of 273°F. As shown in Figure 4, the transient with the lowest frequency produces a risk contribution less than 1% of the limit. The PTS transient with the intermediate frequency contributes about 1% of the risk limit, while the transient with the highest frequency contributes about 10% of the limit. Quantitative evaluation of the total PTS risk for the example plant showed it would be acceptable (15.8% of the risk limit) even if the PTS screening criteria of 270°F were exceeded.

As can be demonstrated using Figure 3, if the PTS risk limit of 5×10^{-6} /year is exceeded, there are two ways to reduce the risk. First, the frequency of the limiting PTS transient(s) can be reduced. For example, new procedures and training could be provided or new trip functions could be implemented (logic and/or hardware changes). Both of these types of changes would tend to reduce the chance (frequency) of the PTS transient occurring. The second way to reduce the risk is to increase the critical crack depth. The depth can be increased by reducing the degree of embrittlement, as measured by RT_{PTS} , by reducing the severity of the limiting PTS transient(s) or a combination of both of these. Examples of these types of reductions include shielding and/or fuel management to reduce fluence (embrittlement) or heating of the water in the refueling water storage tank to reduce the transient severity (specifically, to increase the final temperature).

An application of the alternative PFM method to evaluate changes in PTS transient frequency and severity is also provided in Figure 4 for an example plant. Even though it would not be required for this application, the benefits (risk reduction) of the following proposed changes were evaluated: 1) the final temperature for the two low-frequency PTS transients would be increased 15°F by heating of the water in the refueling water storage tank, 2) the frequency for the highest frequency transient would be reduced by a factor of 10 due to trip function logic and hardware upgrades and 3) flux reduction measures via fuel management would be implemented to reduce the RT_{PTS} value from 273 to 260°F. The combined effects

of these changes, as shown in Figure 4, would be to reduce the total PTS risk for the example plant vessel by more than a factor of 8. Based upon this type of information and the implementation costs for the proposed changes, the utility would then be able to make better decisions regarding which PTS mitigation options are cost effective.

CONCLUSIONS

The following are some initial conclusions resulting from this work as reported by EPRI [4].

- 1) A simplified fracture mechanics approach has been developed for evaluating PTS for vessels that are projected to exceed the NRC's embrittlement (PTS) screening criteria.
- 2) It has been shown that there is a relationship between allowable transient frequency and calculated critical crack depth. This relationship affords the analyst a quick way to assess the acceptability of the transient and assess potential plant modifications if the result is unacceptable.
- 3) The method is based on one comprehensive PFM analysis that is generic to all vessels. This, however, does require concurrence from the NRC with regard to the input values that were used in the analysis. Examples of inputs that need to be agreed upon include cladding effects, flaw density and distribution, fracture toughness curves, mandating the use of surface flaws, etc.
- 4) The acceptance criterion is based on a non-exceedance of probability of initiation, rather than non-exceedance of probability of failure which includes the uncertainties associated with crack arrest. This eliminates further uncertainty associated with the determination of how and when propagating cracks will arrest.
- 5) The alternative PFM method has not introduced any new fracture mechanics analysis techniques that are unfamiliar to the NRC.
- 6) The alternative PFM method has been benchmarked to the NRC's PTS risk study [5] and compared to an actual plant evaluation recently performed by the NRC. The results show that the technique is consistent and compatible with the results of those studies.

FUTURE PLANS

It is EPRI's intent to quantify the benefit of this methodology through an application of a detailed pilot study for a PWR. The goals of this pilot study, which is being supported by the Westinghouse Owners Group, are: 1) show that the analysis can be economically implemented, 2) identify potential improvements to plant operations, equipment and/or systems to lessen the risk of RPV failure due to a PTS event and 3) demonstrate that the vessel can indeed be operated safely at levels of embrittlement in excess of the current PTS screening criteria. The results of this study will be documented in a future EPRI report.

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Table 1
Definition of Realistic Transients for the Alternative PFM Method

Type of Transient	Transient Parameter	Value for Transient Frequency Range of:		
		$10^{-2} - 10^{-3}$ /yr	$10^{-3} - 10^{-5}$ /yr	$10^{-5} - 10^{-7}$ /yr
Currently Limiting Transients (Low Temp. and Pressure) in the WOG PTS Risk Study [9]	Final Temp.	206 °F	117 °F	97 °F
	Rate β	0.10/min.	0.08/min.	0.16/min.
	Max. Pressure	1500 psi	1100 psi	1100 psi
Potentially Limiting Transients if Frequency Increases Significantly (Higher Temp. and Pressure)	Final Temp.	218 °F	142 °F	117 °F
	Rate β	0.15/min.	0.08/min.	0.08/min.
	Max. Pressure	2250 psi	1500 psi	2000 psi
Note: Transients with frequency $> 10^{-2}$ per year would have final temperatures > 270 °F and be covered by the NRC PTS risk study [5]. Transients with frequency $< 10^{-7}$ per year would have minimal contribution to PTS risk.				

Figure 1
Flow Chart for Vessel PTS Risk Analysis

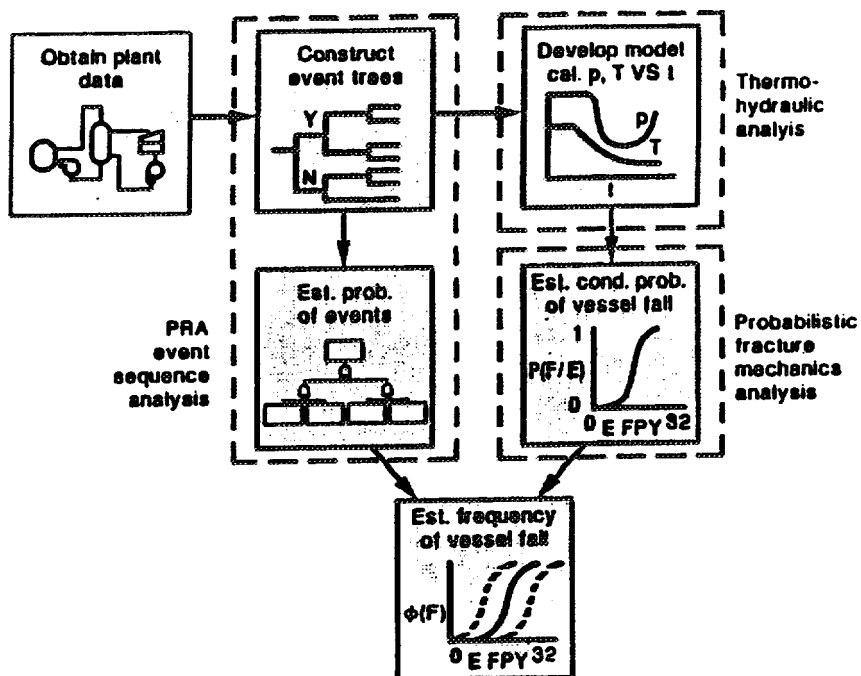


Figure 2
Alternative Method Probability Index Curves

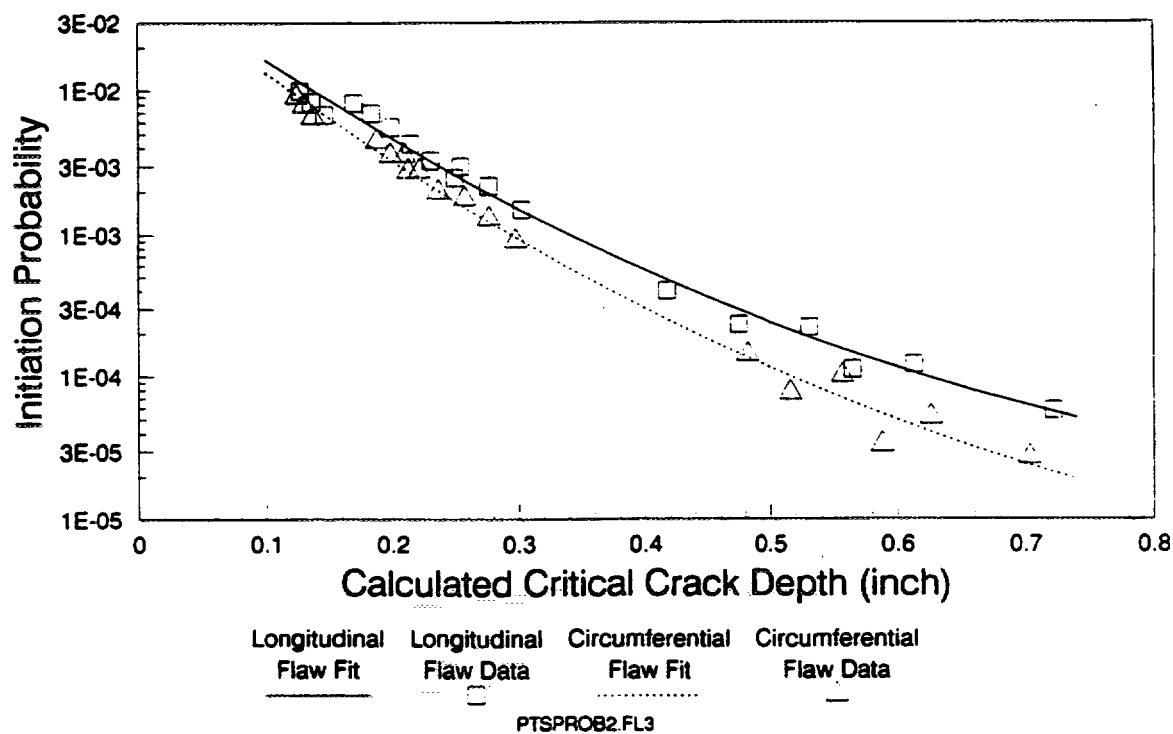
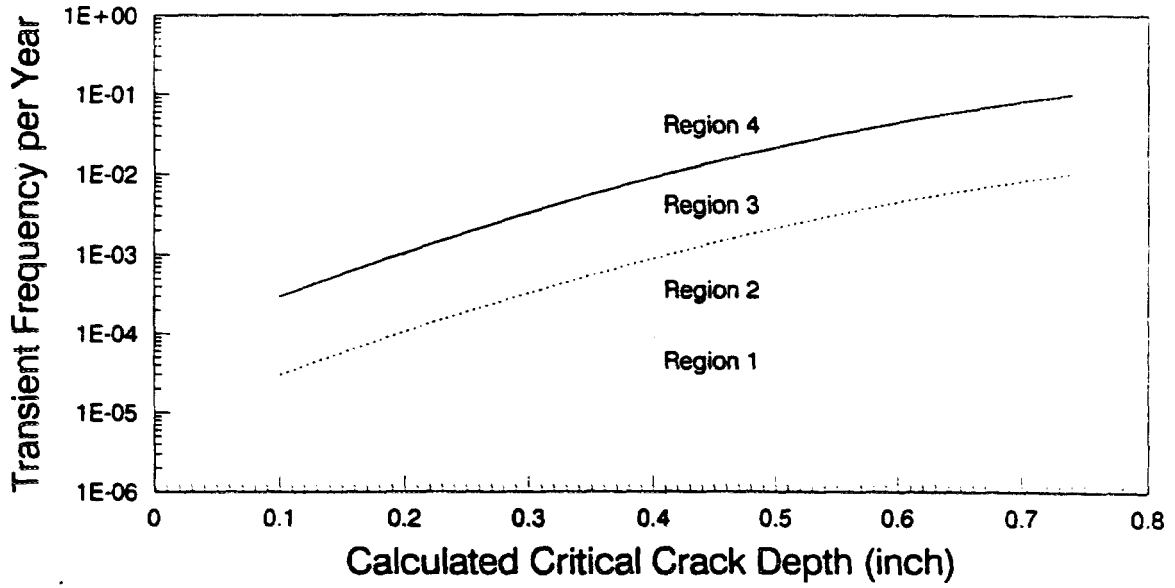


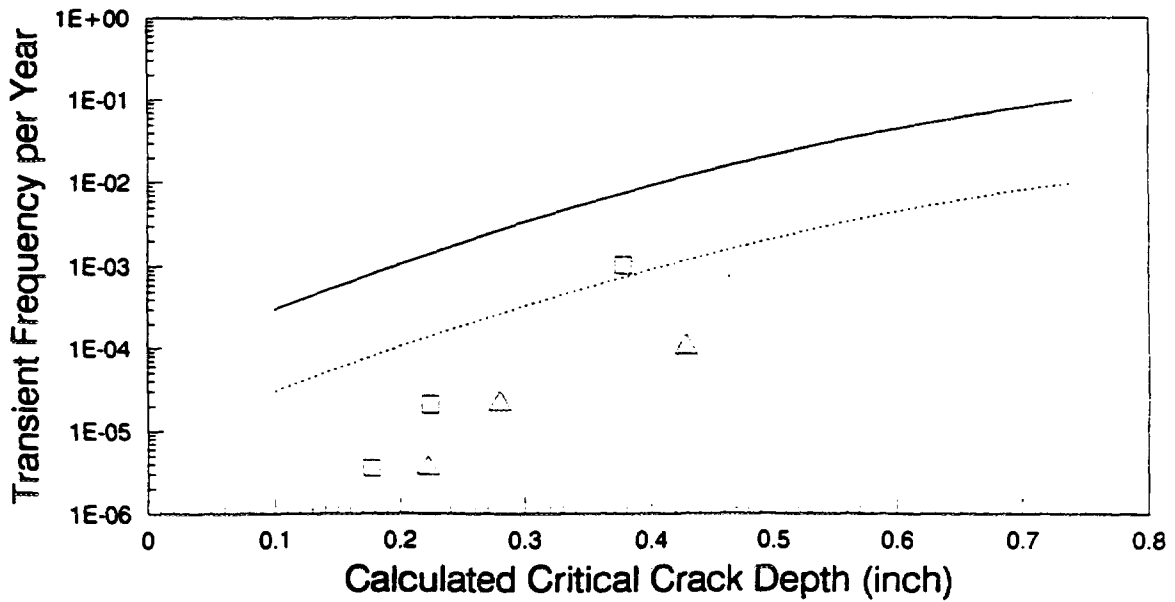
Figure 3
PTS Transient Index Curves for Long Flaws



Risk of $5.0E-6/\text{year}$ Risk of $5.0E-7/\text{year}$ Risk of $5.0E-8/\text{year}$
 ————— ————— —————

PTSFREQ2.FL3

Figure 4
Example Plant PTS Risk Evaluation



Risk of $5.0E-6/\text{year}$ Risk of $5.0E-7/\text{year}$ Risk of $5.0E-8/\text{year}$ Example Plant Example Plant
 ————— ————— ————— Reference Case Modifications
 ————— ————— ————— ————— —————

PTSFREQ5.FL3



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Pressurized Thermal Shock, Esztergom, Hungary, 6-8 May*

Plant Specific PTS Analysis of Kori Unit 1

by

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ABSTRACT

Currently, a nuclear PLIM(Plant Lifetime Management) program is underway in Korea to extend the operation life of Kori-1 which was originally licensed for 30 years. For the life extension of nuclear power plants, the residual lives of major components should be evaluated for the extended operation period. According to the residual life evaluation of reactor pressure vessel, which was classified as one of the major components crucial to life extension, it was found by screening analysis that reference PTS temperature would exceed screening criteria before the target extended operation years. In order to deal with this problem, a plant-specific PTS analysis for Kori-1 RPV has been initiated. In this paper, the relationship between PTS analysis and Kori-1 PLIM program is briefly described. The plant-specific PTS analysis covers system transient analysis, downcomer mixing analysis, and probabilistic fracture mechanics analysis to check the integrity of RPV during various PTS transients. The step-by-step procedure of the analysis will be described in detail. Finally, various issues regarding RPV materials and its integrity will be briefly mentioned, and their implications on Kori-1 PTS analysis will be discussed. Despite of the screening analysis result concern, it is now expected that Kori-1 PTS issue can be handled through the plant-specific PTS analysis.

WHAT IS PTS?

Until late 1970s, it was thought that the most critical thermal shock of pressurized water reactor would occur when cold emergency core cooling water was injected to the primary system in the event of large break loss-of-coolant accident. In this case, considerable thermal stress at the inner surface of reactor pressure vessel would affect the integrity of the primary system. However, due to the rapid decrease of system pressure,

additional stress from system pressure at pressure vessel could be ignored. In 1978, different type of transient occurred at Rancho Seco NPP in U. S.. During the excessive feedwater flow transient, the system pressure remained considerably high while temperature at the RPV wall decreased rapidly after the cold ECCS water was injected to the system. When this kind of transient occurs, the effect of pressure should be fully considered in stress analysis of reactor vessel. When cracks are present at the embrittled reactor vessel, they could grow under the combined stress to the outer surface of reactor vessel.

This kind of phenomenon was defined as a Pressurized Thermal Shock (PTS) by USNRC in early 1980s. NRC subsequently initiated PTS analysis studies on H. B. Robinson (Westinghouse)¹, Oconee-1 (B&W)², and Calvert Cliffs-1 (CE)³. It was found that the failure probability of vessel strongly is depended on the degree of radiation embrittlement, measured as adjusted reference PTS temperature (RT_{PTS}). Especially, some welds, containing large amount copper and located in the core beltline region, were considered to be susceptible to radiation embrittlement and critical to the reactor vessel integrity⁴. Based on these and other researches, PTS rule (10CFR50.61) was issued by USNRC in 1985. It was revised in 1991 and again in 1996 to reflect advanced knowledges and clarify ambiguities.

The content of the PTS rule are⁵;

- Define reference PTS temperature, RT_{PTS}
 $RT_{PTS} = \text{initial } RT_{NDT} + \text{shift of } RT_{NDT} + \text{Margin}$
- Provide methods of calculating RT_{NDT} shift
 - a. utilizing chemistry factor tables based on copper and nickel contents
 - b. utilizing surveillance specimen test data
- Define PTS screening criteria
 - a. $RT_{PTS} < 270 \text{ F}$ for plates, forgings, and axial welds
 - b. $RT_{PTS} < 300 \text{ F}$ for circumferential welds
- Require every plant to submit estimated RT_{PTS} at EOL fluence
- If the estimated RT_{PTS} are to exceed the screening criteria before EOL, plant-specific PTS analysis containing probabilistic methods should be performed for continued operation.
- Plant-specific PTS analysis should be done based on the Reg. Guide 1.99 and 1.154.

PTS RESEARCH ACTIVITIES

When the PTS problem was first issued, it was thought that many of PWR RPVs in the U. S. would reach PTS screening criteria before the end of the licensed periods. Accordingly, in the 1980s, EPRI developed computer codes to simulate the mixing phenomena at the downcomer region when the cold ECCS water is injected through cold leg⁶. The concept of probabilistic fracture mechanics in estimating the integrity of reactor vessel with presence of hypothetical cracks had been introduced from the early PTS studies^{1,2,3}. Several PFM codes for PTS analysis had been developed during 1980s and 1990s, and EPRI-CRIEPI joint study of benchmarking those codes has been completed recently⁷. Researches for

determining the flaw size distribution and proper margin terms has been performed.

Figure 1 shows regulatory guides relevant to reactor pressure vessels considering the effects of irradiation embrittlement. For the Kori-1 RPV that was designed in the late 1960s and fabricated around 1970, PTS problem was not considered at the design stage. Furthermore, Linde 80 weld filler wire which contained high copper and nickel contents had been used in beltline region welds⁸ of Kori-1. In the early days of operation, the most significant problem was low upper-shelf energy(USE) of beltline region welds which was the characteristic of Linde 80 welds. This issue was cleared after conducting fracture mechanics analysis in compliance to the 10CFR50 App. G, showing that RPV of Kori-1 could maintain its integrity over 40 operating years⁹ from USE point of view. During the analysis, RT_{PTS} of beltline region weld was also estimated by the method given in 10CFR50.61. It was shown that RT_{PTS} of circumferential weld would exceed screening criteria at 34 operating years. Thus, a plant-specific PTS analysis according to Reg. Guide 1.154 was needed to check the integrity of RPV at the event of PTS beyond 34 operating years.

Although the plant-specific PTS analysis has not been performed in Korea before, key aspects of PTS analysis such as probabilistic safety analysis, system analysis and mixing analysis has been performed for various purposes. By congregating researchers in these field, a comprehensive Kori-1 plant-specific PTS analysis will be conducted.

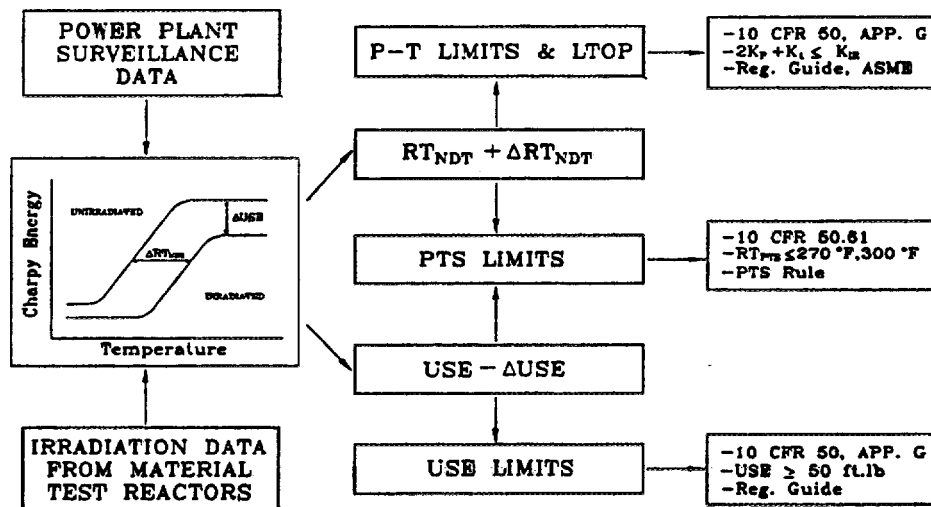


Figure 1. Regulatory guides concerning the integrity of reactor vessels

KORI-1 PLIM AND PTS

Korea Electric Power Corporation(KEPCO) conducted a feasibility study of Kori-1 lifetime extension during the period of Nov. 1993 - Nov. 1996¹⁰, i.e. called PLIM phase I. The overall schedule of Kori-1 PLIM program is shown in Figure 2.

During the PLIM Phase I, thirteen major components were selected by the systematic screening of systems, structures, and components(SSCs) for technical evaluation. As an

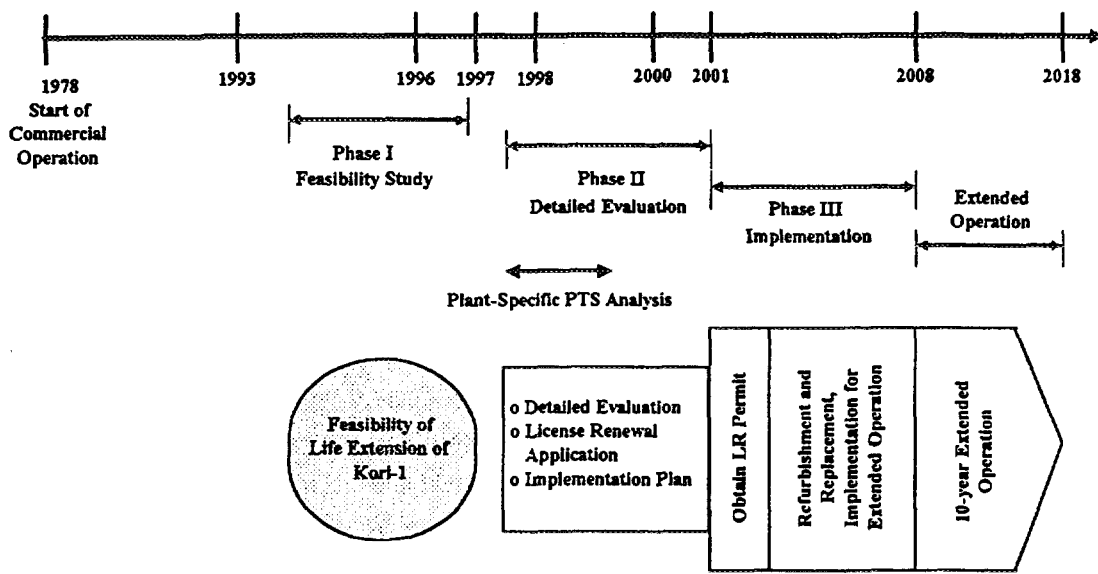


Figure 2. Kori-1 PLIM Schedule and Plant-Specific PTS Analysis

integral part of nuclear PLIM, fatigue lives and aging mechanisms of major components were evaluated. Remaining fatigue lives of major components were evaluated using simple calculation procedure and detailed fatigue cycle counting. The result showed that, for thirteen major components, there were sufficient fatigue margins for extended operation beyond 30 year licensed period. The second phase of PLIM program will be started in 1997 for more detailed evaluation on wider ranges of SSCs, preparation of license renewal application documents, and site implementation schedule. During the PLIM Phase II, the scope of SSCs subjected to life evaluation will be greatly expanded. Also, various aging mechanisms that were not fully considered in Phase I will be evaluated in detail.

Through the feasibility study, the potential aging mechanisms of RPV, which was classified as the most important component to plant life extension, were identified and evaluated. The residual life evaluation of RPV revealed that from the fatigue point of view, RPV can be operated over 60 years¹¹. However, irradiation embrittlement of the beltline region weld was identified as the potentially life-limiting aging mechanism of RPV. Weld metal of Kori-1 RPV beltline region showed low USE, that is below 50 ft-lbs, since the first surveillance test. It was the characteristic of Linde 80 welds containing large amount of copper and nickel. Detailed fracture mechanics analysis was done in accordance with 10CFR50 App. G & H to verify the safety of RPV⁹. It was shown by this analysis that despite of low USE, RPV could maintain its integrity over 40 operating years. Through the analysis, the other aspect of RPV integrity, PTS criteria was also evaluated. RT_{PTS} was calculated according to the methods given in PTS rule and Reg. Guides. As shown in Figure 3, PTS screening criteria for the circumferential weld was projected to be exceeded at 27.4 EFPY(or 34 operating years), well ahead of 40 operating years that was the target operating period of Kori-1 PLIM Program.

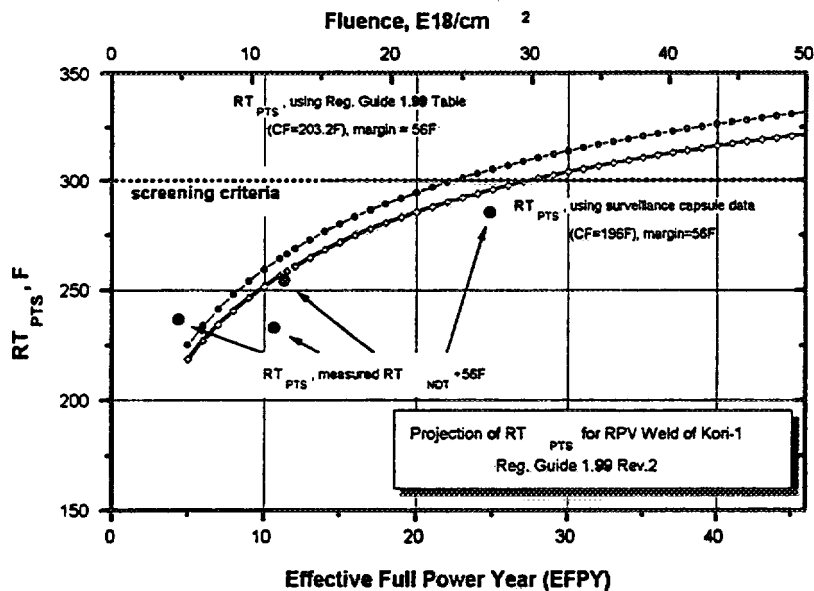


Figure 3. Projection of RT_{PTS} of Kori-1 RPV beltline region weld

The necessity of a plant-specific PTS analysis was well recognized by KEPCO during the PLIM feasibility study. As a result, PTS project was initiated at the beginning of 1997 to be completed in 2 years. As shown in Figure 2, the results of PTS analysis will be available at the middle of PLIM Phase II. This result will be incorporated into the license renewal application documents of Kori-1. The details of the plant-specific PTS analysis will be described in the next section.

Kori-1 Plant-Specific PTS Analysis

The overall flow of a plant-specific PTS analysis is shown in Figure 4. As shown in the figure, the PTS initiating events should be identified by carefully analysing plant specific data. Next, PTS significant transient sequences are classified and grouped based on the similarity in thermal-hydraulic nature. In selecting initiating events and transient sequences, published results of other plants^{1,2,3} are also utilized after careful review. For this purpose the existing PTS analysis results were searched and summarized in Table 1. Currently, 5 initiating events, such as, small break loss of coolant accident(SBLOCA), main steam line break(MSLB), loss of main feedwater(LMFOW), steam generator tube rupture(SGTR), and loss of heat sink(LOHS) are selected to be considered in the analysis. Detailed list and probabilities of PTS significant transient sequences of initiating events are to be obtained from plant-specific probabilistic safety analysis(PSA) results.

For the selected transient sequences, thermal-hydraulic analysis will be performed.

Table 1. Summary of previous PTS analysis results

PLANT	Initiating Events	PTS Risk	Remark
Oconee-1 (Duke Power)	SBLOCA	$3.0 \times 10^{-8} / \text{Rx-y}$	Designer: B&W 2-loop - 2 hot legs - 4 cold legs
	MSLB	$1.6 \times 10^{-8} / \text{Rx-y}$	
	LOMFw	$3.4 \times 10^{-8} / \text{Rx-y}$	
	SGTR	$4.3 \times 10^{-7} / \text{Rx-y}$	
	Safe Shutdown	$1.1 \times 10^{-8} / \text{Rx-y}$	
	Residual	$2.7 \times 10^{-8} / \text{Rx-y}$	
	Total	$4.5 \times 10^{-8} / \text{Rx-y}$	
Calvert Cliffs-1 (BG&E)	SBLOCA	$7.1 \times 10^{-8} / \text{Rx-y}$	Designer: CE 2-loop - 2 hot legs - 4 cold legs
	MSLB	$2.6 \times 10^{-8} / \text{Rx-y}$	
	LOMFw	$2.0 \times 10^{-12} / \text{Rx-y}$	
	SGTR	-	
	Residual	-	
	Total	$7.4 \times 10^{-8} / \text{Rx-y}$	
H. B. Robinson-2 (Carolina P&L)	SBLOCA	$4.0 \times 10^{-10} / \text{Rx-y}$	Designer: Westinghouse 3-loop - 3 hot legs - 3 cold legs
	MSLB	$2.2 \times 10^{-8} / \text{Rx-y}$	
	LOMFw	$3.5 \times 10^{-11} / \text{Rx-y}$	
	SGTR	$2.5 \times 10^{-11} / \text{Rx-y}$	
	Residual	$6.8 \times 10^{-10} / \text{Rx-y}$	
	Total	$1.3 \times 10^{-8} / \text{Rx-y}$	
WOG Study	SBLOCA	$5.0 \times 10^{-8} / \text{Rx-y}$	
	LOHS	$8.0 \times 10^{-7} / \text{Rx-y}$	
	FWLB	$5.0 \times 10^{-9} / \text{Rx-y}$	
	SGTR	$0.5 \times 10^{-8} / \text{Rx-y}$	
	Secondary Depressurization	$1.0 \times 10^{-8} / \text{Rx-y}$	
	Total	$6.3 \times 10^{-8} / \text{Rx-y}$	

Two T/H analysis codes were reviewed for the applicability to the PTS significant transients. RELAP5 has been known to do a very rigorous calculation and be used by regulatory bodies when they check the accident analysis results submitted by utilities. It was used extensively in early PTS studies^{1,2,3}. RETRAN has been used by utilities for the non-LOCA type transient analyses for many years. It requires considerably less time to run a T/H transient case than RELAP5. It was decided that the main T/H code for PTS analysis would be RETRAN. Nonetheless, for the transients initiated from SBLOCA whose contribution to total vessel failure probability was known to be the one of the greatest^{1,2,3}, more rigorous analysis will be done with RELAP5.

From the PTS point of view, detailed temperature distribution in the downcomer and vessel wall should be calculated rather than the element averaged temperature, which is available from thermal-hydraulic analysis, to provide proper input to the fracture mechanics analysis. Various codes had been developed by utilities and NRC to analyze the mixing of hot primary coolant and cold ECCS water in the downcomer region near vessel wall. After careful considerations, PHOENICS commercial FEM code was chosen to be used.

The next step is the probabilistic fracture mechanics (PFM) analysis. FAVOR and VISA-II codes will be used for this purpose. As shown in Figure 4, downcomer pressure, vessel wall temperature, and heat transfer coefficient at vessel wall as functions of time are to be provided as input data for PFM codes. Both FAVOR¹² and VISA-II¹³ internally

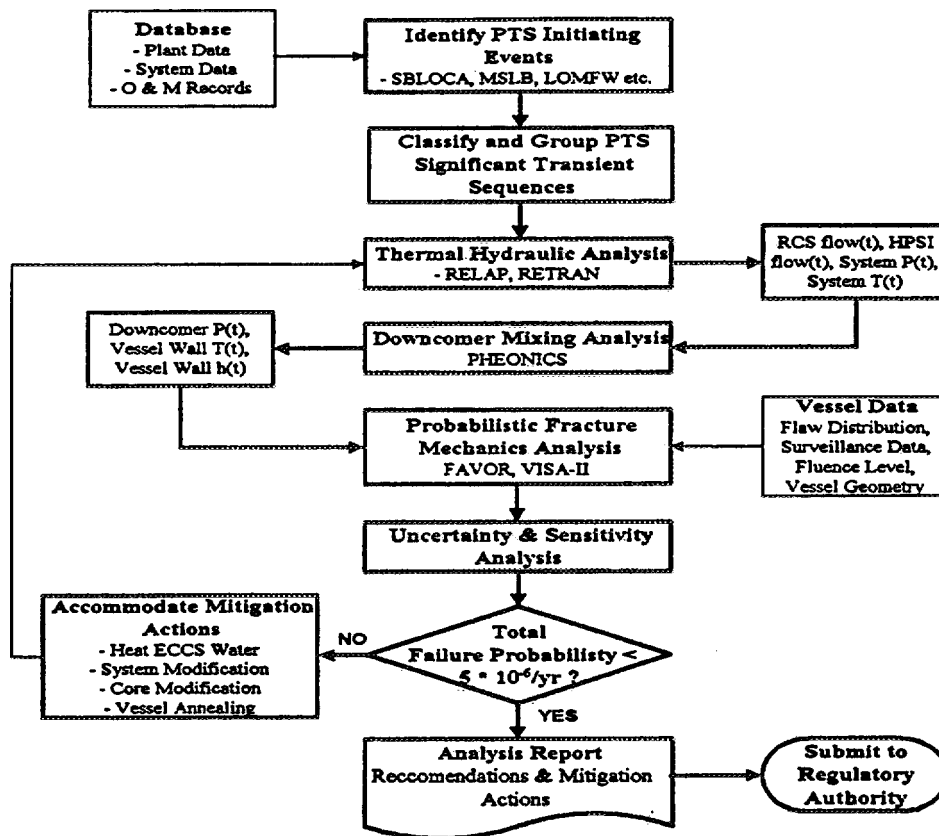


Figure 4. Overall Flow of Kori-1 Plant-Specific PTS Analysis

generate stress and applied fracture toughness within the vessel from these data. The specific vessel data, such as, physical properties of vessel materials, vessel geometry, vessel chemistry, and surveillance capsule data et. al. should be provided, too. Through the PFM analysis, through wall cracking probabilities for PTS significant transients will be calculated. Sensitivity and uncertainty analyses on some parameters will be followed. The effects of potential mitigation actions will be evaluated, too.

Total PTS risk will be calculated by summing up the products of transient frequencies and through-wall cracking probabilities for each PTS significant transient. However, because PSA results of Kori-1 is currently not available, transient frequencies of similar plants will be used to estimate the total failure probability by careful consideration. The results of Kori-1 PSA, which is scheduled to be available in the near future, will be incorporated into the results of PTS analysis later..

The analysis report containing above results will be submitted to the Korean regulatory body in early 1999 for a review. The results will be directly used for the RPV life evaluation which is one of the major task of PLIM Phase II program.

ISSUES RELATED TO PTS OF KORI-1 RPV

In parallel to the plant-specific PTS analysis of Kori-1, several other aspects of RPV integrity are being reviewed or under consideration. Some are described in this section.

Initial RT_{NDT}

The results of Kori-1 RPV weld surveillance tests are summarized in Table 2. Initial RT_{NDT} of Kori-1 reactor beltline weld WF233 was reported as -10 F^{12} . Recently, the adoption of nil-ductility temperature from drop weight test (T_{NDT}) as initial RT_{NDT} was proposed by BWOG based on the fracture toughness test results in the transition regions¹³. According to the weld qualification report, T_{NDT} of Kori-1 WF233 weld was reported as -20 F . This is a potentially very promising in that it will reduce the projected RT_{PTS} somewhat, thus reducing the vessel failure probability during PTS events.

Table 2. Surveillance Capsule Test Data for Kori-1 WF233 Weld.

Capsule	Fluence (10^{19} n/cm^2)	RT_{NDT} shift (F)	Adjusted RT_{NDT} or RT_{PTS} (F)	Upper Shelf Energy, ft-lbs
Unirradiated	-	-	-10^*	66.3
V	0.48	191	237	47.8
T	1.16	187	233	41.8
S	1.23	208	254	46.5
R	2.70	239	285	40.1

Note: * initial measured RT_{NDT}

Margin Term In RT_{PTS}

In calculating the RT_{PTS} as shown in Figure 3, twice the standard deviation of RT_{NDT} shift was applied as a margin. For generic Linde 80 welds, it was given as 28 F in PTS rule. In recent revision of PTS rule, some ambiguity in RT_{PTS} calculation was clarified that the value of standard deviation can be reduced in half when;

1. There is a measured initial RT_{NDT} value.
2. There are measured RT_{NDT} shift data from surveillance capsule test.
3. In estimating RT_{NDT} shift, chemistry factor based on the surveillance capsule data are used.
4. Both measured initial RT_{NDT} and RT_{NDT} shift are taken from the controlling materials with regard to radiation embrittlement.
5. Surveillance capsule data are credible.

The last item, or the credibility criteria for Kori-1 surveillance program are under investigation. For the time being, the previous RT_{PTS} projection remains valid unless new RT_{PTS} calculation is approved by Korean regulatory body.

Fluence Recalculation

Currently, the maximum fast neutron fluence at the vessel inner wall at 40 operating years is estimated as $3.57 \times 10^{19}\text{ n/cm}^2$, which was used to generate the data shown in

Figure 3⁹. This calculation was done following NRC guideline DG-1025. This guideline was recently revised as DG-1053 with some changes in cross-section data of dosimeter elements and vessel fluence calculation method. Preliminary results of fluence recalculation, by the revised guideline, suggested that fast neutron fluence at the vessel inner wall was considerably overestimated¹⁴. About 20% decrease in fluence can be translated into 10 F decrease in RT_{PTS} of WF233 weld. A detailed calculation of fluence is now underway.

Additional Surveillance Test

There were initially 6 surveillance capsules in Kori-1 RPV. Until now, 4 of them have been pulled out and tested. The fluence of the remaining 2 capsules (P and N) are approaching 32 EFPY (or 40 operating years) in a few years. It has been suggested that at least one of them should be tested in the near future to verify reported RT_{PTS} projection and, if necessary, to be used in vessel annealing study. It is expected that additional surveillance test will decrease the chemistry factor, and increase the credibility of RT_{PTS} projection. The exact schedule will be set as soon as the fluence recalculation results are available.

Other Embrittlement Mitigation Options

Several actions are known to be available for utilities to reduce the failure probabilities of embrittled reactor vessel at the event of PTS transients. Only two of them will be discussed in here. First, reactor core can be modified to reduce the fast neutron flux at vessel inner wall. This can be attained either by installing shielding materials around the area of high fast neutron flux, or by modifying fuel loading pattern with which less fast neutron can escape from core into the vessel cavity. Of the two, low leakage loading pattern known as L³P had been already adopted in Kori-1 since the 4-th fuel cycle. Even lower leakage loading pattern, such as low-low leakage loading pattern(L⁴P) seems possible. The practical benefit of such loading pattern is needed to be evaluated. Currently, installing shielding materials within the reactor cavity is not considered.

The second option is vessel annealing to recover the fracture toughness of RPV materials. Kori-1 seems to have a special advantage over other plants in that there are only one circumferential weld in the core beltline region, located more than a foot below core mid-plane. This would minimize the heating zone and, consequently, low thermal stress around nozzle area located far away from the WF233 weld. There has been some basic researches aiming the possibility of vessel annealing in Korea. The necessity of vessel annealing depends on the eventual target operating period of Kori-1. If additional life extension program more than 40 years are initiated, vessel annealing seems to be the promising option to solve the problems of low upper shelf energy, PTS, and shrinking P-T limit, all at once. For current target life of 40 years, RPV integrity verification by the plant-specific PTS analysis might be appropriate.

CLOSING REMARKS

Designed and fabricated around 1970, Kori-1 RPV has a high copper and nickel containing weld, known as WF233. Irradiation embrittlement of the weld over the years decreased fracture resistance of the materials, and increased reference nil-ductility transition temperature(RT_{NDT}). In the midst of Kori-1 PLIM program, it came out as an issue that the calculated RT_{PTS} could exceed the PTS screening criteria several years before the target extended operating period.

To deal with the concerns of PTS issues, a plant-specific PTS analysis has been initiated in parallel with the PLIM Phase II program. It is expected that through the plant-specific PTS analysis, the integrity of Kori-1 RPV will be assured. The results will be appropriately incorporated into the overall PLIM Phase II program.

Several issues concerning Kori-1 RPV materials were discussed from the view point of PTS. The possibility of reduction in initial RT_{NDT} value and margin term can affect in a positive way to solve the PTS issue. With additional surveillance capsule test, the reliability of RT_{PTS} estimation will be enhanced. The results of fluence recalculation according to the revised method will also affect the overall results of RT_{PTS} estimation and PTS analysis.

The applicability of mitigation options were discussed too, in case additional measures should be taken to reduce the RPV failure frequencies by PTS events. Vessel annealing needs careful consideration in connection with the ultimate target life of RPV.

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**Methodology for Pressurised Thermal Shock Evaluation
IAEA Meeting, 5-8 May 1997, Esztergom, Hungary**

**Evaluation of fracture mechanics analyses used in RPV integrity assessment
regarding brittle fracture**

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ABSTRACT

Electricité de France has conducted during these last years some experimental and numerical research programmes in order to evaluate fracture mechanics analyses used in nuclear reactor pressure vessels structural integrity assessment, regarding the risk of brittle fracture. These programmes included cleavage fracture tests on large scale clad specimens containing subclad flaws with their interpretations by 2D and 3D numerical computations, and validation of finite element codes for pressurised thermal shocks analyses.

Four clad specimens made of ferritic steel A508 C13 with stainless steel cladding, and containing shallow subclad flaws, have been tested in four point bending at very low temperature in order to obtain cleavage failure. The specimen failure was obtained in each case in base metal by cleavage fracture. These tests have been interpreted by two-dimensional and three-dimensional finite element computations using different fracture mechanics approaches (elastic analysis with specific plasticity corrections, elastic-plastic analysis, local approach to cleavage fracture). The failure of specimens are conservatively predicted by different analyses. The comparison between the elastic analyses and elastic-plastic analyses shows the conservatism of specific plasticity corrections used in French RPV elastic analyses.

Numerous finite element calculations have also been performed between EDF, CEA and Framatome in order to compare and validate several fracture mechanics post processors implemented in finite element programmes used in pressurised thermal shock analyses. This work includes two-dimensional numerical computations on specimens with different geometries and loadings. The comparisons show a rather good agreement on main results, allowing to validate the finite element codes and their post-processors.

1. INTRODUCTION

Electricité de France has conducted during these last years some experimental and numerical research programmes in order to evaluate fracture mechanics analyses used in nuclear reactor pressure vessels structural integrity assessment, regarding the risk of brittle fracture. Cleavage fracture tests have been

performed on large scale clad specimens containing subclad flaws with their interpretations by 2D and 3D numerical computations (1)(2). Numerous two-dimensional finite element calculations on specimens with different geometries and loadings have also been performed in collaboration with CEA and Framatome in order to compare and validate several fracture mechanics post processors implemented in finite element programmes used in pressurised thermal shock analyses (3)(4)(5).

2. EDF CLEAVAGE FRACTURE TESTS ON CLADDED SPECIMENS WITH SUBCLAD FLAWS

2.1 Description of specimens

Four clad specimens (DSR4, DSR1, DSR3 and DD2) containing each a shallow subclad flaw have been submitted to a four point bending mechanical loading (1). The specimens geometry is described shortly on figure 1. The central part of each specimen is extracted from a vessel shell ring of A508 C13 ferritic steel. The size of specimens is approximately 120 mm thickness, 1700 mm length and 150 mm width. The cladding is layered on the top surface using an automatic submerged arc welding process. A stress relief heat treatment is applied after cladding, at 600 °C during 8 hours. The artificial subclad flaw is made by fatigue before cladding.

2.2 Testing conditions

The four clad specimens are loaded in four-point bending, as shown on figure 1. The tests are performed at very low temperature (-170 °C) in order to obtain brittle fracture in base metal. The instrumentation includes thermocouples (placed on surface and inside the specimen to control the temperature during the test), strain gauges on upper and lower surfaces, load and load line displacement transducers.

2.3 Materials data

Characterization of stainless steel cladding, base metal and heat affected zone (HAZ) has been conducted including chemical analyses, Charpy impact and tensile tests, crack growth resistance and fracture toughness. The chemical composition of base metal is given in table 1. The base metal fracture

toughness K_{Ic} , measured on 20 % side grooved CTJ25 specimens (net thickness 20 mm), is presented on figure 2. The base metal RT_{NDT} is $-40\text{ }^{\circ}\text{C}$. The material characterization specific to local approach to fracture will be described later (3).

2.4 Experimental results

A cleavage fracture has been obtained in base metal in each test, without crack arrest. Figure 3 shows the surface aspect of two specimens after failure : the surface aspect is always typical of a cleavage fracture, at least in the upper part of the section. Main results are summarized in table 2. The flaw shape is practically semi-elliptical in case of DSR4, DSR1 and DD2 tests and approximately semi-circular in case of DSR3 test (1)(2).

3. MECHANICAL ANALYSES USED IN FRENCH RPV STRUCTURAL INTEGRITY ASSESSMENT

3.1 Two-dimensional elastic analysis with plasticity corrections

In the elastic analyses, two plasticity corrections are applied after the computation of the stress intensity factor K_I to take into account plasticity at the crack tips (Irwin correction) and yielding in the cladding (α correction), according to the approach proposed for the analysis of the French PWR vessels (1) :

Irwin correction : $K_{Irwin} = K_I \cdot ((2a+r_a+r_b)/(2a))^{0.5}$

with

$2a$: crack depth

r_a, r_b : plastic zone radius at the crack tip in cladding (a) and base metal (b) given by $r = ((K_I/\sigma_y)^2)/6\pi$ where σ_y is the yield strength of the corresponding material

α correction : $K_{cp} = \alpha \cdot K_I \cdot ((2a+r_a+r_b)/(2a))^{0.5}$

α is a coefficient depending on the plastic zone radius r and the remaining ligament value b between the crack tip and the surface. This coefficient is determined by :

$$\alpha = 1 \quad \text{if } r < 0.05b$$

$$\alpha = 1 + 0.15 \cdot ((r - 0.05b)/(0.035b))^2 \quad \text{if } 0.05b < r < 0.085b$$

When the condition $r = 0.085b$ is exceeded, α is the lowest of the value given by the above expression and the limiting value 1.6

3.2 Two-dimensional elastic-plastic analysis

In the elastic-plastic analyses, the stress intensity factors K_J are deduced from the calculation of the G energy release rate ($K_J = (E \cdot G / (1 - \nu^2))^{0.5}$).

3.3 Three-dimensional elastic and elastic-plastic analyses

Two tests (DSR3, DD2) are interpreted using three-dimensional finite element analyses performed with EDF finite element code Aster (6). The stress intensity factors (elastic K_I and elastic-plastic K_J) are calculated along the crack front in base metal and compared to the base metal fracture toughness K_{Ic} . The stress intensity factors K_I and K_J are deduced from the calculation of the energy release rate G by the classical expression : $K = \sqrt{(E \cdot G / (1 - \nu^2))}$.

The energy release rate G is obtained using the Theta method described in (7).

3.4 The local approach to cleavage fracture (Beremin model)

The local approach to cleavage fracture (Beremin model or Weibull model), developed by F. Mudry, is based on the weakest link theory and the use of Weibull statistics (8)(9)(10). The model, now well known, is based on the elastic - plastic computation of a structure, without coupling between plasticity and damage, and on the use of a damage criterion computed from the stress - strain field at the crack tip.

The probability P of initiating cleavage fracture is expressed as

$$P = 1 - \exp(-(\sigma_w / \sigma_u)^m)$$

σ_w is the " Weibull stress " and characterizes the mechanical conditions exerted at the crack tip relevant to the risk of initiating cleavage fracture :

$$\sigma_w = (\sum_j \sigma_{1j}^m (V_j / V_0))^{1/m}$$

σ_{1j} is the value of maximum principal stress in the finite element cell, number j , V_j is the volume of the mesh element and V_0 the volume of the material cell. The integration is made on the plastic zone ahead of the crack tip.

The model is defined by two parameters m and σ_u . m is the Weibull parameter and characterizes the scatter in cleavage fracture. σ_u is defined as the intrinsic cleavage stress of the material and characterizes the resistance to cleavage fracture. Both parameters, which are assumed to be characteristics of the material, are usually determined through tests on axisymmetrical notched tensile specimens and numerical calculations.

3.5 Identification of m , σ_u Beremin model parameters (local approach)

The identification of Beremin model parameters, m and σ_u , is performed on axisymmetrically notched tensile specimens AE2 (notch radius 2 mm). The procedure used for this has been previously described in reference (3). This identification is conducted in the frame of an extensive cooperative research program between EDF, CEA, Framatome and AEA Technology (3). The material is taken from the same forging vessel ring of A508 C13 ferritic steel, as for the cladged specimens previously described. Fourty tests on axisymmetric notched specimens were performed at $-170\text{ }^{\circ}\text{C}$ in the pure cleavage regime, conducted by two laboratories. Two sets of parameters are finally identified, according to the number of

notched specimens taken into account in the identification procedure :

- . $m = 27$, $\sigma_u = 2657$ MPa (20 specimens)
- . $m = 24$, $\sigma_u = 2696$ MPa (40 specimens)

4. MAIN INTERPRETATIONS OF THE CLEAVAGE FRACTURE TESTS

4.1 Two-dimensional analyses

Main results obtained at the crack tip in the base metal, deduced from the two-dimensional numerical calculations (1), are presented on figure 4. The elastic stress intensity factors K_{cp} (including the two specific plasticity corrections) and the elastic-plastic stress intensity factors K_I are compared to the base metal fracture toughness K_{IC} determined on conventional fracture mechanics CT25 specimens ($a/W : 0.55$).

The elastic stress intensity factors K_{cp} deduced from the elastic analyses are always greater than the base metal fracture toughness. Compared to the fracture toughness, the margins are important as soon as the α correction is significant (case of DSR1, DSR3 and DD2 tests). The two-dimensional analysis of DSR3 test (crack depth : 13 mm) appears too conservative to a three-dimensional analysis.

The comparison of the elastic-plastic stress intensity factors K_I with the base metal fracture toughness K_{IC} shows reasonable margins. These margins are smaller than in the elastic analyses except for the DSR4 test for which the α correction is not significant ($\alpha < 1.1$).

4.2 Comparison between the two-dimensional elastic and elastic-plastic analyses

The comparisons between the stress intensity factors deduced from the elastic analyses (K_{cp}) and the elastic-plastic analyses (K_I) show that the elastic analyses are conservative as soon as the plastic deformation in the cladding is not negligible (figure 5)(1). This conservatism is due to the α correction proposed in the French RCC-M code which takes into account the yielding of the cladding during the loading. This correction is important as soon as the plastic zone at the cladding side crack tip reaches a notable proportion of the remaining ligament. This correction is however necessary because the elastic analyses can greatly underestimate the crack opening in base metal when plastic straining of the cladding occurs (figure 6).

4.3 Three-dimensional interpretation of DSR3 and DD2 tests

Complementary interpretation of DD2 and DSR3 tests have been more recently performed using three-dimensional elastic and elastic-plastic computations performed with the EDF finite element code Aster (6). The stress intensity factors (elastic K_I and elastic-plastic K_I) are calculated along the crack front in base metal and compared to the base metal fracture toughness K_{IC} . The stress intensity factors K_I and K_I are deduced from the calculation of the energy release rate G by the classical expression : $K = \sqrt{(E.G / (1 - \nu^2))}$. The energy release rate G is obtained using the Theta method described in (7).

In order to apply later the local approach to cleavage fracture (Beremin model), the use of very refined mesh (at the crack tip along the crack front) is required (3). Both three-dimensional meshes (DSR3 and DD2) are presented on figures 7 and 8. These meshes, made using the GIBI mesh generating tool, contain approximately 2000 elements and 9000 nodes in the first case (DSR3) and 3000 elements and 12000 nodes in the second case (DD2). The refinement at the crack tip is very important in both cases, the size of first elements at the tip is 60 μm for DSR3 mesh and 50 μm for DD2 mesh.

The main mechanical properties of both materials are given in the table 3.

DSR3 test

Main results of the three-dimensional elastic-plastic analysis of DSR3 test are presented on figure 9 (stress intensity factor K_I along the crack front in base metal) and 10 (stress intensity factor K_I versus load at the deepest point of the crack)(6). Figure 9 shows a variation of the elastic-plastic stress intensity factor K_I along the crack front. The maximum value of K_I is reached at the deepest point of the crack in base metal ($\phi = 90^\circ$). At the critical load ($F = 695$ kN), we obtain $K_I = 54$ MPa. $\sqrt{\text{m}}$ for the deepest point of the crack. This value can be compared to the base metal fracture toughness K_{IC} previously presented on figure 2 (at the DSR3 test temperature $T = -170^\circ\text{C}$, 40 MPa. $\sqrt{\text{m}} < K_{IC} < 48$ MPa. $\sqrt{\text{m}}$). This comparison clearly shows that the maximum value of the stress intensity factor along the crack front K_I is greater than the base metal fracture toughness K_{IC} determined on classical CT25 specimen.

DD2 test

Main results deduced from three-dimensional elastic and elastic-plastic analyses of DD2 test are presented on figure 11 (stress intensity factor K_I along the crack front in base metal) and 12 (stress intensity factor K_I versus load at the deepest point of the crack), including a comparison between elastic and elastic-plastic calculations (6). Figure 11 shows a strong variation of the elastic-plastic stress intensity factor K_I along the crack front (stronger than in DSR3 analysis). As in the case of DSR3 interpretation, the maximum value of K_I is reached at the deepest point of the crack in base metal ($\phi = 90^\circ$). For the critical load ($F = 890$ kN), we obtain $K_I = 51$ MPa. $\sqrt{\text{m}}$ for the deepest point of the crack. This value can be compared to the base metal fracture toughness K_{IC} previously presented on figure 2 (at the DD2 test temperature $T = -170^\circ\text{C}$, 40 MPa. $\sqrt{\text{m}} < K_{IC} < 48$ MPa. $\sqrt{\text{m}}$). This comparison clearly shows that the maximum value of the stress intensity factor along the crack front K_I is greater than the base metal fracture toughness K_{IC} determined on classical CT25 specimen, as in the case of DSR3 test.

The elastic and elastic-plastic analyses can be compared between themselves on the same figures 11 and 12. This comparison clearly shows that the elastic analysis for a subclad flaw, without specific plasticity corrections, is non conservative compared to an elastic-plastic analysis even if the global behavior of the specimen remains linear (the load versus displacement remains linear during all the test). For a subclad flaw, the elastic analysis can greatly underestimate the crack opening in base metal when plastic straining of the stainless steel cladding occurs (by comparison with an elastic-plastic analysis).

4.4 Conclusion about interpretation with classical approach

Four mechanical tests have been performed in four-point bending by EDF on large size mock-ups containing an underclad crack made by fatigue before cladding. A cleavage fracture initiated in base metal has been obtained on each mock-up at a temperature of about -170°C.

The cleavage fracture of each mock-up is always conservatively predicted by an elastic analysis including plasticity corrections (Irwin correction and α correction) and by an elastic-plastic analysis : the stress intensity factors K_{cp} or K_J are always greater than the base metal toughness K_{Ic} . The comparison between the elastic analysis and the elastic-plastic analysis shows that the first one is often more conservative ; it is due to the fact that the α correction proposed in French RCC-M code is important as soon as the plastic zone at the crack tip reaches α notable proportion of the remaining ligament. However, the comparison between K_{cp} (elastic analysis) and K_J (elastic-plastic analysis) shows that the α correction is necessary because the elastic analysis underestimates the crack opening in base metal when yielding of the cladding occurs.

Two tests, DSR3 and DD2, have been analysed with a three-dimensional classical approach involving the calculation of the stress intensity factor K_I along the crack front in base metal. A variation of this stress intensity factor is noticed along the crack front in both cases, more or less strong. For both interpretations, the maximum value of the stress intensity factor K_I is obtained at the deepest point of the crack ($\phi = 90^\circ$). This maximum value is always greater than the base metal fracture toughness K_{Ic} determined on small classical CT25 specimens (20% side grooved specimens corresponding to a 20 mm net thickness).

The DD2 elastic and elastic-plastic analyses have been also compared. In case of a subclad flaw, the elastic analysis is non conservative compared to the elastic-plastic analysis ($K_I < K_J$) as soon as plasticity occurs in cladding, even if the global behavior of the specimen remains linear. The crack opening can be strongly underestimated by an elastic approach for subclad flaws due to cladding yielding. It confirms previous results deduced from two-dimensional finite element computations of clad structures under thermal or mechanical loadings.

4.5 Three-dimensional interpretation of DD2 and DSR3 tests with local approach to cleavage fracture

DSR3 test

The probability of cleavage failure in base metal is evaluated with local approach to cleavage fracture (Beremin model) using m , σ_u Beremin model parameters previously given (two sets of parameters : $m = 27$, $\sigma_u = 2657$ MPa and $m = 24$, $\sigma_u = 2696$ MPa)(3). The evolution of the failure probability during the DSR3 test is presented on figure 13. The failure probability is in good agreement with the experimental specimen failure (critical load : 695 kN). This probability is very similar as for taking m , σ_u parameters identified on 20 ($m = 27$, $\sigma_u = 2657$ MPa) or 40 specimens ($m = 24$, $\sigma_u = 2696$ MPa).

DD2 test

The evolution of the failure probability during the DD2 test is presented on figure 14. As in the case of the DSR3 analysis, the failure probability is in good agreement with the experimental specimen failure (critical load : 890 kN). This probability is also very similar as for taking m , σ_u parameters identified on 20 ($m = 27$, $\sigma_u = 2657$ MPa) or 40 specimens ($m = 24$, $\sigma_u = 2696$ MPa).

Effective stress intensity factor K_I along the crack front

An « effective » stress intensity factor K_w along the crack front l in base metal can be evaluated by the following expression derived from local approach to cleavage fracture in small scale yielding and size effect (11)

$$K_w = [1/l_R \int K_I^4(l) \cdot dl]^{1/4}$$

where $K_I(l)$ is the stress intensity factor along the crack front at the position l , and l_R the length of the crack.

Results obtained for the critical load by this expression are summarized in the table 3 (6). For both experiments, the « effective » stress intensity factor K_w derived from local approach to cleavage fracture and size effect is in agreement with the failure of the specimens DSR3 and DD2. K_w is indeed in the scatter band of base metal fracture toughness determined on CT25 CT specimens at -170 °C ($40 \text{ MPa}\cdot\sqrt{\text{m}} < K_{Ic} < 48 \text{ MPa}\cdot\sqrt{\text{m}}$).

4.6 Conclusion about 3D application of local approach to cleavage fracture

DSR3 and DD2 experiments have been analysed by local approach to cleavage fracture (Beremin model) using m , σ_u Beremin model parameters identified at -170 °C on the same material. The failure probabilities evaluated with this model are in both cases in agreement with the experimental results (failure of specimens). The local approach to cleavage fracture in small scale yielding with size effect has also been used to evaluate an « effective » stress intensity factor K_w along the crack front in base metal. In both experiments, the « effective » value K_w is in agreement with the failure of the specimen and with base metal properties. At the fracture, K_w is either in the scatter band of the base metal fracture toughness K_{Ic} or greater than the maximum value of K_{Ic} .

5. VALIDATION OF FINITE ELEMENT CODES USED IN PRESSURISED THERMAL SHOCK ANALYSES

A research cooperative programme between EDF, CEA, Framatome and AEA Technology is in progress, focused more precisely on the application of the local approach to fracture to reactor pressure vessels (3). Within the framework of this programme, numerous two-dimensional finite element computations have been conducted by partners on specimens and structures in order to compare and validate the fracture mechanics post processors implemented in finite element programmes used by the partners (4)(5).

In the frame of this collaboration, a benchmark on the computational simulation of a clad vessel with a 6.2 mm deep subclad flaw submitted to a thermal transient has been conducted by four partners including two-dimensional elastic

and elastic-plastic finite element analyses (4). The comparison on several parameters between the different finite elements programmes is presented on this application. One complementary numerical computation performed on a clad vessel with a 12.2 mm subclad flaw is also presented (4)(5).

5.1 Benchmark of a clad vessel with a 6.2 mm subclad flaw

Clad vessel geometry

The specimen chosen in this study is a clad vessel submitted to a thermal transient. The base metal is an A508 C13 forging steel and the cladding is a A309L - A308L stainless steel. The main dimensions are the following :

. internal radius	Ri : 2000 mm
. external radius	Re : 2207 mm
. cladding thickness	7 mm
. base metal thickness	200 mm
. subclad flaw	6.2 mm

The crack taken into account is a 6.2 mm deep axisymmetrically subclad flaw (including a 0.2 mm tip in cladding in order to calculate the stress intensity factor at the crack tip in cladding).

Materials properties

The materials properties taken into account in this work are presented in papers (4)(5). The thermal properties and the thermal expansion coefficients are temperature dependent, the other mechanical properties are not temperature dependent (Young's modulus, yield strength, Poisson ratio and stress - strain curve).

The stress - strain curve used for the A508 C13 ferritic steel corresponds to the end of life material properties, including the effect of irradiation.

Loading and boundary conditions

The vessel is submitted to a thermal transient applied on the inner surface, without pressure. This transient is described in papers (4)(5) (thermal from 280°C to 20°C in 20 seconds) . The heat transfer coefficient is fixed at 5000 W /m².°C on the inner clad surface and is equal to zero on the other surfaces.

Mesh requirements and finite element model

The clad vessel is modelled with eight noded reduced integration elements. The refined zone at each crack tip (base metal and cladding) is constituted of at least 8 x 8 square elements, each of side 50 µm. The same mesh refinement at crack tips was used by four partners. One example of mesh is presented in figure 15, with details of the refinement at the crack tip (AEA mesh generated by IDEAS for code with ABAQUS).

Finite element programmes

The thermal and mechanical analyses were conducted by different partners with four finite element programmes (4) :

- . ASTER for Electricité de France
- . CASTEM 2000 for CEA
- . SYSTUS for Framatome
- . ABAQUS for AEA Technology

5.2 Main results of the benchmark

Main results of this benchmark are gathered in paper (4) :

Thermal calculation

The results of the thermal calculation are presented in figure 16. The agreement between the results of the partners is very good.

Elastic analysis

The "stress - free" temperature is taken at the initial temperature of the vessel (280 °C), neglecting thus residual stress. Main result of the elastic analysis is presented in figure 17 (stress intensity factor K_I in cladding and base metal). The agreement is also quite good.

Elastic-plastic analysis

Small strain elastic-plastic analyses have been conducted in all cases. As in the case of the elastic analysis, the "stress - free" temperature is taken at the initial temperature of the vessel (280 °C). The stress intensity factor K_I is deduced from the elastic-plastic calculation of J integral (or G energy release rate), $K_I = (EJ / 1-\nu^2)^{0.5}$.

Main results are presented in figures 18 (opening of the mid point of the crack and 19 (stress intensity factor K_I during the transient in cladding and base metal). The agreement between respective results from EDF, CEA, Framatome and AEA is generally very satisfactory. Some differences can be noticed in the stress intensity factor K_I both on cladding and base metal during the decreasing part of the transient, contrary to the increasing part of the transient where the agreement is still good.

A comparison between the elastic and elastic-plastic computations is made on the stress intensity factor in cladding and base metal (figure 20)(4). Higher values of the stress intensity factor are obtained in the elastic-plastic analysis, due to yielding of cladding. The elastic analysis underestimates the crack opening and the stress intensity factor in case of shallow subclad flaw.

Local approach to fracture

Beremin model (σ_w Weibull stress) and Rice-Tracey model (R/R_0 void growth rate) are applied as post-processors of the elastic-analysis calculation of the clad vessel (hardening and damage are not coupled in this kind of approach).

The evolution of the Weibull stress and the probability of failure during the transient is presented in figure 21 (Weibull parameters considered in this application are $m = 22$, $\sigma_w = 2560$ MPa and $V_0 = (0.050 \text{ mm})^3$)(4). The agreement between EDF, CEA and AEA computations is very good, even in the decreasing part of the transient. The Framatome results, similar to other results in the increasing part of the transient, are very different in the decreasing part of the transient. It is due to the fact that, in SYSTUS programme, the elements under unloading are not considered for the calculation of the Weibull stress σ_w (the stress is in this case fixed to zero).

The R/R_0 parameter is calculated in this benchmark according two methods, first using average stresses in 50 µm * 50 µm square elements, second using average stresses in a 200 µm * 200 µm square element (this element is obtained using 16 50 µm * 50 µm square elements).

The evolution of R/R_0 void growth rate is shown in figure 22. We notice an effect of the size of elements taken into account in this application, whereby higher values of R/R_0 are obtained

with the smallest elements. The examination of results shows that CEA results are a little different (higher values) from EDF and Framatome results.

Conclusion on this benchmark

A benchmark with two-dimensional computations has been conducted by EDF, CEA, Framatome and AEA Technology on a clad vessel under thermal loading. Several parameters from the elastic analysis, the elastic-plastic analysis and the local approach of cleavage and ductile fracture have been compared. This comparison shows a rather good agreement in almost all compared parameters. Some minor differences have been noticed in the elastic-plastic evaluation of the stress intensity factor K_I in cladding and base metal in the decreasing part of the transient, and in the R/R_0 void growth rate.

5.3 Complementary computation on a clad vessel with a 12.2 mm deep subclad flaw

Within framework of this cooperative research programme, numerous two-dimensional computations have been performed by different partners (4)(5). One additional analysis is shortly presented in this part, related to a clad vessel with a 12.2 mm deep subclad flaw.

This analysis has been conducted by EDF and CEA. The vessel geometry (except the flaw depth, 12.2 mm instead of 6.2 mm with 0.2 mm in cladding), the material properties and loading conditions are the same as in the previous study (see previous section). The refinement at the crack tips is also similar ($50 \mu\text{m} \times 50 \mu\text{m}$ square elements).

Some of the EDF and CEA results are presented and compared in figure 23 (elastic and elastic-plastic stress intensity factor in cladding and base metal). The agreement between EDF and CEA results is rather good. However, we always notice some (minor) differences on the stress intensity factors (both cladding and base metal), in the decreasing part of the transient. These results confirm the fact that the elastic analysis is non conservative in case of shallow subclad flaw, due to yielding of cladding.

Figure 24 shows the evolution of Weibull stress and probability of cleavage failure during the thermal transient. Similar values are obtained by EDF and CEA. We can also notice an increase of the Weibull stress, compared to the clad vessel with 6.2 mm deep subclad flaw.

5.4 Synthesis of numerical computations

Two-dimensional numerical finite element analyses have been presented in this paper. The comparisons of results obtained by different partners show generally a rather good agreement on main parameters. Some minor differences have been noticed in a few parameters (stress intensity factor in the decreasing part of the transient, R/R_0 void growth rate).

6. CONCLUSION

A short overview of the experimental and numerical work conducted by Electricité de France has been exposed in this paper related to RPV structural integrity assessment regarding

brittle fracture. Different fracture mechanics methods have been evaluated and validated using cleavage fracture tests on large scale clad mock-ups containing subclad flaws.

Within the framework of a cooperative research programme between EDF, CEA, Framatome and AEA, several numerical benchmarks using two-dimensional computations have been performed in order to compare and validate finite element programmes regarding PTS analyses. Two benchmarks have been presented related to clad vessels submitted to a thermal transient. Numerous parameters have been compared as temperature field in the vessel, crack opening, opening stress at crack tips, stress intensity factor in cladding and base metal, Weibull stress σ_w and probability of failure in base metal, void growth rate R/R_0 in cladding. These comparisons show an excellent agreement on main results. Some minor differences are noticed on the stress intensity factor K_I in the decreasing part of the transient and on the R/R_0 void growth rate. These results allow to validate these finite element programmes for PTS analyses.

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Table 1 - Chemical composition of base metal (cladded specimens)

	C	S	P	Mn	Si	Ni	Cr	Mo	V	Cu	Co	Al
RCCM	< 0.22	< 0.008	< 0.008	1.15 1.6	0.10 0.30	0.5 0.8	< 0.25	0.43 0.57	< 0.01	< 0.08	< 0.03	< 0.04
inner surface	0.14	0.004	0.006	1.31	0.19	0.72	0.17	0.51	< 0.01	0.07	< 0.01	0.015
1/4 thickness	0.18	0.004	0.006	1.32	0.19	0.73	0.17	0.51	< 0.01	0.07	< 0.01	0.016

Table 2 - Main characteristics of DSR4, DSR1, DSR3 and DD2 tests (cladded specimens)

specimen	failure load (kN)	flaw geometry		specimen geometry		cladding thickness (mm)
		depth (mm)	width (mm)	thickness (mm)	width (mm)	
DSR4	640	5	50	127.5	150	8
DSR1	804	4	44	125	145	5
DSR3	695	13	40	124.5	145	4.5
DD2	890	4.5	48	125.2	145	6

Table 3 - Three-dimensional interpretation of DSR3 and DD2 tests (cladded specimens)

specimen and critical load	variation of K_I along the crack front (MPa. \sqrt{m})	maximum value of K_I along the crack front (MPa. \sqrt{m})	K_w (MPa. \sqrt{m})
DSR3 (695 kN)	$20 < K_I < 54$	54	49
DD2 (890 kN)	$20 < K_I < 51$	51	45

Figure 1 - Schematic of the test frame used in four-point bending fracture experiments

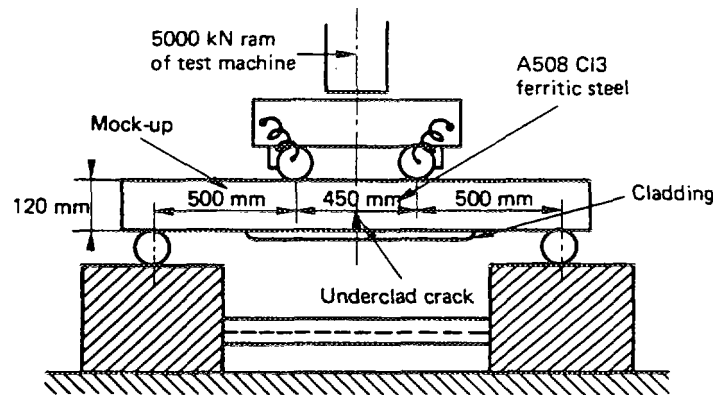


Figure 2 - Fracture toughness of base metal (CTJ25 specimens)

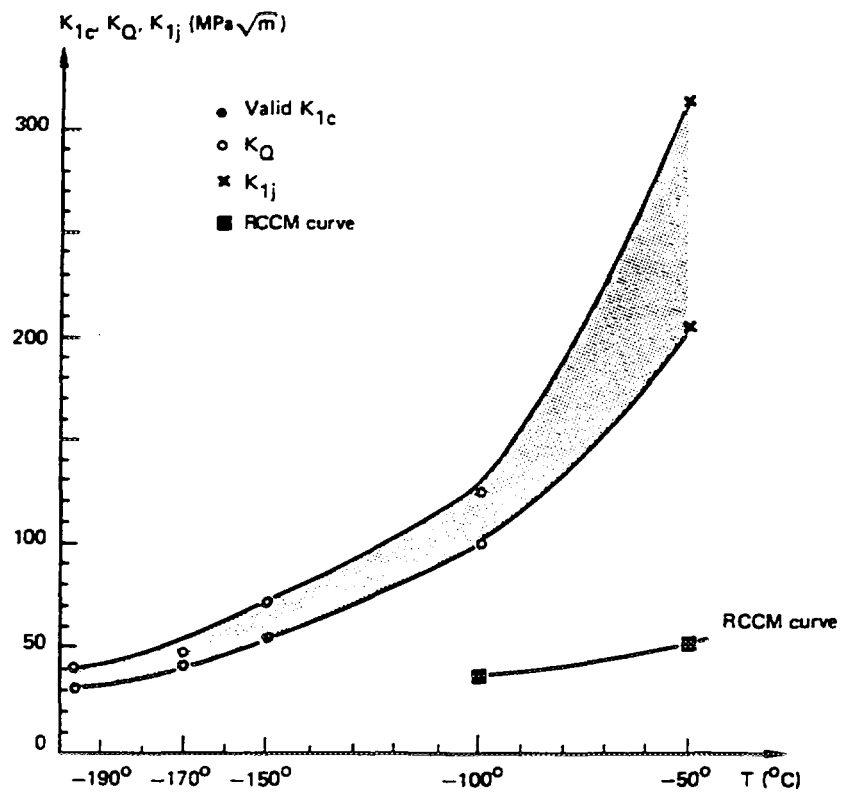
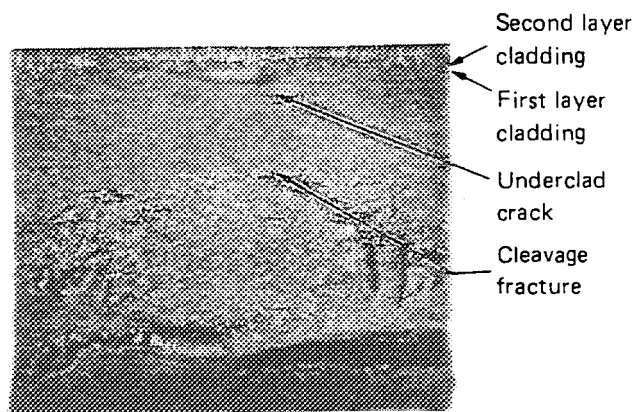
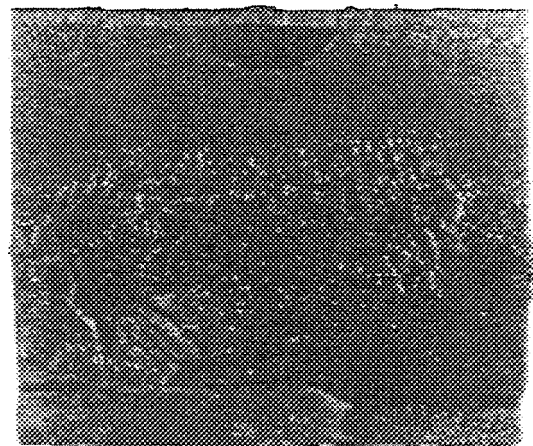


Figure 3 - Cross-section of specimens after fracture

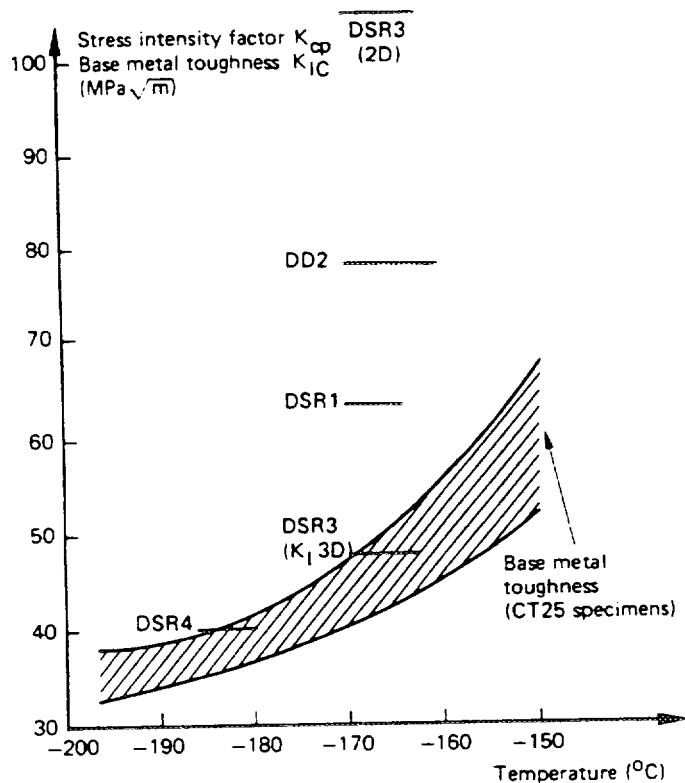


Cross-section of DSR4 mock-up after fracture

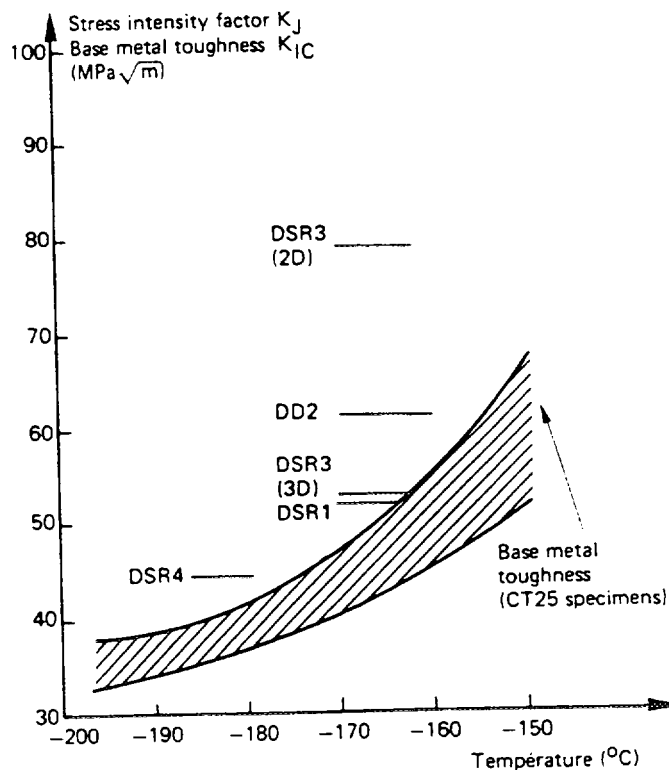


Cross-section of DSR3 mock-up after fracture

Figure 4 - Interpretation of clad mock-ups tests with elastic and elastic-plastic analyses



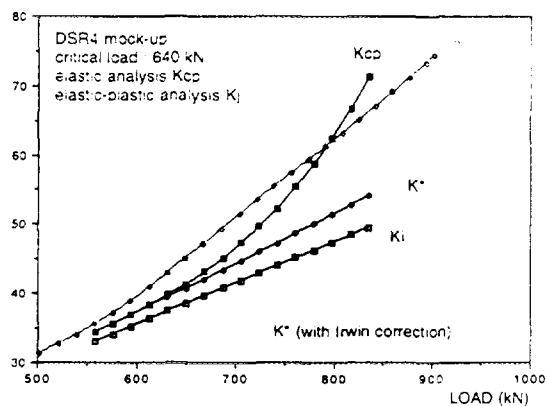
Interpretation of tests with elastic analyses.



Interpretation of tests with elastic-plastic analyses.

Figure 5 - Comparison between elastic and elastic-plastic analyses (cladded specimens)

STRESS INTENSITY FACTOR IN BASE METAL
 K_{cp} , K_I ($\text{MPa m}^{0.5}$)



STRESS INTENSITY FACTOR IN BASE METAL
 K_{cp} , K_I ($\text{MPa m}^{0.5}$)

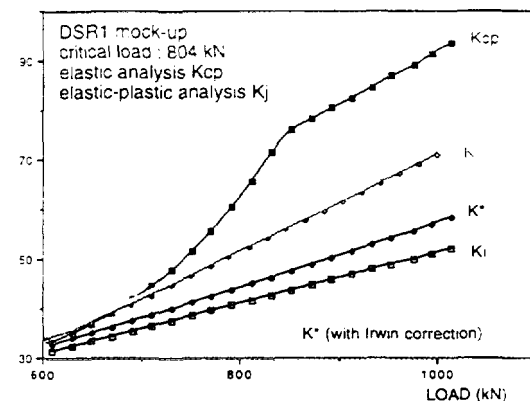


Figure 6 - Crack opening in base metal for DSR4 and DD2 specimens. Comparison between elastic and elastic-plastic calculations

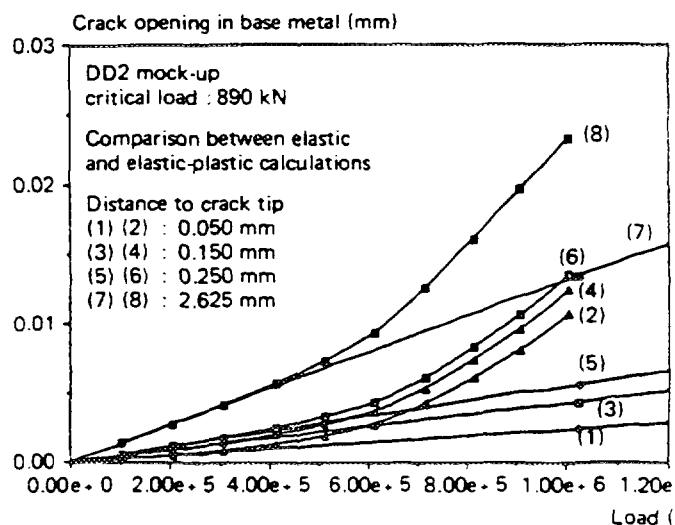
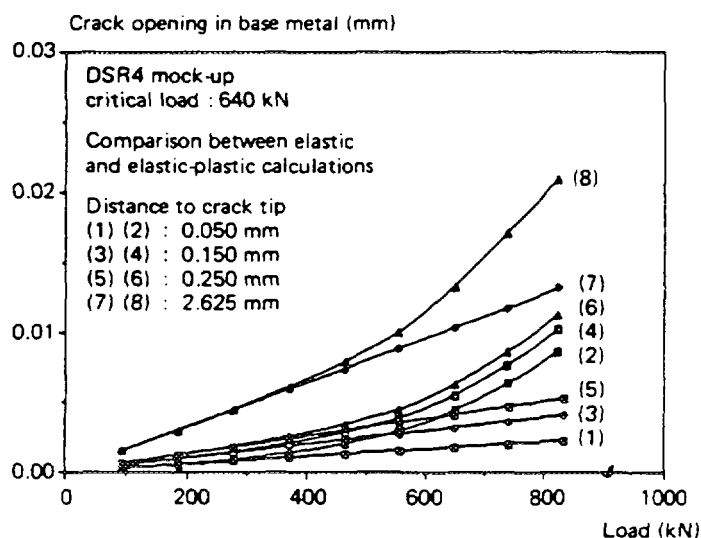


Figure 7 - Mesh of DSR3 specimen

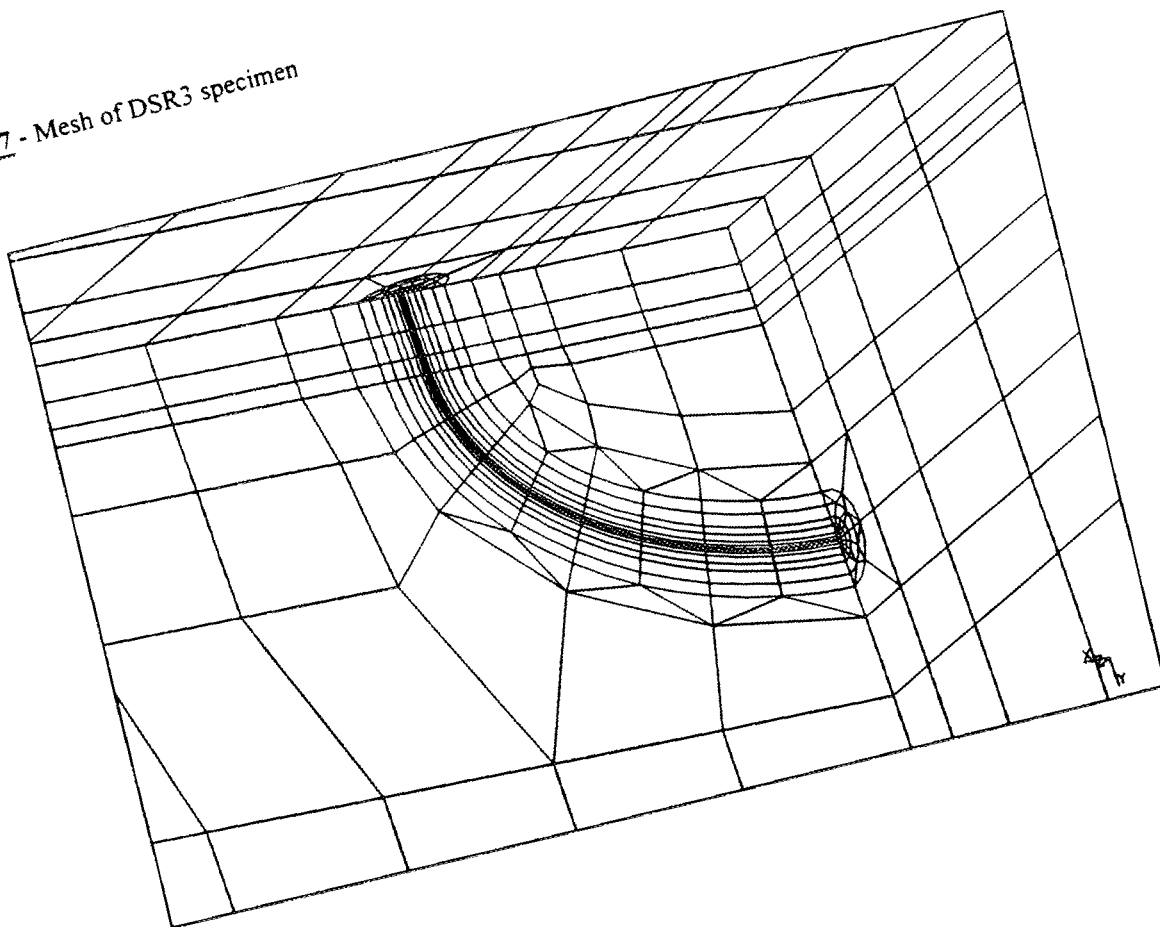


Figure 8 - Mesh of DD2 specimen

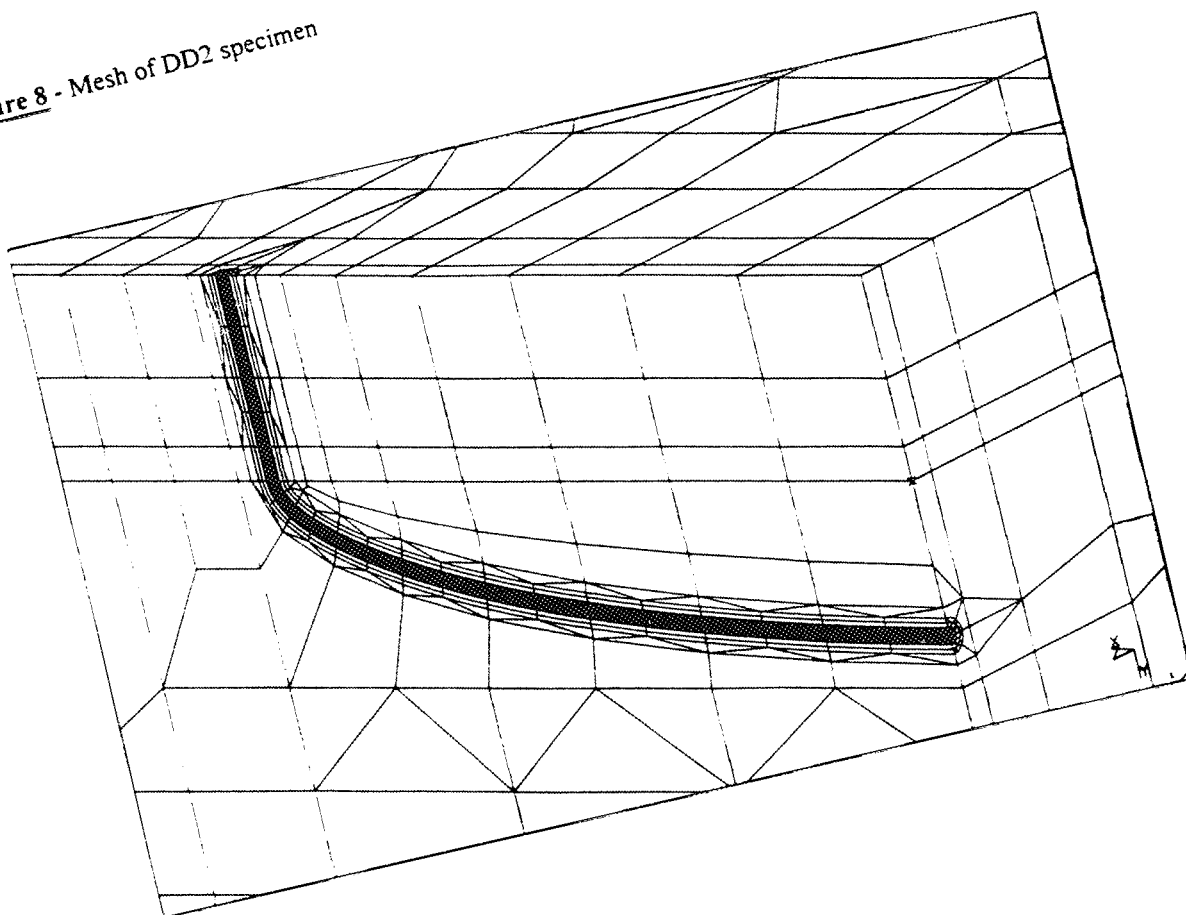


Figure 9 - Evolution of the stress intensity factor K_I along the crack front in base metal (DSR3)

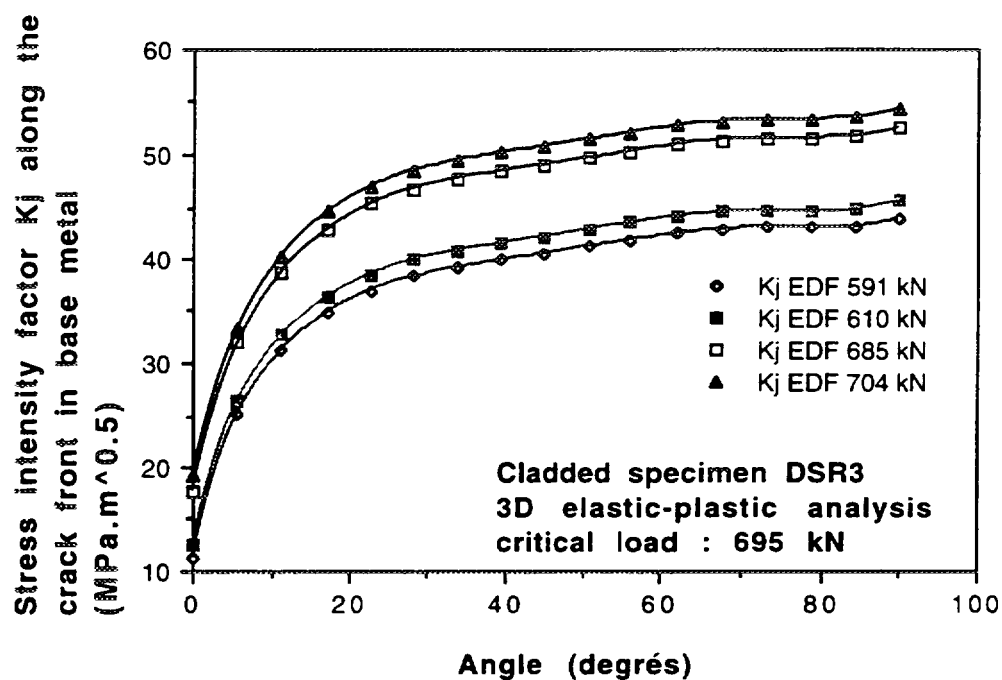


Figure 10 - Stress intensity factor K_I versus load at the deepest point in base metal (DSR3)

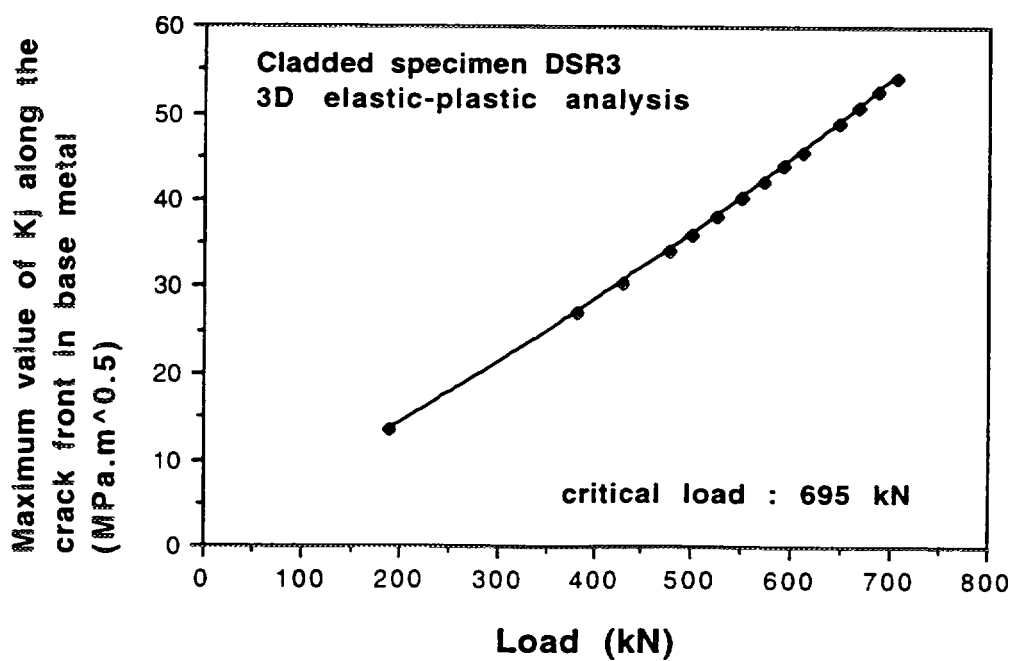


Figure 11 - Evolution of the stress intensity factor K_I along the crack front in base metal. Comparison between elastic and elastic-plastic analyses (DD2)

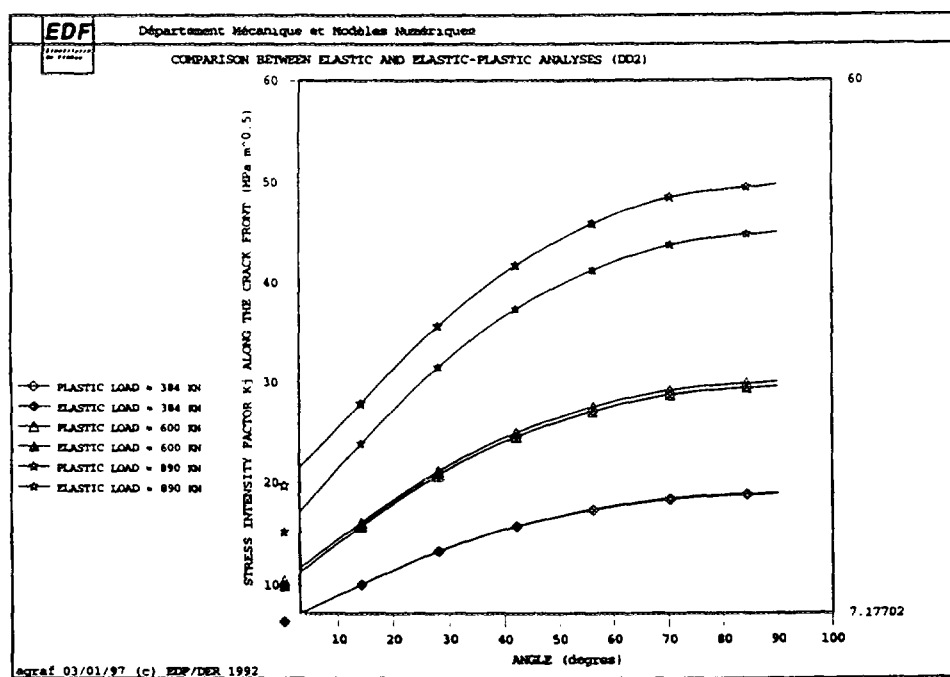


Figure 12 - Stress intensity factor K_I versus load at the deepest point in base metal. Comparison between elastic and elastic-plastic analyses (DD2)

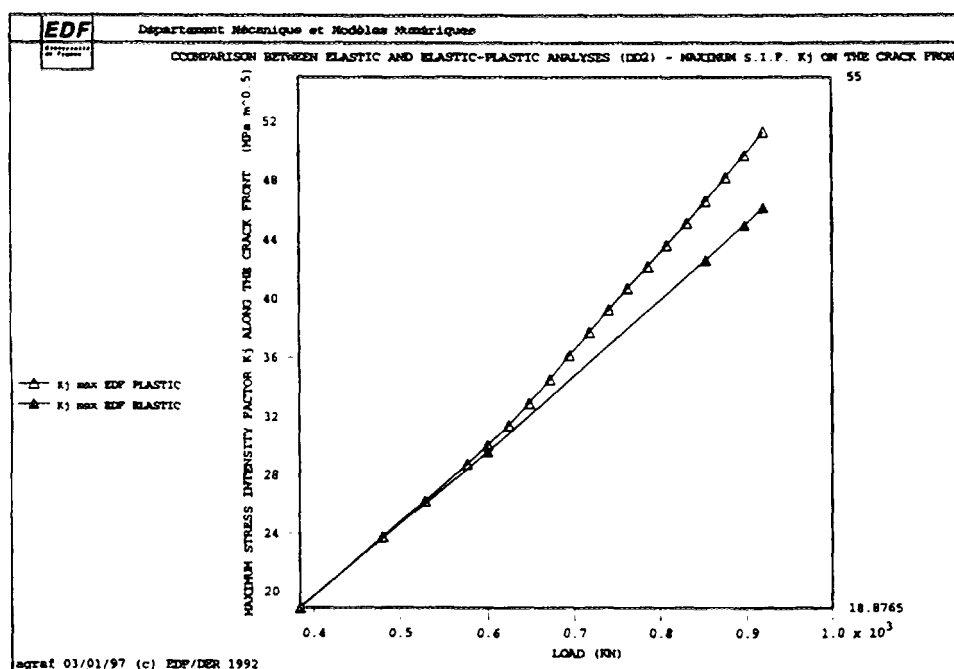


Figure 13 - Probability of failure versus load (DSR3)

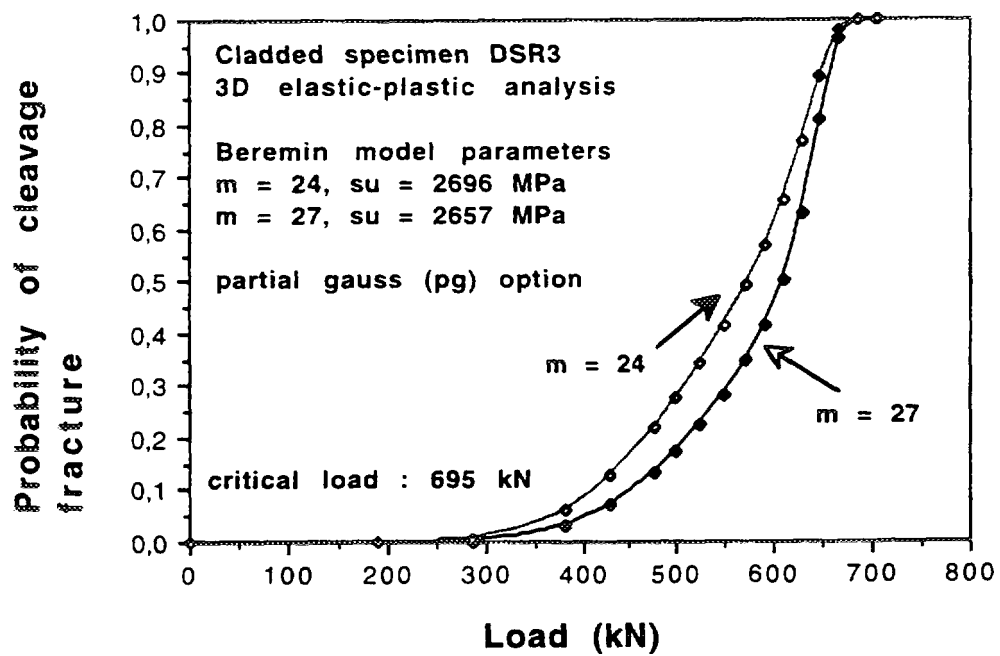


Figure 14 - Probability of failure versus load (DD2)

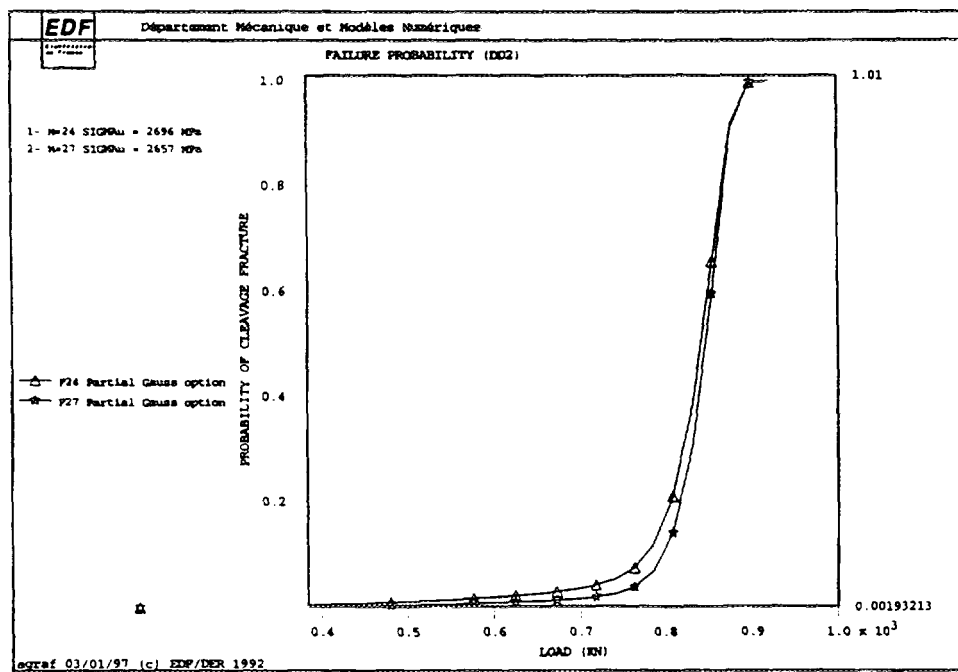
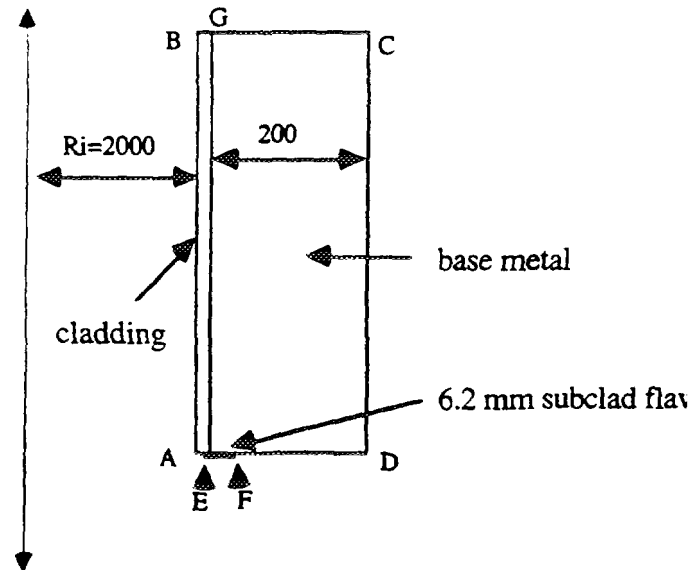
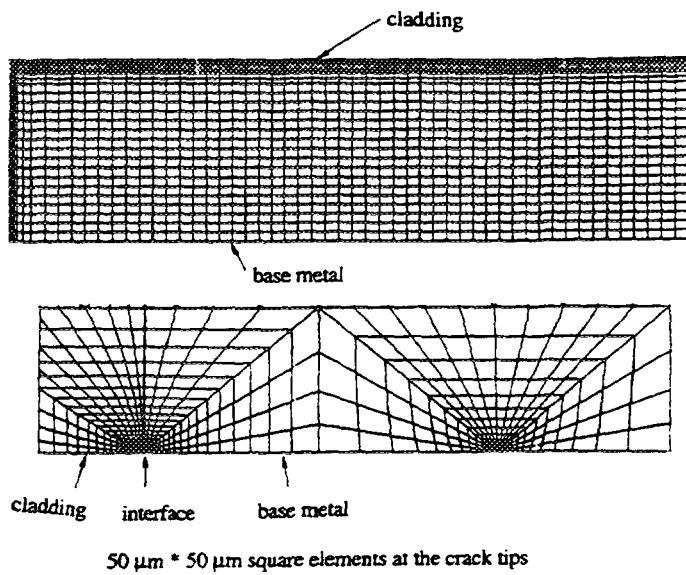
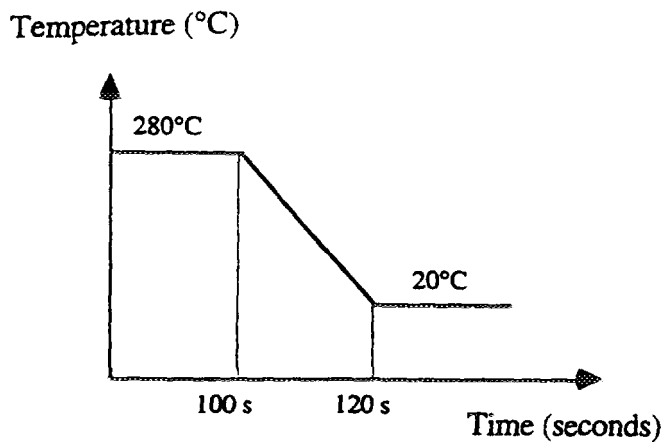


Figure 15 : Finite element model of the vessel (6.2 mm deep subclad flaw)



**GEOMETRY OF THE CLADED VESSEL
(6.2 mm DEEP SUBCLAD FLAW)**

Figure 16 : Thermal calculation of the cladded vessel (6.2 mm deep subclad flaw)



THERMAL TRANSIENT APPLIED TO THE VESSEL

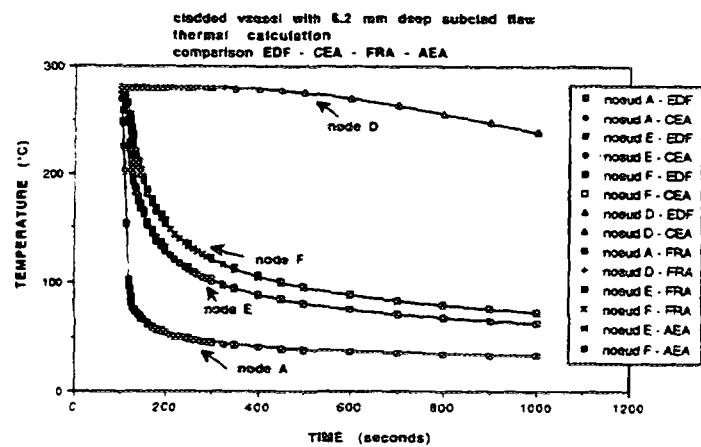


Figure 17 : Stress intensity factor K_I in cladding and base metal during the transient (elastic analysis, 6.2 mm deep subclad flaw)

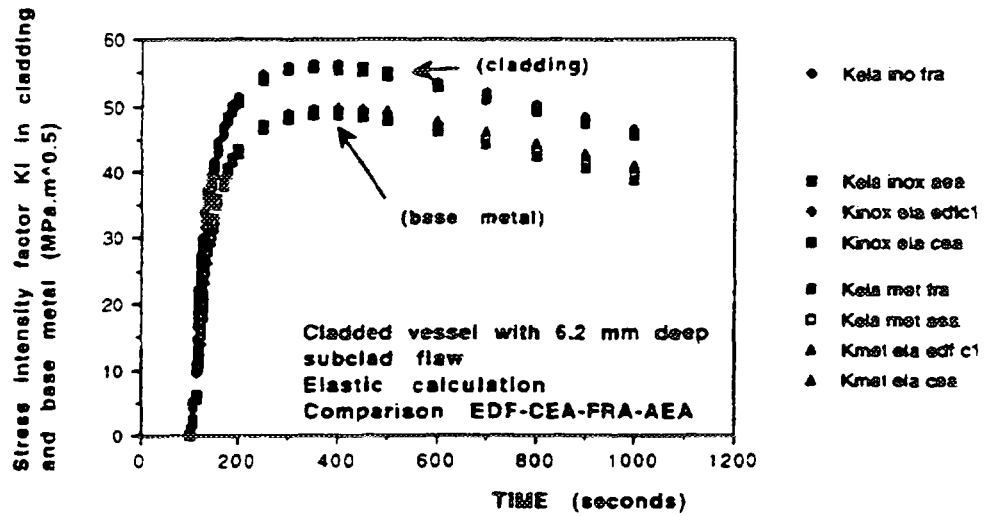


Figure 18 : Opening of the mid point of the crack (elastic and elastic-plastic analyses, 6.2 mm deep subclad flaw)

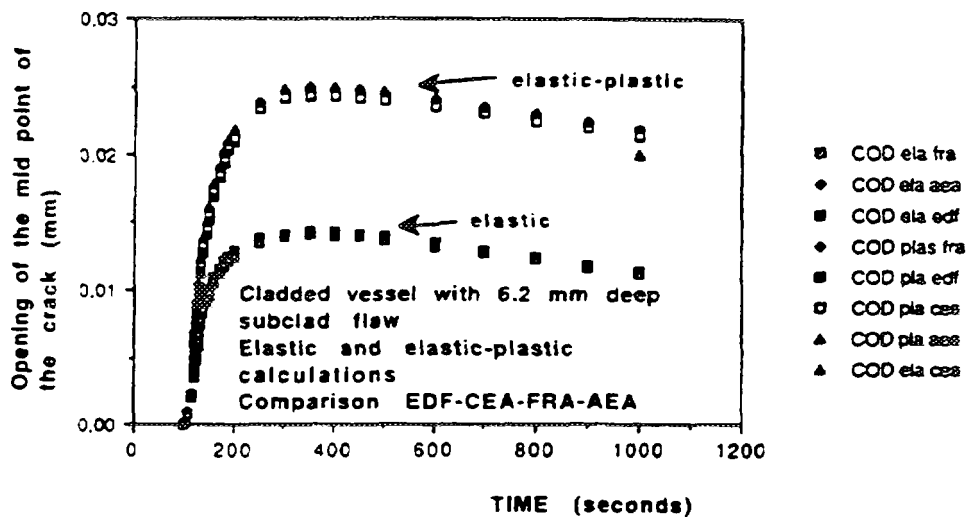


Figure 19 : Evolution of the stress intensity factor K_I during the transient (6.2 mm deep subclad flaw)

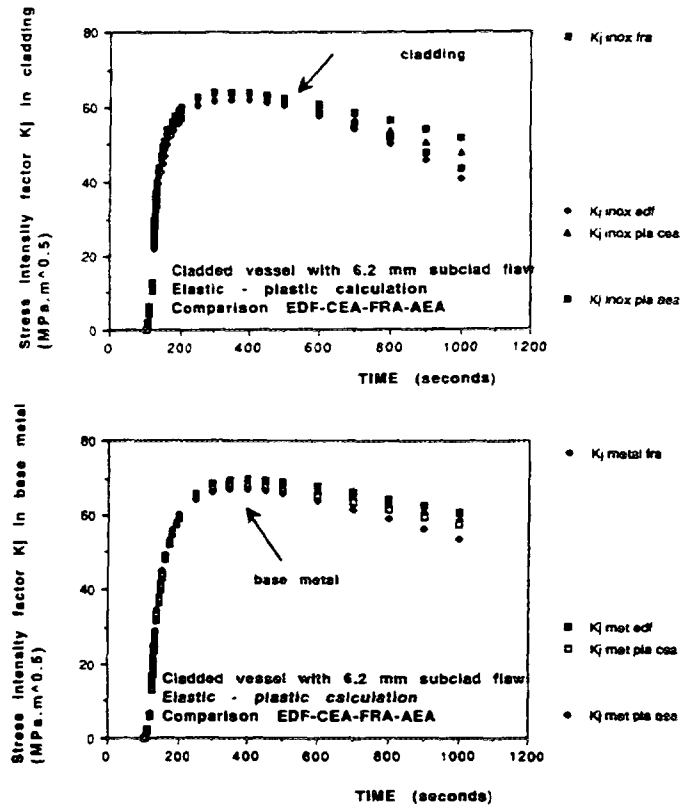


Figure 20 : Evolution of the stress intensity factor during the transient. Comparison between elastic and elastic-plastic analyses (6.2 mm deep subclad flaw)

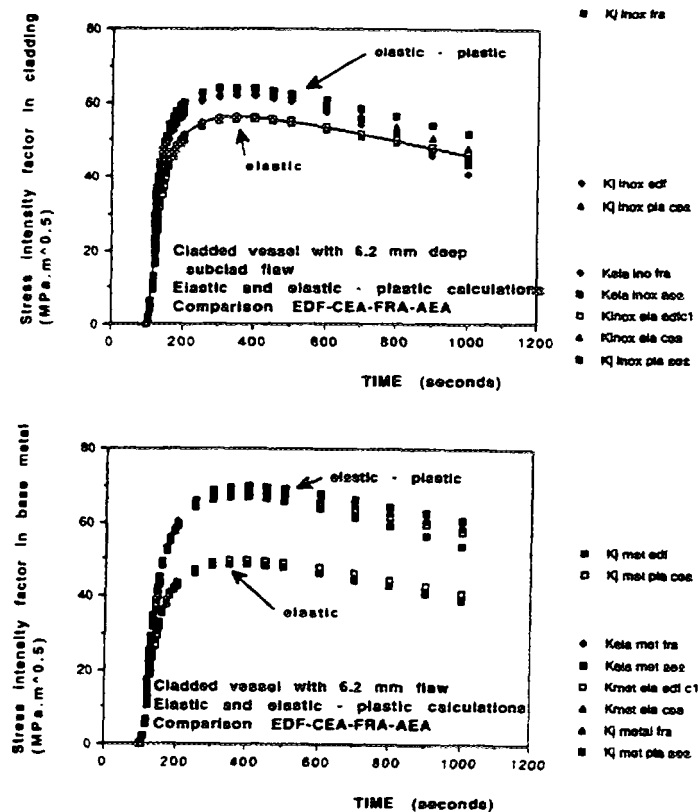


Figure 21 : Evolution of Weibull stress and probability of failure during transient (6.2 mm deep subclad flaw)

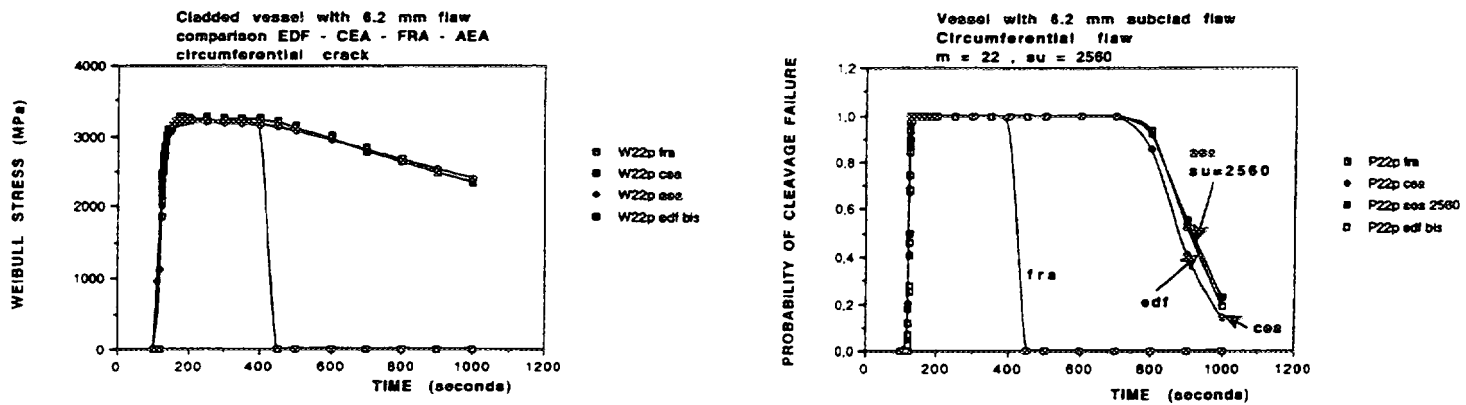
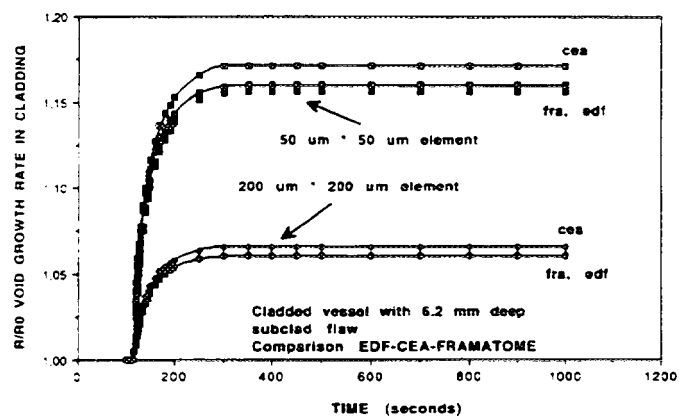


Figure 22 : Evolution of R/R_0 void growth rate during the transient (6.2 mm deep subclad flaw)



EVOLUTION OF R/R_0 VOID GROWTH RATE DURING THE TRANSIENT (6.2 mm DEEP SUBCLAD FLAW)

Figure 23 : Evolution of the stress intensity factor during the transient. Comparison between elastic and elastic-plastic analyses (12.2 mm deep subclad flaw)

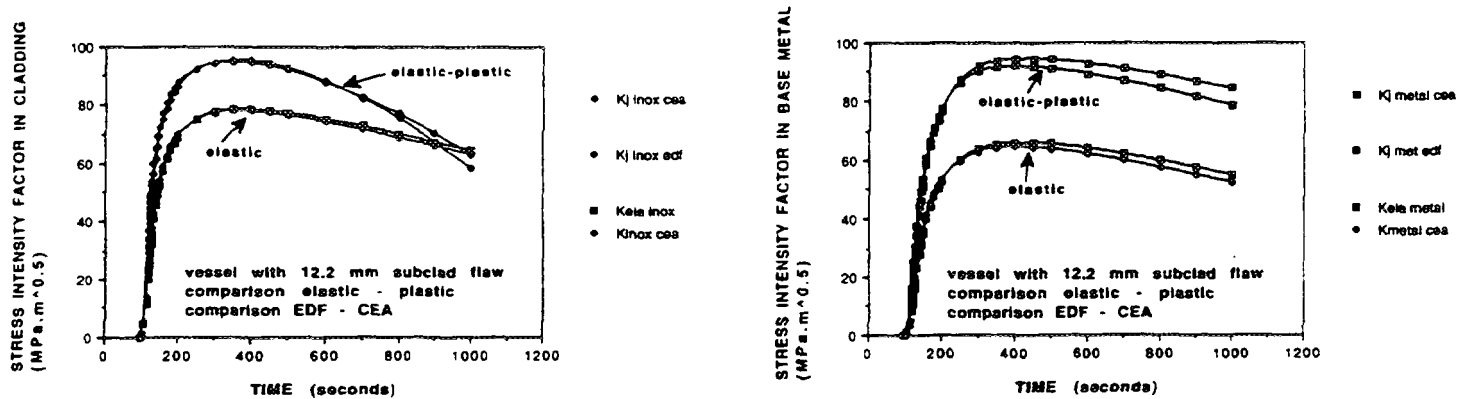
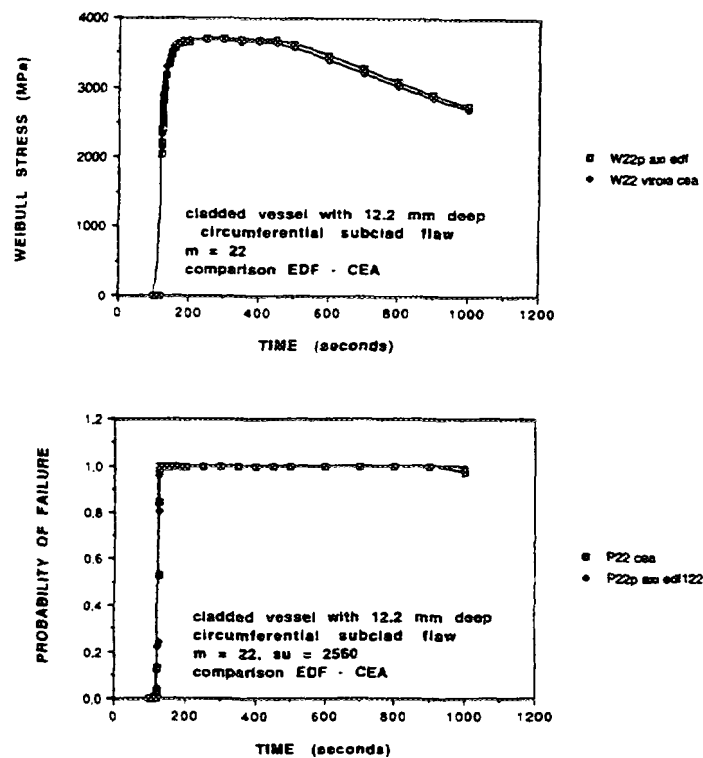


Figure 24 : Evolution of Weibull stress and probability of failure during transient (12.2 mm deep subclad flaw)



EVOLUTION OF σ_w WEIBULL STRESS AND
PROBABILITY OF FAILURE DURING TRANSIENT
(12.2 mm DEEP SUBCLAD FLAW)

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Session 3.
5 MAY, MONDAY

Invited lectures 2

Kataoka, S.	Japan	Status of Pressure Vessel Embrittlement Study in Japanese LWR-s
Server, W.;	USA	Knowledge of Fracture Toughness Properties of Irradiated RPV Steels and Implications to Structural Integrity Assessments.
Popov, A. A.,	Russia	Improvement of Assessment Methods of Brittle Fracture Resistance of WWER Structural Materials



Status of Pressure Vessel Embrittlement Study in Japan
by
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Abstract

The number of nuclear power plants in service for more than 20 years is increasing in Japan. Subsequently, the aging of nuclear power plants will continue to increase and for this reason, the assurance of the safety and reliability of nuclear power plants is becoming more important. Under this circumstance, Japan Government issued a report: "Specific Concepts in Dealing with Nuclear Power Plant High Aging" in April, 1996. This report identified that continuous technology development efforts are important to deal with the issues of nuclear power plant aging, and the following items are extracted for important categories to be developed.

- 1) Aging phenomena evaluation technology
- 2) Inspection / monitoring technology
- 3) Preventive maintenance / repair technology

Japan Power Engineering and Inspection Corporation (JAPEIC) have been implementing various verification test concerning the above items consigned by the Ministry of International Trade and Industry (MITI).

This report outlines the Specific Concepts in Dealing with Nuclear Power Plant High Aging and the past achievements and future plans of various verification tests related to irradiation embrittlement of nuclear reactor pressure vessel, mainly related to Pressurized Thermal Shock (PTS) .

Keywords: PTS, USE, RPV, irradiation embrittlement, fracture mechanics

1. Introduction

With nuclear power generation by light water reactor in Japan now accounting for roughly 30% of the nation's total electric energy generation, nuclear power plays an essential role in national power supply. It is also anticipated that the light water reactor will be dominant in nuclear power

generation for a considerable length of time in the future. On the other hand, the number of nuclear power plants now in service for more than 20 years has increased, and the aging of nuclear power plants will progress in the order of their commissioning date, thus necessitating the need to exercise continued efforts in the maintenance and improvement of nuclear power plant safety and reliability. Against these backgrounds, Japan's efforts to deal with nuclear power plant aging are discussed herein.

2. Current Status of Nuclear Power Plants in Japan and Basic Strategy

As of March, 1996, Japan had 48 nuclear power generation units providing a total output of 42,025,000 kW and accounting for more than 30% of the nation's total electric energy generation. Since 1983, the availability factor of these plants has been maintained at over 70% due to continuous improvement in light water reactor technology and by the stringent management of operation and plant quality. By March 1993, however, some nuclear power plants commissioned into service in the early days of the nuclear age had been operated for more than 25 years. Despite this and in view of the current operating performance of these plants, it is deemed their aging would present no immediate aging problems if the current maintenance practices are continued. (Figure-1, 2)

On the other hand, it would be important to carefully observe the plant component performance and conditions of these senior power plants as aging phenomena which will become apparent first in the senior power plants.

Based on these viewpoints, the major plant components on which evaluation should be performed were defined at first, and technical evaluation was conducted in reference to our knowledge of the aging phenomena appearing in senior nuclear power plants. This evaluation indicated that aged nuclear power plants can generally be operated safely when appropriately managed. There are, however, certain phenomena that develop in line with the aging of nuclear power plant components and structures. It is important, therefore, that a systematic set of procedures by which the aged nuclear power plants can be properly managed be established, this by developing specific procedures and standards for the maintenance and management of aged plants.

Continued technology development is also an important factor in attaining high reliability management. From this aspect, the following three

categories have been defined as areas requiring further intensive study (Figure-3).

- (1) Aging phenomena evaluation technology
- (2) Inspection/monitoring technology
- (3) Preventive maintenance/repair technology

Japan Power Engineering and Inspection Corporation (JAPEIC) have been implementing various exercises concerning the above items on the embrittlement of nuclear reactor pressure vessel consigned by the Ministry of International Trade and Industry (MITI). These JAPEIC's past achievements and future plans are presented as follows. The outline of JAPEIC's activities are also presented in Table-1, 2, and 3.

3. Efforts in aging phenomena evaluation technology

3.1 Efforts in aging phenomena evaluation technology for major component steel materials

In 1985, the PLEX (Plant Life Extension) Project was implemented as a 12-year project to clarify the aging phenomena in major nuclear power plant components and to realize the reliability improvement and life extension of the same.

The PLEX Project incorporated 3 phases. In Phase-I, a feasibility study was conducted to identify the important plant components which are relevant to the extension of plant life.

In Phase-II, the material data required to evaluate and verify life extension technology was studied.

In Phase-III, a comprehensive evaluation work was conducted based on the achievements in Phases-I and -II.

The important plant components identified in Phase-I, and the material data studied in Phase-II, are presented in Tables-4 and -5 for BWRs and PWRs respectively. The achievements of each Phase are shown elsewhere.^{1,2,3}

3.2 Efforts in the irradiation embrittlement evaluation technology of reactor pressure vessel

It is known that the low alloy steels of reactor pressure vessels become embrittled by neutron irradiation. This embrittlement is manifested by a rise in the transition temperature in the ductile-brittle transition curve and a reduction in upper-shelf absorption energy.

The transition temperature rise is an important problem from the aspect of preventing non-ductile fracture and the pressurized thermal shock phenomenon (PTS). Concerning the pressurized thermal shock, tests and research was conducted for 9 years from 1983 as a national project. The achievement was standardized as JEAC4206. An outline of this standard is presented in Paragraph 3.2.1.

The activity of JAPEIC on the reduction in upper-shelf absorption energy of low alloy steels is presented in Paragraph 3.2.2.

3.2.1 Efforts in evaluation technology in transition temperature region

Toward establishing an evaluation method for PTS, a national project was conducted from 1983 to 1991, in which the Japan Power Engineering and Inspection Corporation (JAPEIC) implemented surveys and research into PTS upon consignment by the MITI. Surveys of the PTS transients, fracture toughness tests, material tests related to irradiation embrittlement and model tests were conducted to establish an evaluation method for the PTS phenomena, in order to conduct reactor vessel integrity evaluation under PTS.

Figure-4 presents the PTS evaluation flow. An outline of the evaluation method is discussed below.⁴

(1) Fracture toughness reduction prediction method

To predict reduction in the toughness of reactor vessel steel at the end of its design life and at extended life, testing reactor data and surveillance test data were gathered for base metals and weld metals. These data were statistically treated to establish an irradiation embrittlement prediction equation for base metals and weld metals.

(Base metal)

$$\begin{aligned}\Delta RT_{\text{NDT}} &= [\text{CF}]f^{0.29-0.04\log f} & (1) \\ [\text{CF}] &= -16 + 1210P + 215\text{Cu} + 77\sqrt{\text{Cu} \cdot \text{Ni}} \\ \sigma &= 12 \text{ } ^\circ\text{C}\end{aligned}$$

(Weld metal)

$$\begin{aligned}\Delta RT_{\text{NDT}} &= [\text{CF}]f^{0.25-0.10\log f} & (2) \\ [\text{CF}] &= 26 - 24\text{Si} - 61\text{Ni} + 301\sqrt{\text{Cu} \cdot \text{Ni}}\end{aligned}$$

$$\sigma = 15 \text{ }^{\circ}\text{C}$$

where;

Cu = Copper content (wt.%)

Ni = Nickel content (wt.%)

P = Phosphorus content (wt.%)

Si = Silicon content (wt.%)

f = Neutron fluence (10^{19} m/cm², E > 1 MeV)

σ = Standard deviation ($^{\circ}\text{C}$)

The comparison between the ΔRT_{NDT} value predicted by the above equation and test data using surveillance test for both base metal and weld metal are presented in Figure-5. The error of prediction equation of base metal and weld metal are within the limit of about $\pm 2 \sigma$ to the surveillance test data.

ΔT_{KIC} is derived from the above equation based on the test results of $\Delta RT_{\text{NDT}} = \Delta T_{\text{KIC}}$, and the envelope encompassing their lowest values which are obtained by shifting the initial K_{IC} by ΔT_{KIC} are used as the K_{IC} vs. temperature curve for evaluation of PTS.

(2) Crack propagation evaluation method in PTS phenomenon

A postulated flaw (depth:10 mm, surface length:60 mm) was given to the inner surface of a pressure vessel, and a fracture mechanics evaluation was performed for the selected PTS transient. The PTS transients selected were MSLB (main stem line break), SBLOCA (small break LOCA) and LBLOCA (large break LOCA).

Figure-6 shows the worst enveloped curve of K_I vs. temperature curves according to PTS and TS (Thermal Shock) transients, and the K_{IC} vs. temperature curve at the end of design life and at the extended life. To verify the result of this evaluation, A533B Cl.1 test pieces simulating the same low toughness and having the same thickness as a pressure vessel were manufactured for the test. The test facilities and the test piece for this experiment are presented in Figure-7 and 8. In this experiment, the temperature distribution in the thickness direction and the bending load were controlled to simulate the worst enveloped K_I vs. temperature curve in Figure-6. The lower bound of K_{IC} data of the test piece is expressed by the following equation.

$$K_{IC} = 20.2 + 130\exp\{0.0161(T-108)\} \quad (3)$$

The K_{IC} at the crack tip at extended life end is expressed by the following equation.

$$K_{IC} = 20.2 + 130\exp\{0.0161(T-121)\} \quad (4)$$

The temperature difference between experimentally obtained K_I vs. temperature and K_{IC} vs. temperature was equal to the temperature difference obtained by analytical calculation. This test demonstrated the followings.

- * Unstable crack propagation does not occur at design life end.
- * Unstable crack propagation does not occur at extended life end.
- * The crack depth has a safety margin of at least 2.

Based on the above test results, the evaluation method of PTS phenomena was standardized into JEAC4206.

3.2.2 Efforts in evaluation technology for upper-shelf region

As the effect of neutron irradiation on reactor pressure vessel steel embrittlement, in addition to the rise of transition temperature, there is also the issue of the reduction of absorption energy at upper-shelf region. Concerning the evaluation of the upper-shelf region, JEAC4206 provides that the Charpy absorption energy is 102 J or more in unirradiated material, and 68 J or more after irradiation. However, no method to evaluate integrity at energy of less than 68 J is provided. In the U.S.A., R.G.1.99 Rev. 2 provides a reduction prediction equation at energy less than 68 J concerning the absorption energy reduction at upper-shelf. However, the materials used for the development of this equation differ from Japanese materials, and the data at high irradiation region are not included. For these reasons, it is necessary that a prediction equation be developed which would be appropriate to Japan's domestic pressure vessel steels. Therefore, a verification test was started inside the Nuclear Power Plant Life Management Technology (PLIM) Project in 1996 to verify and standardize the method of evaluating the integrity of pressure vessels in which upper-shelf absorption energy is reduced by irradiation embrittlement.

In this Project, the following verification tests are planned to evaluate the integrity at the upper-shelf region for domestic pressure vessel steel materials by the fracture mechanics method.

- *Basic material tests of sample materials

- * Verification of the prediction equation of upper-shelf absorption energy.

- * Verification of the correlation equation between upper-shelf absorption energy and fracture toughness.

These verification tests will be completed by 2001 with standardization established thereafter. A detailed test schedule is presently under study.

Related to the integrity evaluation of pressure vessel steels, one more project named Structural Assessment of Flawed Equipment (SAF) project is implementing in JAPEIC. In the SAF Project started in 1991, a test sample simulating a real pressure vessel was manufactured. To verify the appropriateness of the flaw behavior evaluation method, small flaws are given to this simulated vessel material to test flaw propagation behavior during service life and fracture behavior after the flaw reaches the propagation limit. In this test, a sample material simulating low toughness below 68J at upper shelf region due to irradiation embrittlement will be used to study flaw behavior. The stable crack propagation behavior of irradiation embrittled pressure vessel steel material will be evaluated by taking into account the achievements of the PLIM Project to implement the integrity evaluation of pressure vessels.

4. Efforts in inspection/monitoring technology

Surveillance test pieces are installed in pressure vessels in order to monitor the effect of neutron irradiation on the vessel material over set periods of time. The effect of neutron irradiation is identified by periodically removing the surveillance test pieces and testing them, then used in the determination of continued service from the aspect of pressurized thermal shock. There is an increasing need to increase the measurement data toward improving the reliability of such monitoring, and to revise the direction by which test pieces are sampled, because the importance of fracture toughness testing has increased from the viewpoint of the PTS phenomenon, because

the importance of compact tension test pieces has increased to evaluate integrity at the upper-shelf region, and because the sampling direction of monitoring test pieces was revised from the LT direction to the TL direction.

Ministerial Ordinances, Notifications and JEAC4201 stipulates the installation of surveillance test pieces. There is, however, no provision concerning the reconstitution of these test pieces. For this reason, test piece reconstitution technology will be established and its applicability to actual reactors will be verified by tests in the PLIM Project. In verifying the applicability of reconstitution technology to actual reactors, a verification test program will be designed to verify the applicability of reconstitution technology to irradiated materials, the reproductability of the reconstitution technology in a hot lab, and the feasibility of reloading reconstituted surveillance test pieces.

5. Efforts in preventive maintenance/repair technology

The materials of the primary system components of PWR plants and the reactor internals of BWR plants are subjected to degradation by neutron irradiation. In the Nuclear Power Plant Maintenance Technology (PMT) Project, a 7-year project started in 1996, an effective surface quality improvement technology for the prevention of damage by SCC and other factors in PWR primary system components and BWR reactor internals will be identified, verification tests will be performed to verify the improvement of corrosion resistance, and the applicability and effectiveness of the surface quality improvement technology as a preventive maintenance tool will be verified.

6. Conclusion

In Japan, a variety of verification tests are being performed to realize high reliability plant management in order to deal with plant aging. In 1996, the PLIM Project and PMT Project were initiated to verify the integrity evaluation technology for aged plant components and the preventive maintenance technology dealing with plant aging. The achievements of these verification tests will be reported in future at appropriate times.

7. References

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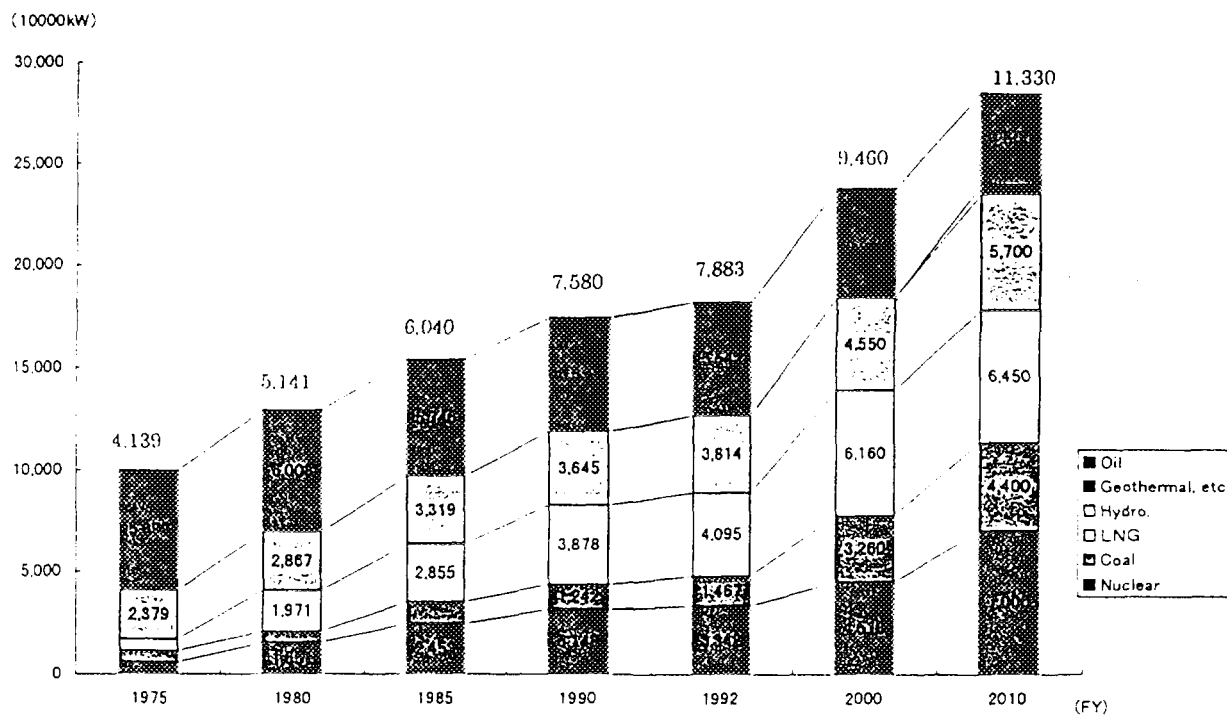


Figure 1 The Trend of Total Electricity Generation & Its Compositions

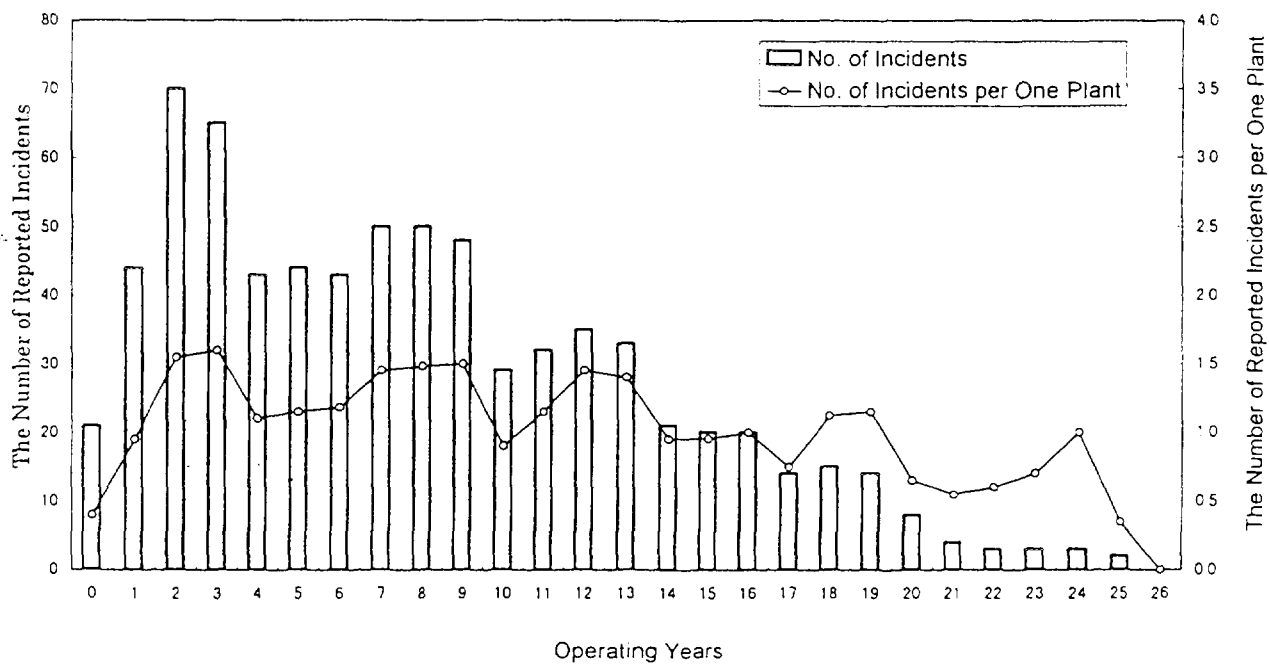


Figure 2 The Number of Reported Incidents of Nuclear Power Plants

Table 1 Outlines of the Projects

Project Name	Objective	Research Outline
PTS (Pressurized Thermal Shock of Nuclear Reactor Pressure Vessel)	The objective is to verify the integrity of reactor pressure vessel on pressurized thermal shock.	<ul style="list-style-type: none"> • Fracture mechanics test. • Model verification test.
PLEX (Nuclear Power Plant Life Extension Technology Development)	In order to extend the life of nuclear power plants, it is intended to collect material data required for prediction of nuclear power plant life, to establish the life prediction methodology.	<ul style="list-style-type: none"> • Feasibility study for plant life extension. • Verification test and life extension technology evaluation. • Overall evaluation.
SAF (Structural Assessment of Flawed Equipment)	In order to prove the integrity of nuclear power plant equipment and piping during their service life, this verification test is planned to conduct fracture mechanics experiments and analyses concerning small flaws which are hypothesized on power plant structures.	<ul style="list-style-type: none"> • Study of flaw behavior evaluation. • Model verification test.
PLIM (Nuclear Power Plant Life Management Technology)	The objective is to verify the technology regarding various material aging phenomena for safety related major components of nuclear power plants.	<ul style="list-style-type: none"> • Verification test of evaluation method on irradiation embrittlement of RPV. • Verification test of reconstitution method of surveillance specimen. • Verification test of thermal aged duplex stainless steels.
PMT (Nuclear Power Plant Maintenance Technology)	The objective is to verify the availability of surface repairing technology for aging components of nuclear power plants.	<ul style="list-style-type: none"> • Verification test for irradiated specimen which will be applied various surface repairing technology.

Table 2 R&D Items of JAPEIC on Reactor Pressure Vessel

Project Name	R&D Categories			Major Aging Phenomena				
	Examination & Monitoring Technology	Preventive Maintenance & Repair Technology	Aging Evaluation Technology	Fatigue	Thermal Aging	Irradiation embrittlement	Corrosion	IASCC
PTS			○			○		
PLEX			○	○	○		○	○
SAF			○					
PLIM	○		○		○	○		
PMT		○						○

Table 3 Schedule of the JAPEIC'S Projects

	'83	'84	'85	'86	'87	'88	'89	'90	'91	'92	'93	'94	'95	'96	'97	'98	'99	2000
PTS																		
PLEX																		
SAF																		
PLIM																		
PMT																		

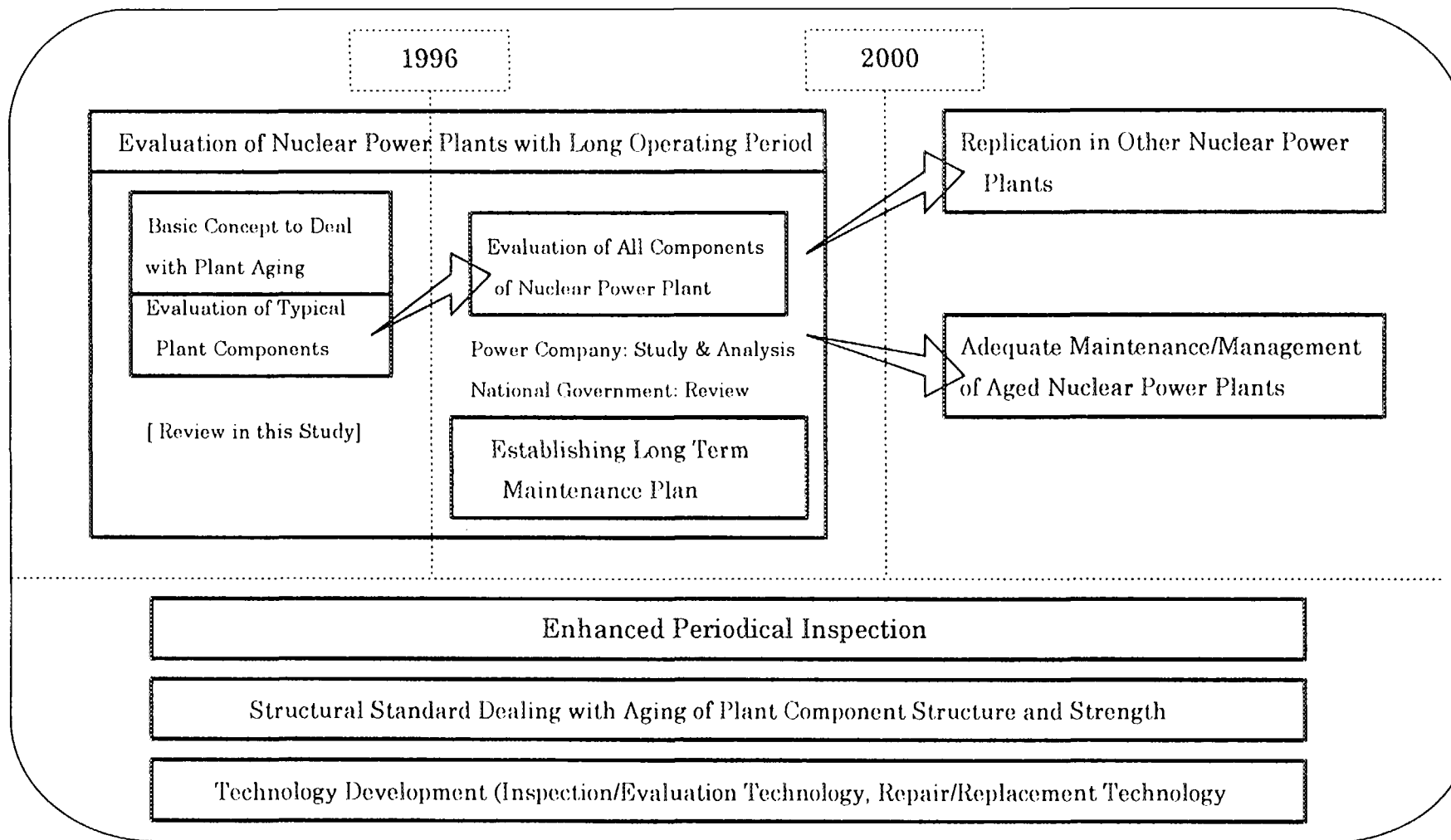


Fig. 3 Entire Schedule for Aged Nuclear Power Plants

Table 4 Evaluation Matrix and Material Data (BWR)

Main Components	Structural Components Material	Aging Phenomena				
		Thermal Embrittlement	Irradiation Embrittlement	Fatigue	Irradiation Assisted SCC	Insulation Degradation
Reactor Pressure Vessel	Low Alloy Steel		○	○		
	Carbon Steel			○		
	Stainless Steel			○		
	Nickel Base Alloy			○		
Core Internals	Stainless Steel		○	○	○	
Reactor Primary System Piping	Stainless Steel			○		
	Carbon Steel			○		
PLR Pump	Duplex Stainless Steel	○		○		
PLR Pump In-let and Out-let Valve	Duplex Stainless Steel	○		○		
Electrical Cable						○

Table 5 Evaluation Matrix and Material Data (PWR)

Main Components	Structural Components Material	Aging Phenomena				
		Thermal Embrittlement	Irradiation Embrittlement	Fatigue	Irradiation Assisted SCC	Insulation Degradation
Reactor Vessel	Low Alloy Steel		○	○		
	Nickel Base Alloy			○		
Pressurizer	Low Alloy Steel			○		
	Stainless Steel			○		
Reactor Internals	Stainless Steel		○	○	○	
Reactor Coolant Pump	Duplex Stainless Steel	○		○		
Steam Generator	Low Alloy Steel			○		
	Carbon Steel			○		
	Nickel Base Alloy			○		
Reactor Coolant Piping	Duplex Stainless Steel	○		○		
Electrical Cable						○

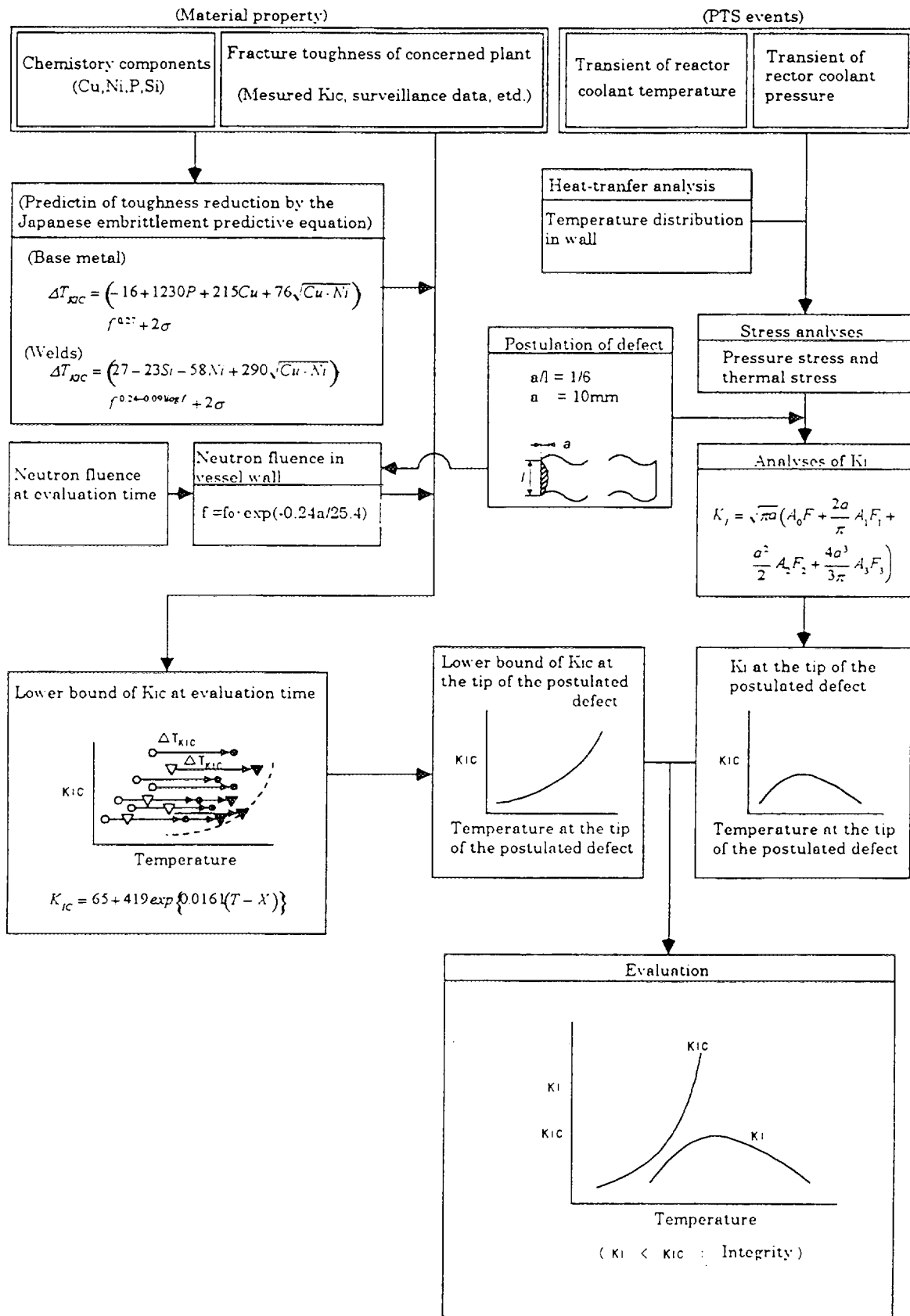


Figure 4 Flow of PTS evaluation

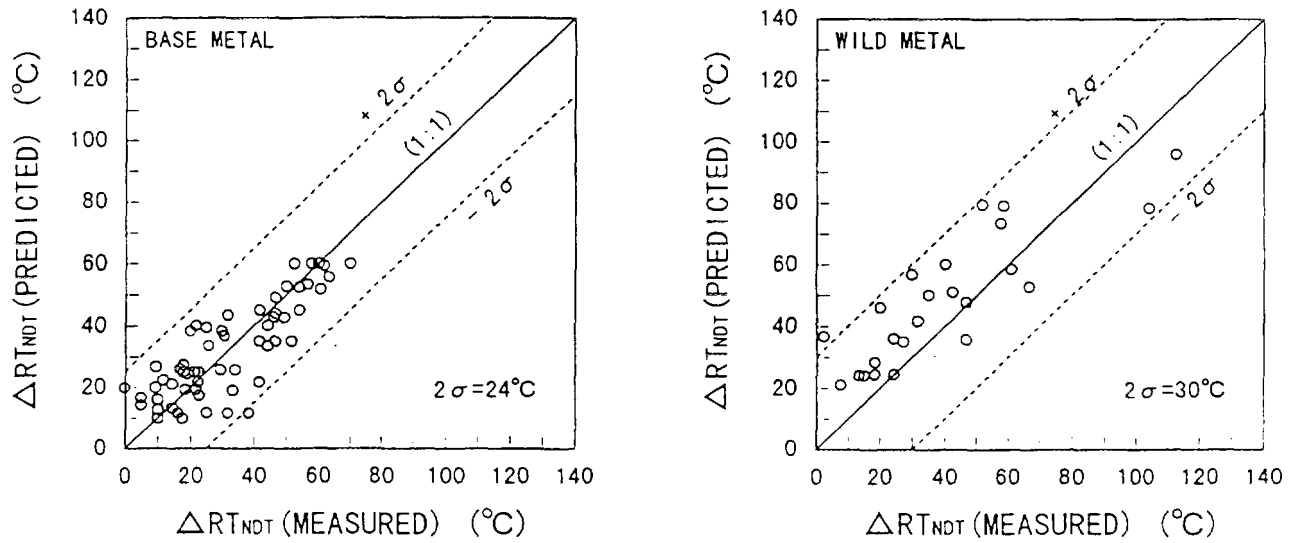


Figure 5 Comparison between the predicted ΔRT_{NDT} and measured ΔRT_{NDT} for the Japanese PWR plant's surveillance data

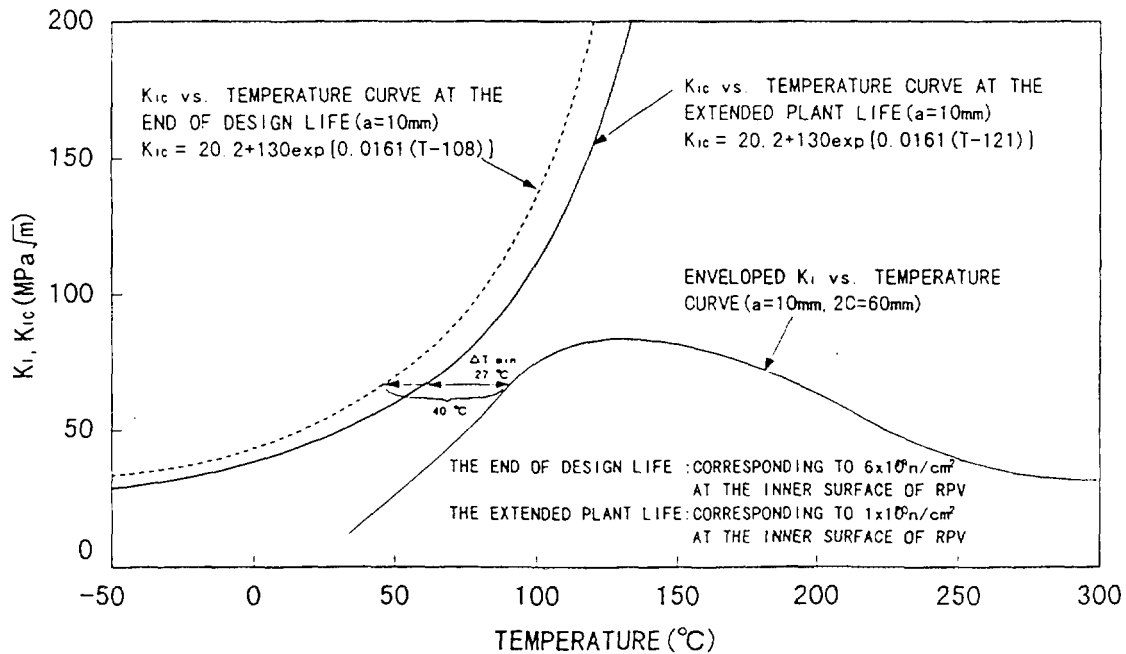


Figure 6 Comparison of envelopes K_I vs. temperature curve with K_{IC} vs. temperature at the end of design life and extended life

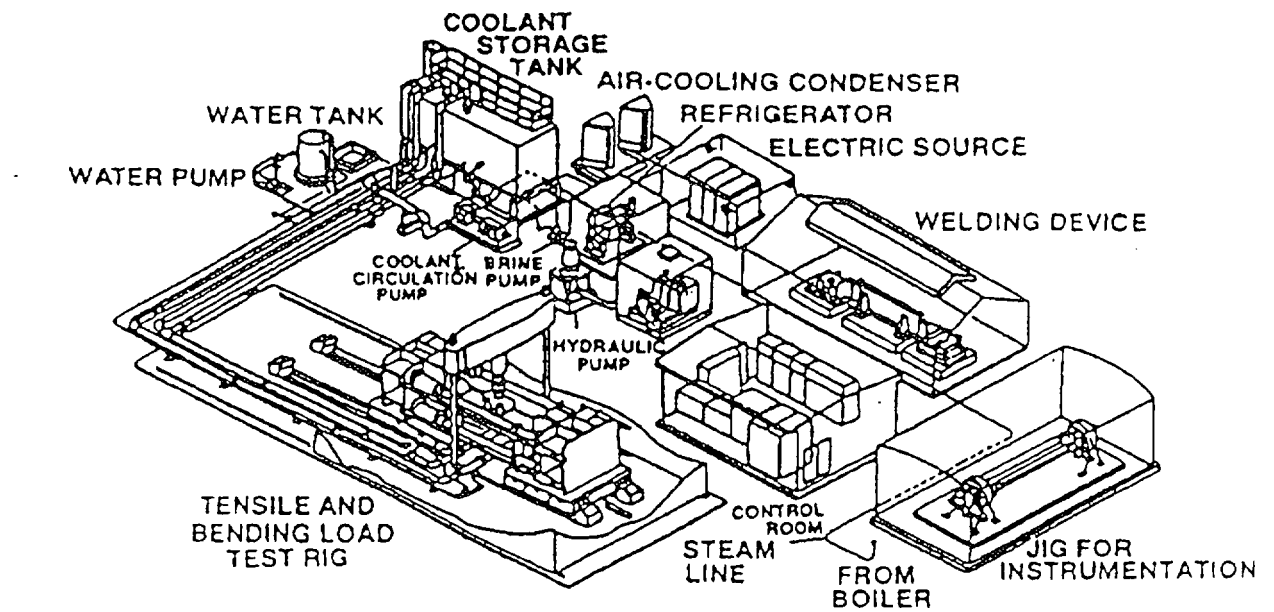


Figure 7 Schematic drawing of test facility.

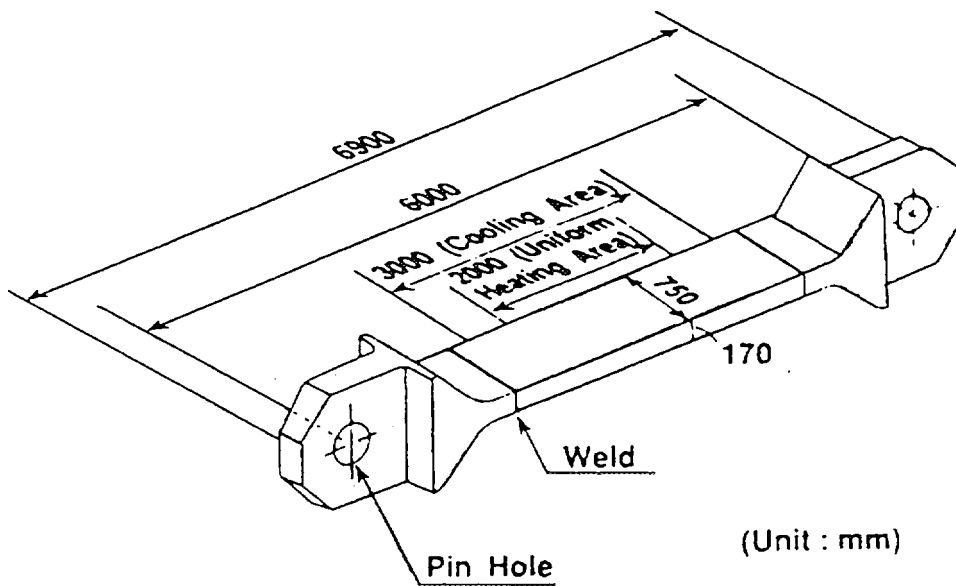


Figure 8 A large flat specimen for modal test



On the Proper Fracture Toughness Properties to be Used for Pressurized Thermal Shock Evaluations

by

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Embrittlement of nuclear reactor pressure vessel steels has been monitored through extensive surveillance programs conducted routinely for U.S. nuclear power plants. The level of embrittlement is factored into normal operating procedures for heatup and cooldown of the vessel. Additionally, since the early 1980s, pressurized thermal shock (PTS) has been a non-design condition that all U.S. plants have had to prove adequate toughness relative to a severe overcooling and pressure-increasing transient event. The regulations in the U.S. were revised in 1985 to include a simple screening criterion which was called the PTS Rule (Title 10, Code of Federal Regulations, Part 50.61). This screening criterion is for the value of RT_{PTS} which cannot exceed 270°F (132°C) for base materials and axial welds and 300°F (149°C) for circumferential welds. RT_{PTS} is the projected value of the reference temperature, RT_{NDT} , that corresponds to the end-of-license fluence for the vessel. Although these values are termed screening criteria, in practice they are really limits that require serious attention if they are going to be reached.

In the early 1990s, a plant in the U.S. was projected to exceed these screening limits even before the end-of-license based upon very conservative re-evaluation of the degree of toughness degradation. This plant was Yankee Rowe, one of the earliest nuclear plants built in the U.S. There were several other integrity issues for the Yankee Rowe vessel in addition to the level of radiation embrittlement. The ability to perform a reliable inspection of the beltline region was questionable due to the type of spot-welded cladding that was used. Other issues had to do with defining the PTS transients and the severity of the transients based upon operating actions and reliability. Later studies indicated that the assumptions used by NRC in evaluating the Yankee Rowe embrittlement were very conservative.

The traditional approach in the U.S. for evaluating PTS has relied upon probabilistic studies in which the toughness has been based upon the data used to generate the lower bound ASME Code K_{IC} and K_{IR} curves. A mean curve through this data with a Gaussian statistical distribution assumed, except for a lower bound cutoff of somewhere between 2 and 3 standard deviations, has been used. The RT_{NDT} normalizing concept has been maintained which then requires the measured shift in Charpy V-notch toughness at the 41 J (30 ft-lb) energy level be used to adjust the position of the Code curves. The Master

Curve method provides a unique alternative in providing a much better measure of real fracture toughness, plus the opportunity to use a more refined statistical distribution using Weibull statistics. There are active moves in the U.S. to Standardize and Codify the Master Curve (also termed T_0 method). Benefits to both deterministic and probabilistic analyses will be realized since more realistic measures of toughness can be used.

Background

- Fracture toughness of irradiated RPV steels is a key element of a PTS evaluation
- Fracture toughness is needed over a temperature range extending into the transition regime for RPV welds and base metal irradiated to different levels
- Surveillance program materials should be utilized to the maximum extent possible
- PTS evaluations have been performed both using deterministic and probabilistic methods
 - Deterministic approaches require some sort of lower bound estimates of toughness as a function of temperature or a mean curve with a safety factor added later (or both)
 - Probabilistic methods require a statistic mean curve and an appropriate definition of the distribution characteristics

U.S. PTS Evaluations

- Combined deterministic and probabilistic calculations were used to develop the NRC PTS Rule (10 CFR Part 50.61) in 1985
- Projected value of reference temperature, RT_{NDT} , at end-of-license, termed RT_{PTS}
 - Circumferential welds: $RT_{PTS} \leq 149^{\circ} \text{ C } (300^{\circ} \text{ F})$
 - Axial welds and base metal: $RT_{PTS} \leq 132^{\circ} \text{ C } (270^{\circ} \text{ F})$
- Emphasis was placed upon probabilistic justification which included Regulatory Guide 1.154 for assessing future plant-specific probabilistic evaluations
- Yankee Rowe was the first vessel to attempt to use Regulatory Guide 1.154 and a detailed plant-specific PTS evaluation to substantiate continue plant operation

Yankee Rowe (continued)

- Other important vessel integrity issues were related to flaw distribution which hinged on a reliable inspection and transient severity as dictated by plant operation
- NRC had a difficult time assessing the Yankee Rowe probabilistic analysis since many diverse fields are brought together
- Low temperature (wet) thermal annealing was being considered as a mitigative measure (similar to BR3)
- Economics and uncertainty in the regulatory process finally led to early retirement of Yankee Rowe in 1992
- Later studies and analyses (some related to the sister vessel BR3 in Belgium) have shown that the degree of embrittlement in the Yankee Rowe materials was not nearly as severe as projected by NRC

Yankee Rowe Issues

- Yankee Rowe was being evaluated as a lead plant for license renewal (extension) in the late 1980s and early 1990s, but key issues concerning vessel integrity were revealed which led to early shutdown
- Lack of knowledge concerning vessel material embrittlement was a key concern
 - RPV materials chemistry (and its variability) were not clearly known
 - Surveillance program results were limited and inadequate
 - Projections of embrittlement were made by NRC consultants based upon conservative approaches that indicated an immediate problem
 - A supplemental surveillance program was initiated using surrogate materials, but the question of surrogate quality was raised
- A complete probabilistic evaluation using Regulatory Guide 1.154 was conducted, but other issues and generic questions were raised

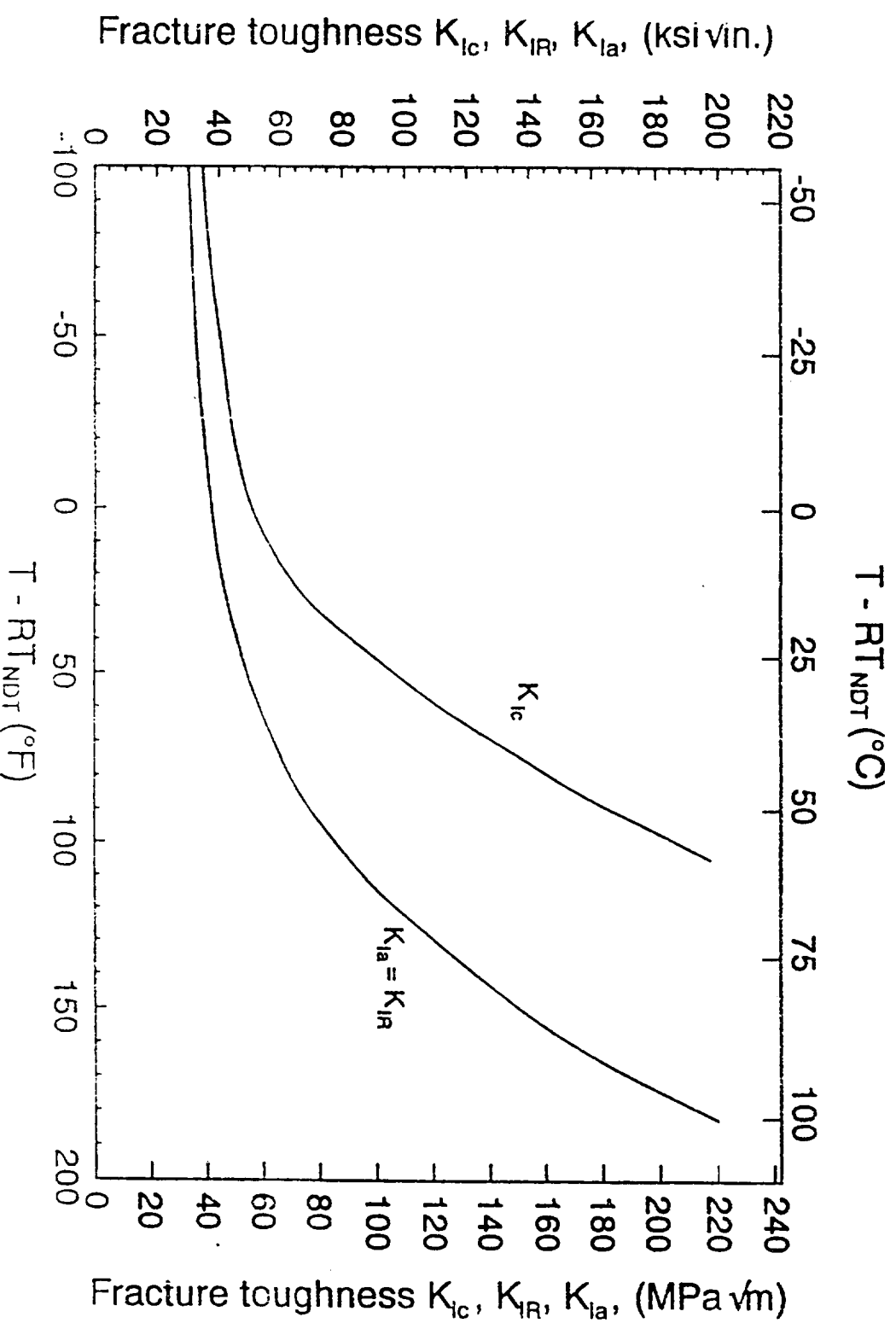
Standard Approach for Fracture Toughness of Irradiated RPV Steels

- Assume that we have a general fracture toughness curve shape that is dictated by a fracture parameter that we can easily measure from surveillance capsule materials
- Ideally, the fracture parameter would be fracture toughness itself, but limited irradiation space and the infancy of fracture mechanics going back to the 1960-1970 time frame has generally forced us to use Charpy V-notch properties
- U.S. approach uses the RT_{NDT} parameter which is initially a marriage of Charpy V-notch at 68 J (50 ft-lb) and drop-weight NDT tests, and later is adjusted based upon Charpy V-notch shifts at 41 J (30 ft-lb)
- Other countries use something similar (such as T_k) which is also based upon Charpy V-notch tests

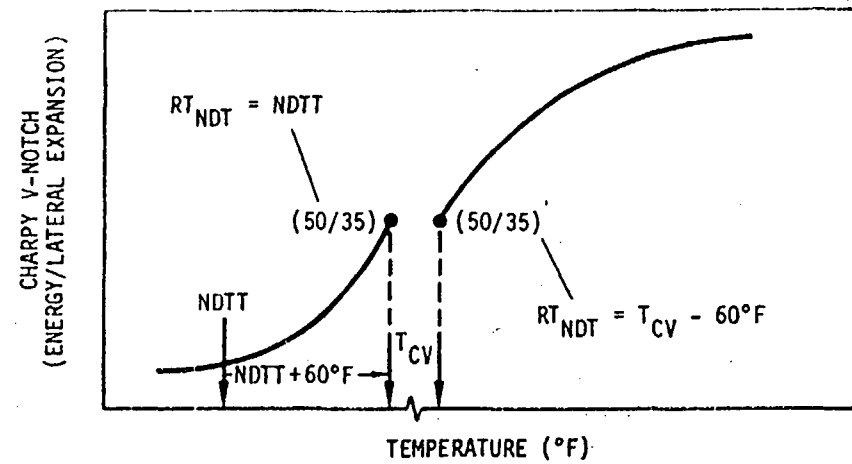
Current U.S. Reference Toughness Approach

- Initial RT_{NDT} is based upon measured nil-ductility temperature, NDT, and attainment of at least 68 J (50 ft-lb) at a temperature 33°C (60°F) above the NDT
- Adjusted RT_{NDT} is based upon measurement and prediction of the shift in Charpy properties at the 41 J (30 ft-lb) level:
 - Adjusted $RT_{NDT} = \text{initial } RT_{NDT} + \Delta RT_{NDT} + \text{margin}$
 - Margin is a comfort factor based upon quality of measured surveillance results and general uncertainty in predictive equations (e.g., Regulatory Guide 1.99, Rev. 2)
- Reference toughness curves in ASME Code provide a fixed curve shape for lower bound fracture toughness for use in performing deterministic vessel integrity evaluations (and determining operating pressure-temperature curves)

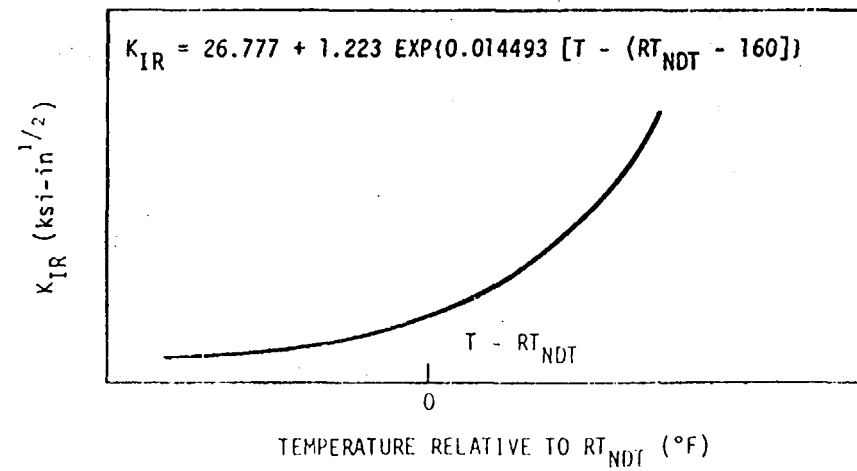
ASME Code Reference Toughness Curves



Initial RT_{NDT} Concept

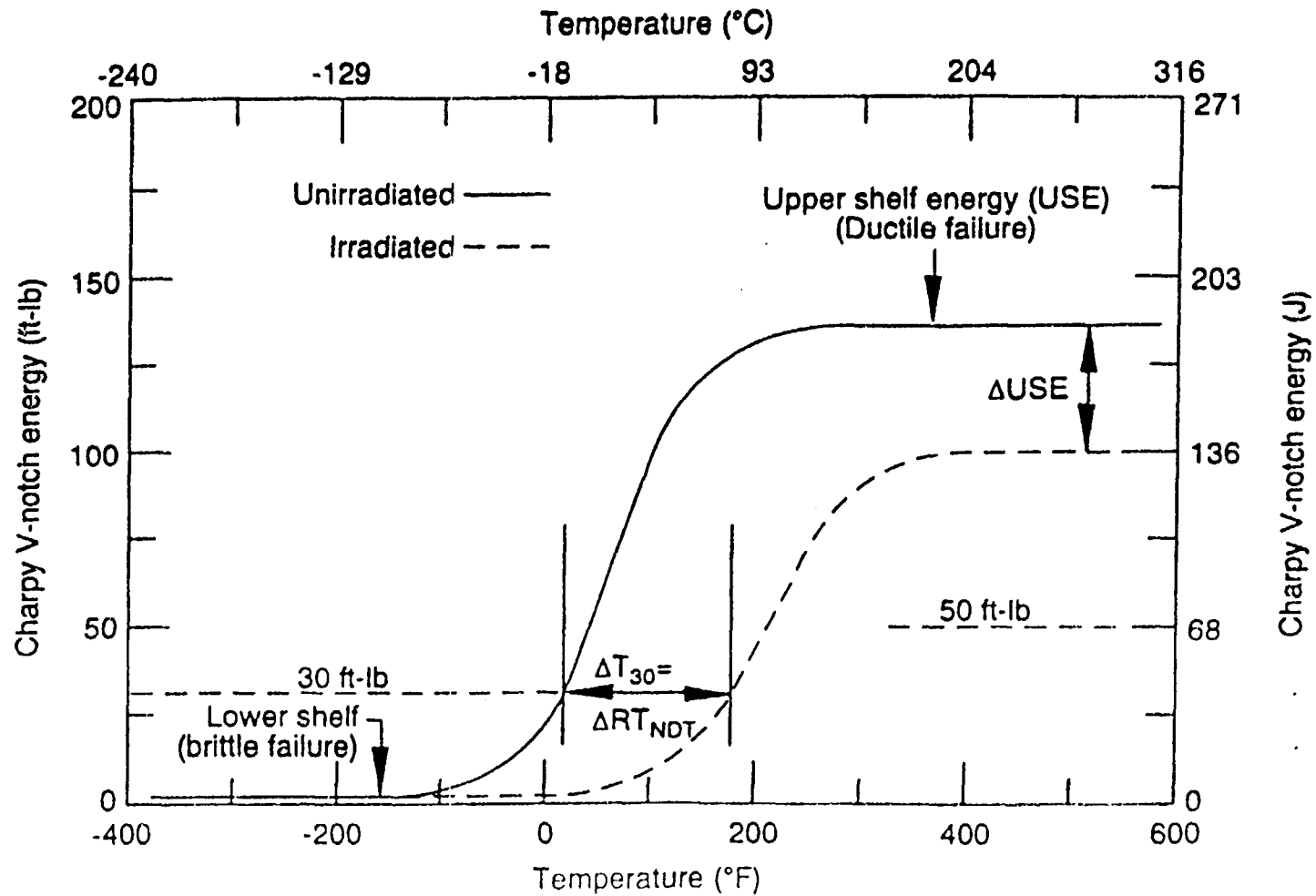


(a)



(b)

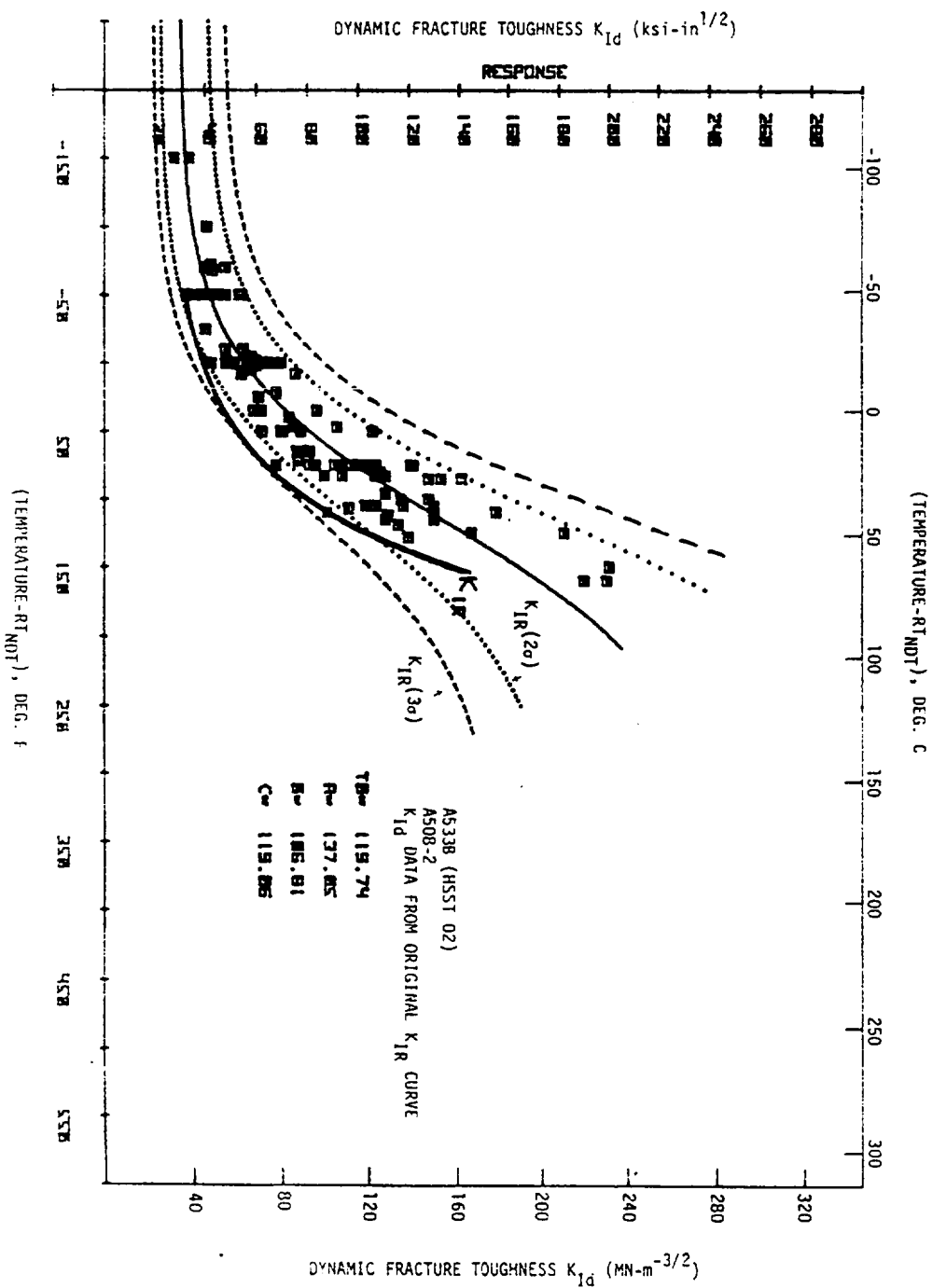
Charpy Curve Shift with Irradiation



Key Issues for Current Reference Toughness Approach

- Heat-to-heat variability was to be removed using RT_{NDT} method; results looking at more than 50 heats of RPV steels showed that absolute temperature was statistically better than $T - RT_{NDT}$
- Curve shape is assumed constant even for some low upper shelf toughness steels; limited results do not show a curve shape change for lower bound toughness
- For probabilistic analyses, researchers have taken the original set of data used to derive ASME reference curves and fit a mean curve
- Statistics applied to this mean curve are generally assumed to be Gaussian, but a lower bound value of 2-3 standard deviations is assumed to account for potential non-Gaussian behavior on the low toughness end

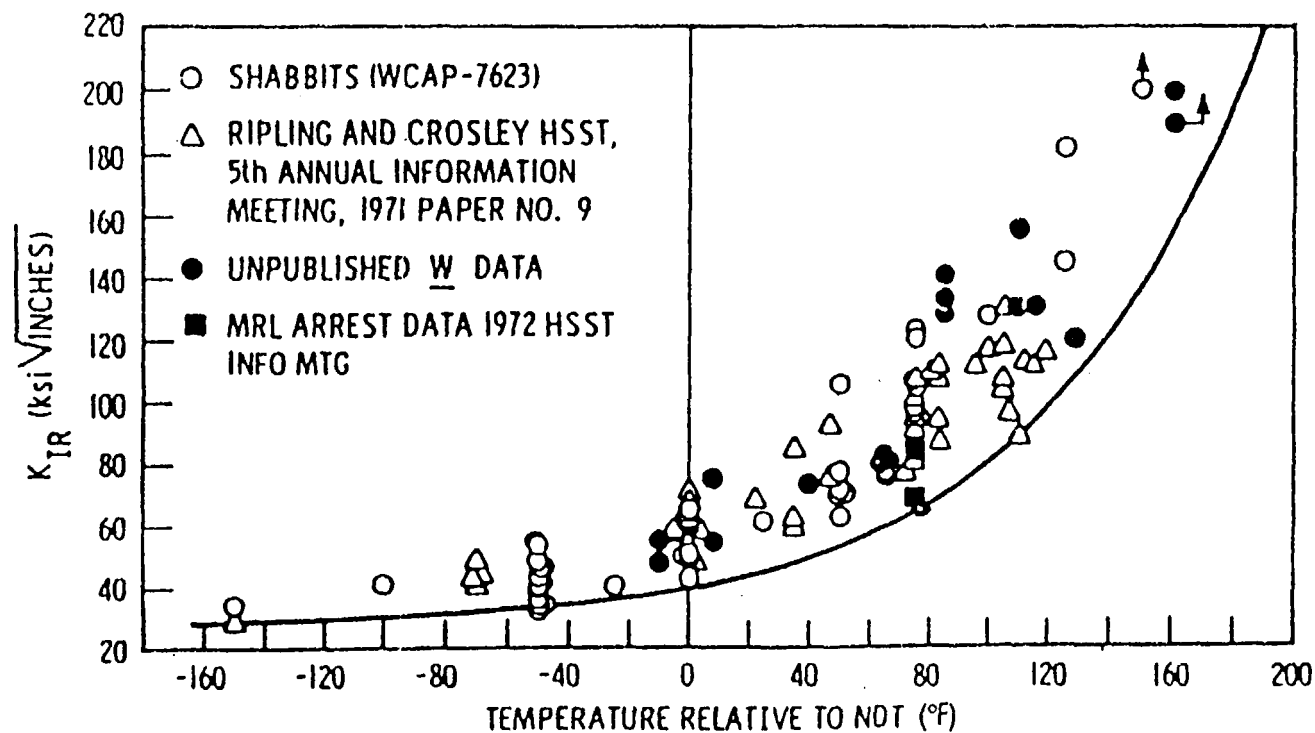
ASME Code K_{IR} Curve Dynamic Data and Statistical Fit



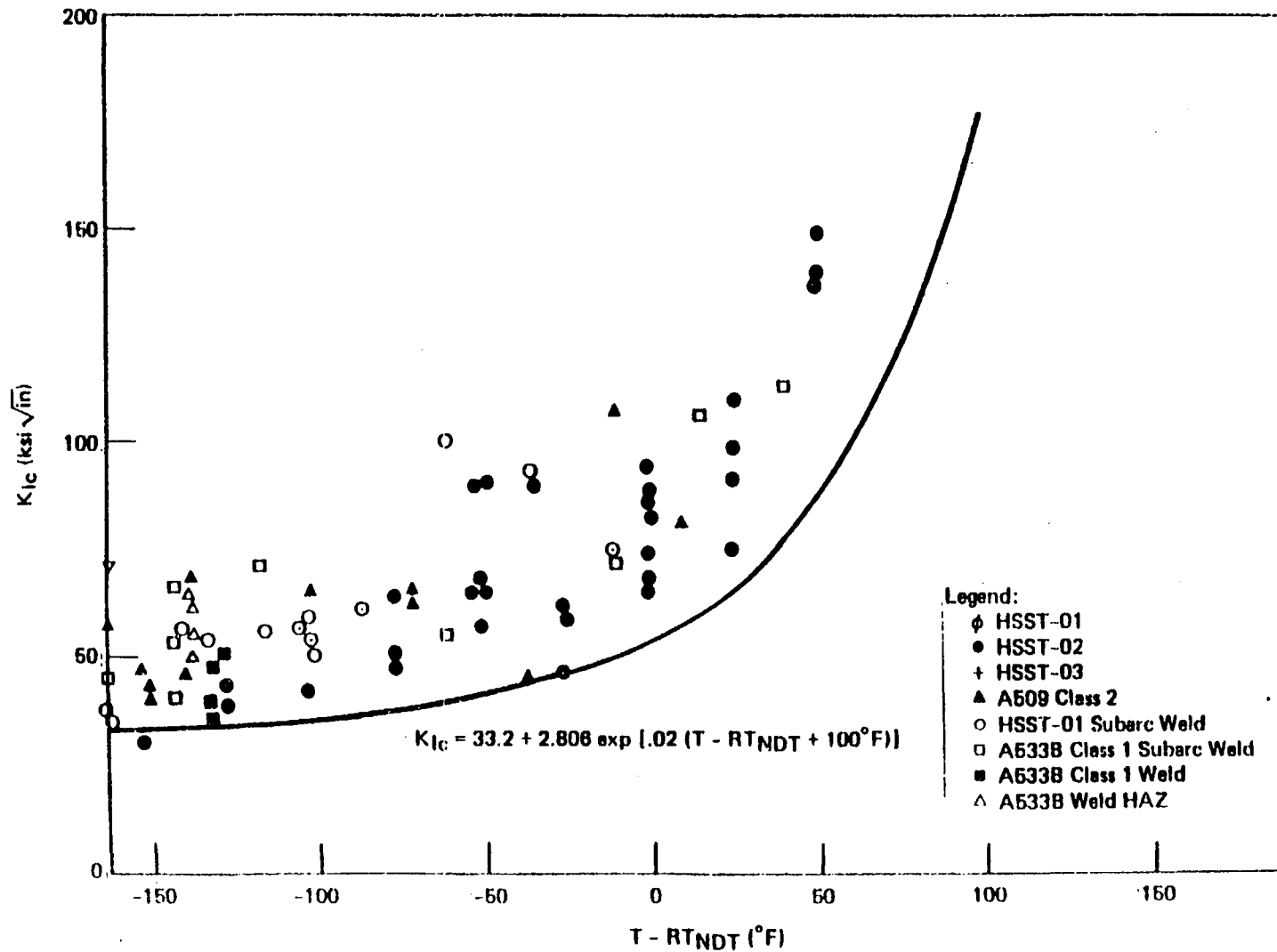
Key Issues for Current Reference Toughness Approach (continued)

- Code curves were based upon a limited amount of data generated beginning in the late 1960s; additional data have not shown Code curves to be non-conservative as compared to original data sets normalized using RT_{NDT}
- Some materials can show a greater shift in static fracture toughness than the Charpy shift would suggest
- Dynamic and crack arrest toughness shifts more closely match Charpy shifts (less data available, however)
- Precracked Charpy impact tests have been shown to be a better normalizing parameter than RT_{NDT}
- Direct measurement of “valid” toughness is better than using Charpy inferred toughness

Original K_{IR} Curve from WRC 175



ASME Code K_{IC} Curve Basis



Master Curve Toughness Approach is Actively Being Codified in U.S.

- Uses Weibull statistics and cleavage weak link concepts to define position of a master curve (better definition for deterministic and probabilistic analyses)
- Based upon elastic-plastic toughness results using small specimens (including precracked Charpy specimens tested in slow three-point bending as fracture mechanics tests)
- Number of specimens needed and test temperature requirements are being established by ASTM and ASME Code bodies (PVRC Task Group and International Programs)
 - ASME Section III -- initial RT_{NDT} , RT_{T_0}
 - ASME Section XI -- adjusted RT_{NDT}
 - ASTM E08 -- Standard for Master Curve and T_0 determination
 - ASTM E10 -- application to surveillance programs

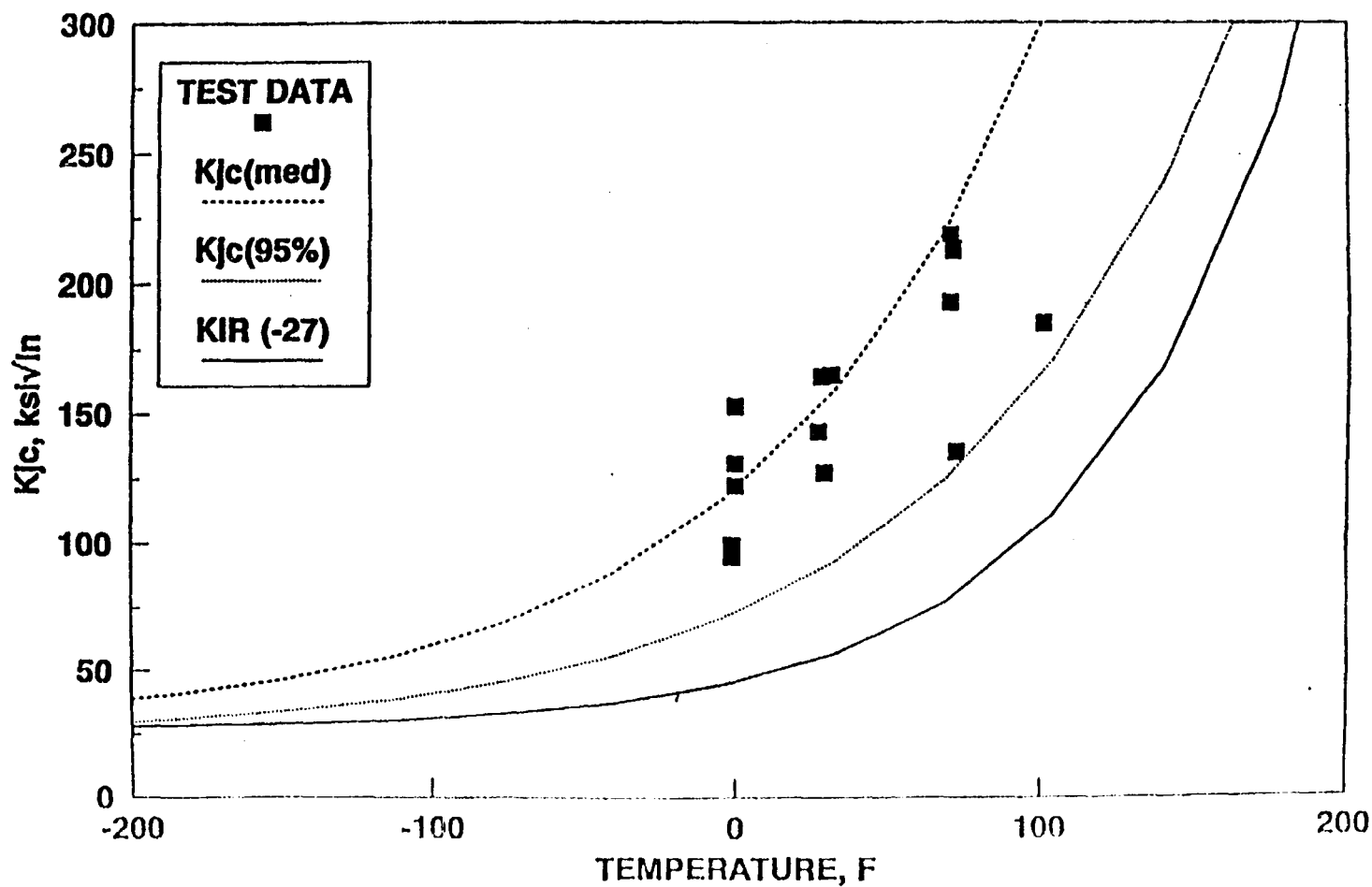
Application of Master Curve in U.S.

- Redefinition of initial RT_{NDT} for one Linde 80 weld metal (WF-70)
- Other weld metals (particularly Linde 1092) and at least one plate material show much better toughness response than RT_{NDT} approach would suggest
- Technical concerns:
 - Constraint and specimen size effects
 - Uncertainty in T_o determination
 - Curve shape: $K_{Jc(med)} = 30 + 70 \exp [0.019 (T - T_o)]$
 - Dynamic and static toughness data
 - Definition of lower bound for deterministic use (lower confidence/tolerance bound levels)

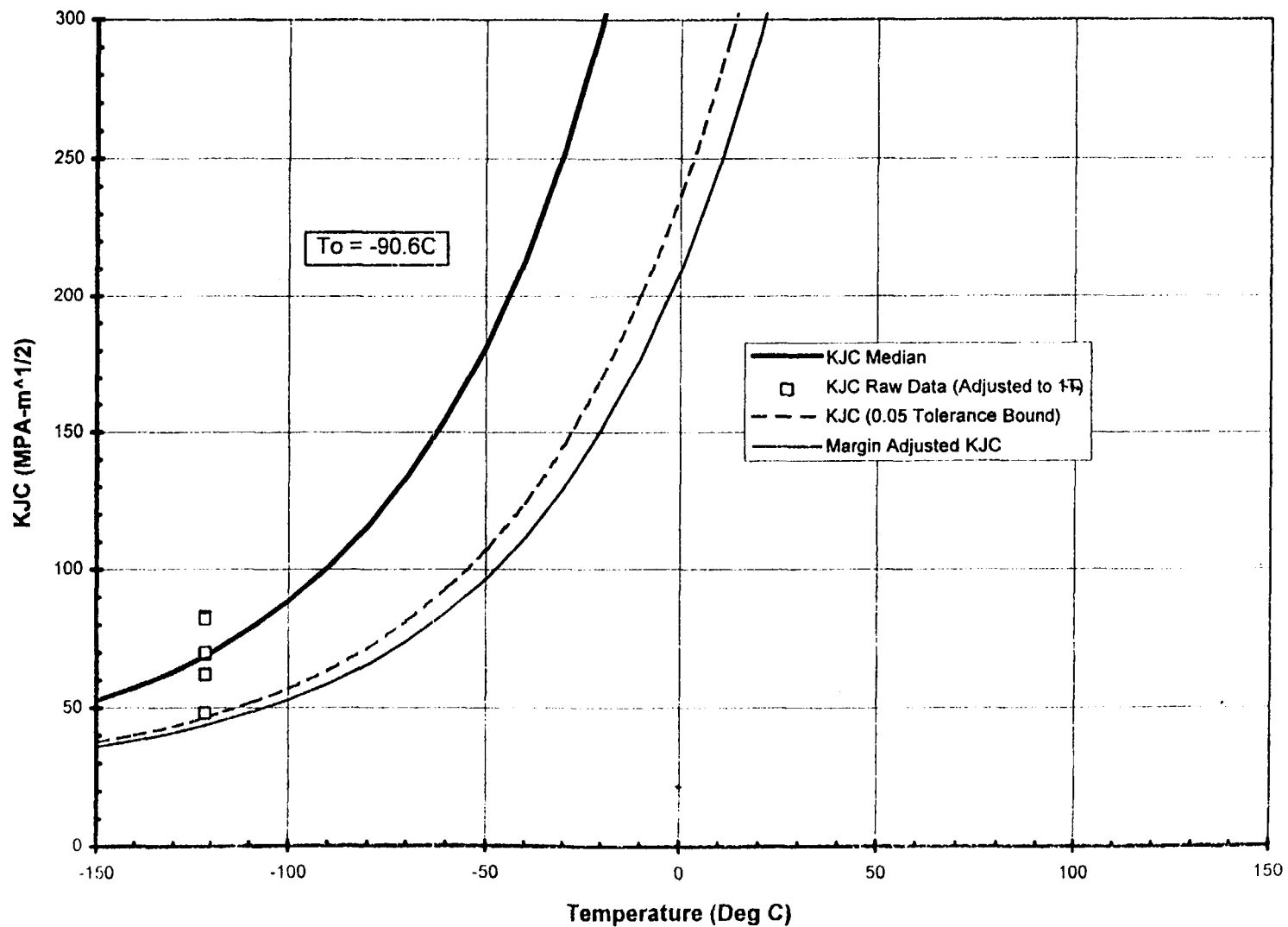
Application for Linde 80 Initial RT_{NDT}

FRACTURE TOUGHNESS - WF-70

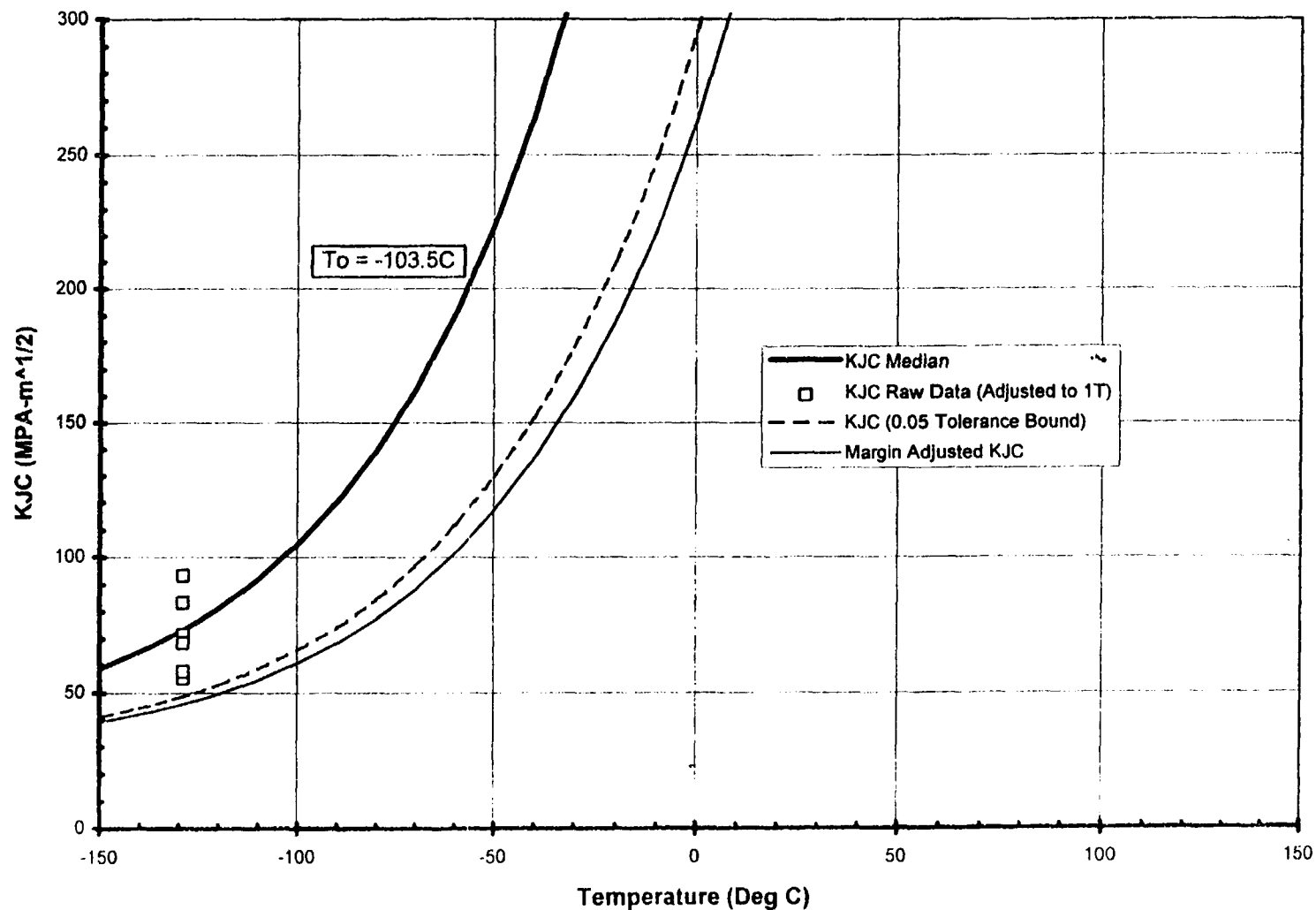
DYNAMIC TOUGHNESS DATA, $T = 0$ F



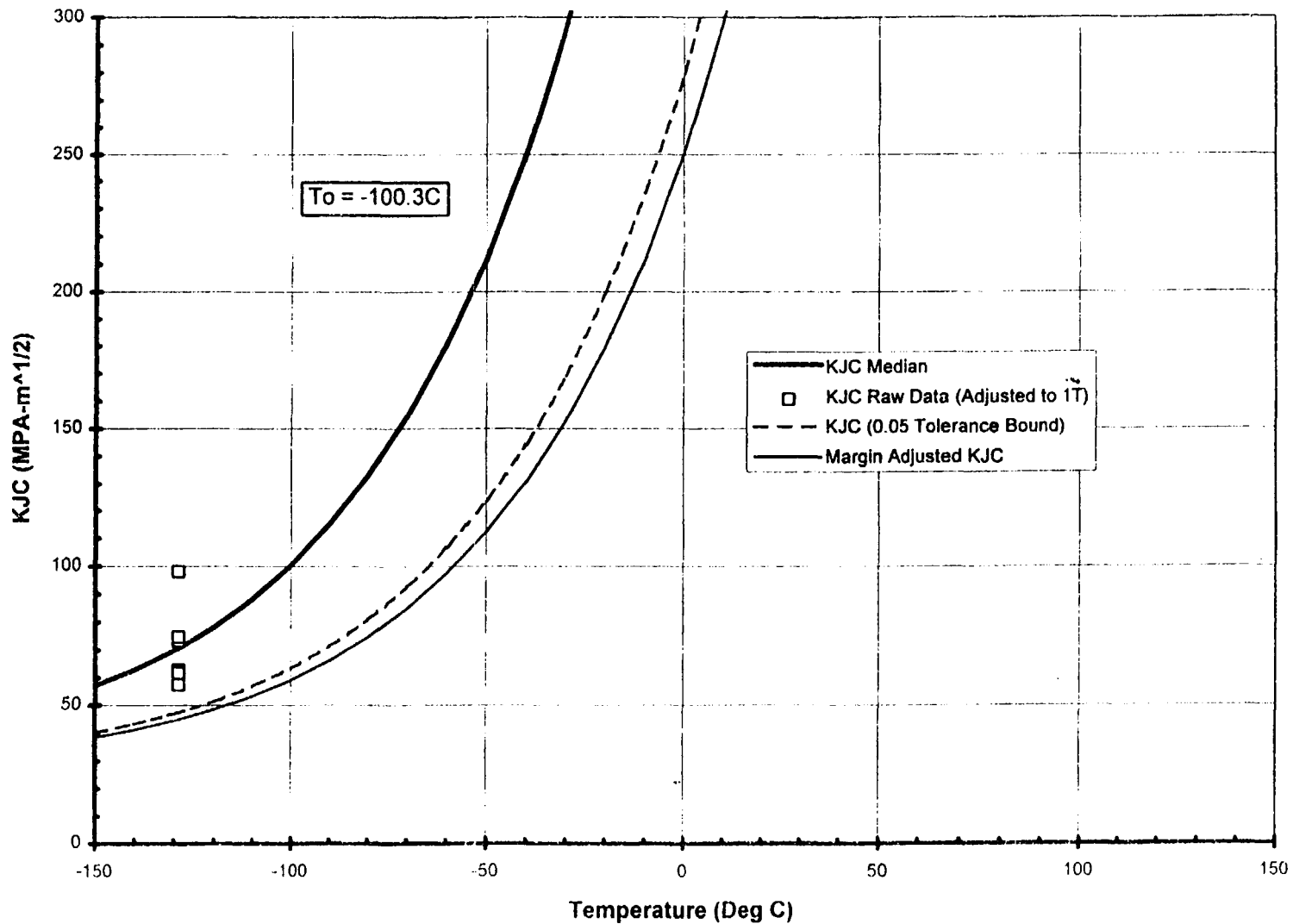
Recent Results for a Linde 1092 Weld Metal (1/2-T CT)



Recent Results for a Linde 1092 Weld Metal (Reconstituted Precracked Charpy)



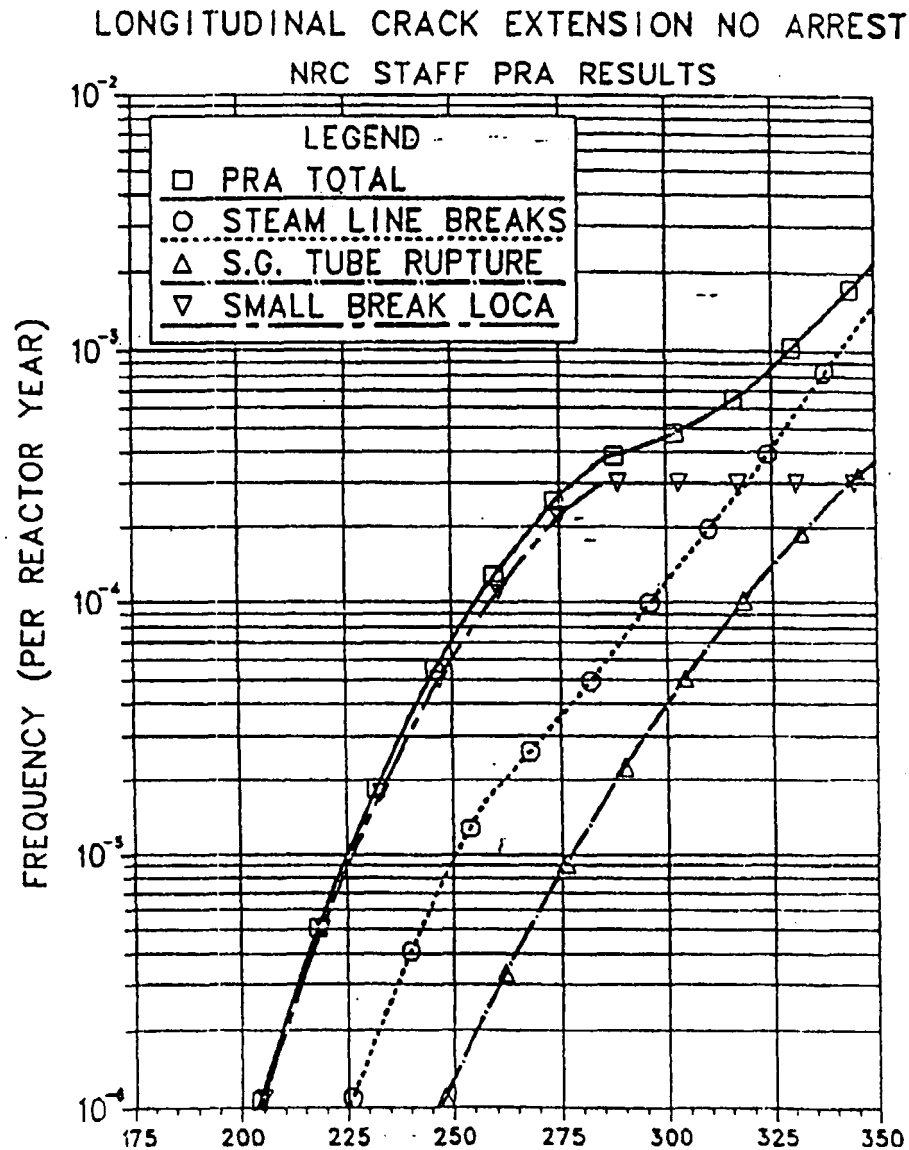
Recent Results for a Linde 1092 Weld Metal (Precracked Charpy)



Master Curve Application to Probabilistic Evaluations

- Effect of a reduced RT_{NDT}
 - Approximately an order of magnitude reduction in conditional failure probability for a reduction of 15°C (27°F)
 - Modified surveillance programs can provide this key information
- Effect of different curve shape and statistics
 - Initial calculations of conditional failure probability suggest similar results
 - Further calculations are needed
- Need to carefully assess technical issues for direct comparison

Original NRC Evaluations for PTS Rule



Conclusions

- Key element for PTS evaluations (either deterministic or probabilistic) is the RPV materials fracture toughness
- Yankee Rowe attempted probabilistic analyses with uncertain toughness properties
- Past studies have relied upon conventional approaches using Charpy V-notch data for plant-specific materials
- Master Curve methodology provides distinct advantages for future analyses if modified surveillance results are utilized
- U.S. Standards and Codes are implementing the T_0 and Master Curve approach

improvement of methods to evaluate brittle failure resistance of the WVER reactor pressure vessels.

A.A.Popov*, M.F.Rogov**, U.G.Dragunov**, E.V.Parshutin*

At the next 10 years a number of Russian WVER nuclear power plants will complete its design lifetime. Normative methods to evaluate brittle failure resistance of the reactor pressure vessels used in Russia have been intended for design stage.

The evaluation of reactor pressure vessel lifetime in operation stage demands to create new methods of calculation and new methods for experimental evaluation of brittle failure resistance degradation.

Figure 1 shows lower part of WVER-440/230 reactor pressure vessel is the oldest type of reactor is operated in Russia.

The main objective of the study in this type of reactor is weldment number 4.

In this report an analysis is made of methods to determine critical temperature of reactor materials including the results of instrumented Charpy testing.

1. Critical temperature

Fig. 2. shows different definition of the transition temperature (from R.Gerard AMES report N4).

Fig. 3. Russian method definition of the transition temperature. At 1989 in Stuttgart at same meeting I told about differentiation the total fracture energy Charpy-V into the components.

Now at Europe is proposed ESIS instrumented Charpy-V standard.

Fig. 4. illustrates differentiation of Charpy-V energy.

Fig. 5. - characteristic behavior of Charpy-V energy as a function of temperature and load deflection curves typical lower shelf, transition and upper shelf behavior.

Fig. 6. shows transition curves constructed on the basis of serial tests of Charpy-V for 15Ch2NMFAA steel. Was test two dif-

* Engineering Center of Nuclear Equipment Strength, Research and Development Institute of Power Engineering

** Experimenter's and Designer's Office "Hydropress"

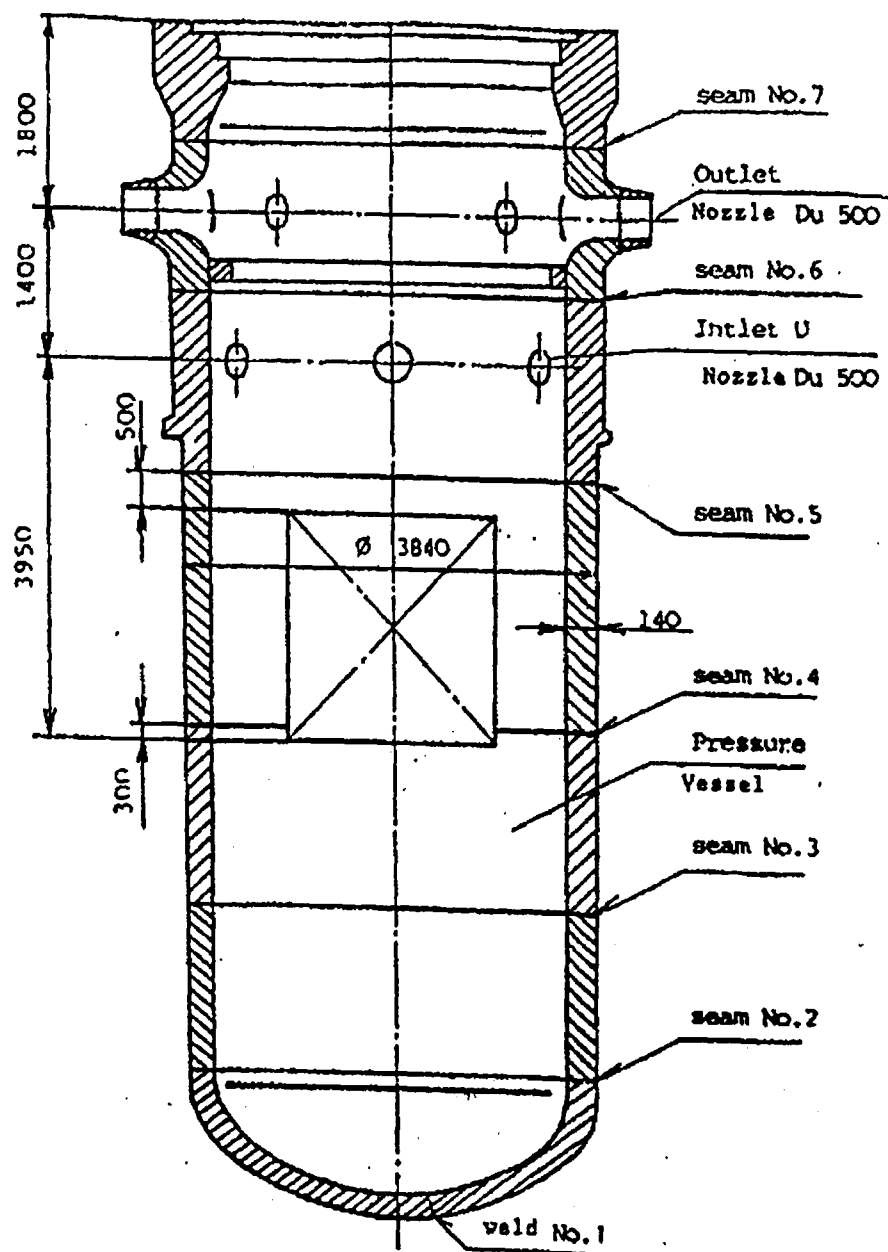


Fig. 1 Lower part of WWER-440/230 reactor pressure vessel.

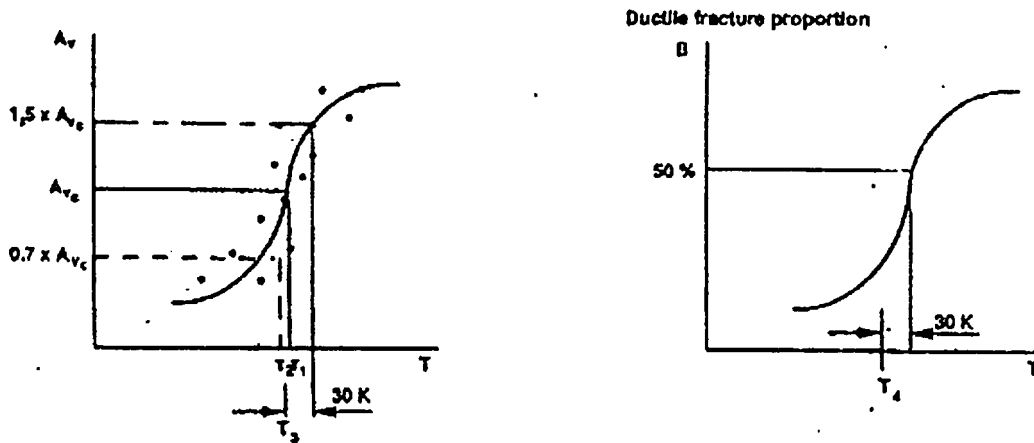
Fig. 2. Definitions of the transition temperature

Country	Transition temperature	Definition	Shift under irradiation												
U.S.	RTndt	ASME III, article NB 23331. RTndt is the minimum of: Tndt measured by drop weight tests (ASTM E208) Tcv-33° : where Tcv is the temperature at which each of three Charpy-V specimens tested at this temperature exhibits at least 0.89 mm lateral expansion and 68J of absorbed energy	Shift of the Charpy V-notch curves, measured at the 41J lev												
Belgium	RTndt	ASME definition	Shift of the Charpy V-notch curves, measured at the 41J lev												
Finland	Tk	Russian definition (in practice: 47 J energy level on the Charpy curve)	Shift of the Charpy V-notch curves, measured at the 47J lev												
France	RTndt	RCC-M code, section III, article MC-1240 The definition of the RTndt is practically identical to the ASME definition	Shift of the Charpy V-notch curves, largest of the shifts measured at the 7daJ/cm^2 on the energy curves, and at the 0.89 mm level on the lateral expansion curves.												
Germany	RTndt	ASME definition	Shift of the Charpy V-notch curves, measured at the 41J lev												
Russian Federation	Tk	PNAE-G-7-002-88 (ref.RU1) Tk is the temperature that fulfills the following conditions: <ul style="list-style-type: none">The mean value of Charpy-V absorbed energy at temp. Tk must not be lower than the values given in the table 2'.At the temp.Tk, none of 3 tested specimens should exhibit an absorbed energy below 70% of the values in the table 2'.The mean value of Charpy-V absorbed energy at temp. Tk+30°K must not be lower than 1.5 times the values given in the table 2'.The ductile fracture proportion of each specimens at temp. Tk+30°K must not be lower than 50%.	Tk is determined directly in irradiated conditions <table><tr><th colspan="2">Table 2'</th></tr><tr><th>Proof stress Rp0.2 (MPa)</th><th>Absorbed Energy (J)</th></tr><tr><td><300</td><td>23</td></tr><tr><td>300-400</td><td>31</td></tr><tr><td>400-550</td><td>39</td></tr><tr><td>550-890</td><td>47</td></tr></table>	Table 2'		Proof stress Rp0.2 (MPa)	Absorbed Energy (J)	<300	23	300-400	31	400-550	39	550-890	47
Table 2'															
Proof stress Rp0.2 (MPa)	Absorbed Energy (J)														
<300	23														
300-400	31														
400-550	39														
550-890	47														
Spain	RTndt	ASME definition	Shift of the Charpy V-notch curves, measured at the 41J lev												
Sweden	-	ASME definition used in practice													
Netherlands	RTndt	ASME definition													
U.K.	-	The fracture toughness curves in irradiated condition must be justified on a case-by-case basis													

The transition temperature T_K is that temperature which fulfills the following conditions :

1. The mean value of notched bar impact strength at a temperature T_K must not be lower than the values shown in the table below.
2. At a temperature T_K , none of the three tested specimens should exhibit notched bar impact strength below 70 % of the value in the table.
3. The mean value of the notched bar impact strength at $T_K + 30$ K must not lie below 1,5 times the value in the table.
4. The ductile fracture proportion of each specimen at $T_K + 30$ K must not be lower than 50 %.

In summary: T_K is the highest of the temperatures T_1, T_2, T_3, T_4 according to the following diagrams

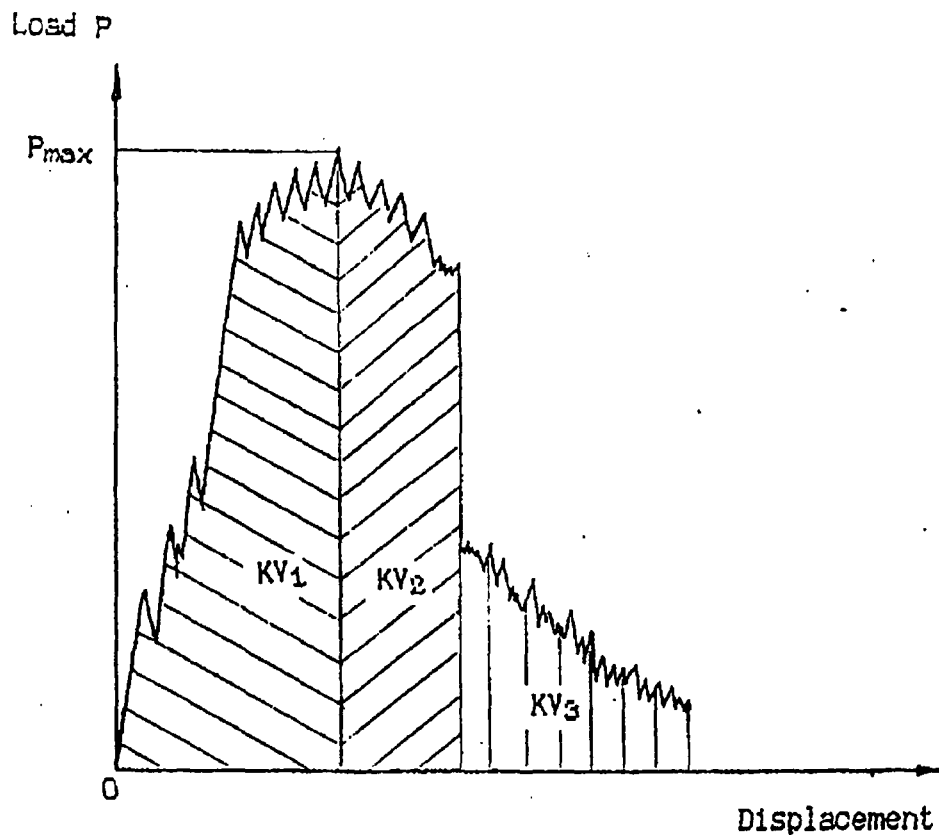


N. B. : In general $T_K = T_1$

Proof stress $R_{p0,2}$ (MPa)	Notched bar impact strength (KCV) _c (J/cm ²)	Absorbed energy A_{v_c} (J)
≤ 300	30	24
300 - 400	40	32
400 - 550	50	40
550 - 700	60	48

Fig.3. Determination of the transition temperature T_K according to the USSR regulations.

Fig. 4. Differentiation of Charpy-V notch energy.



$$KV = KV_1 + KV_2 + KV_3$$

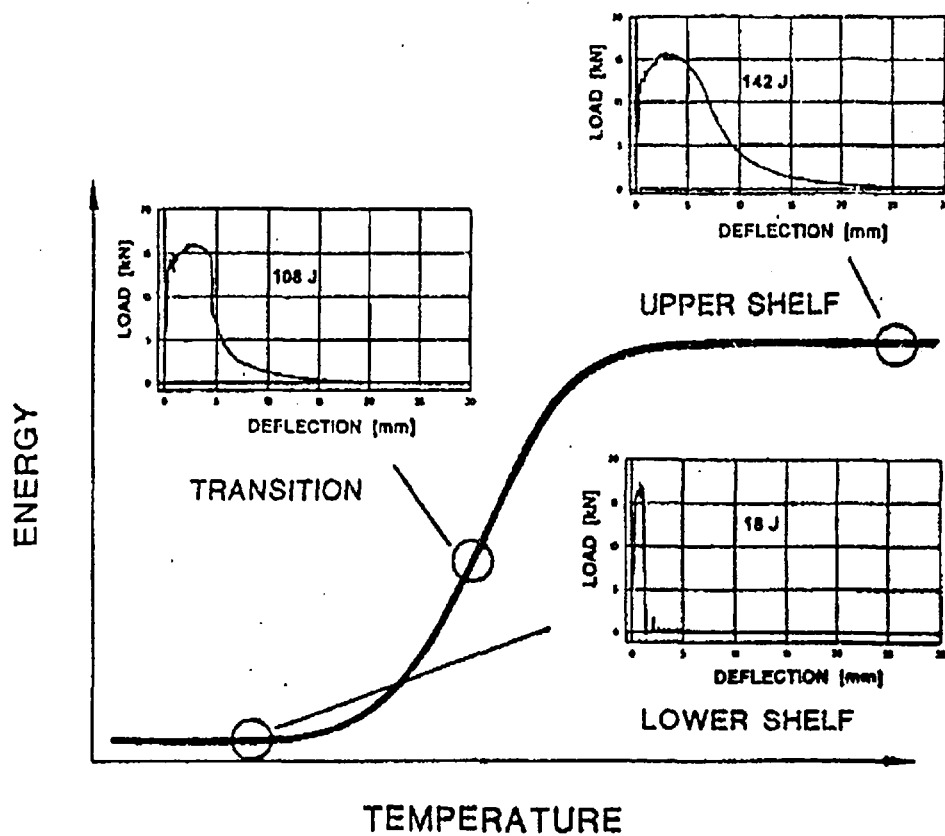
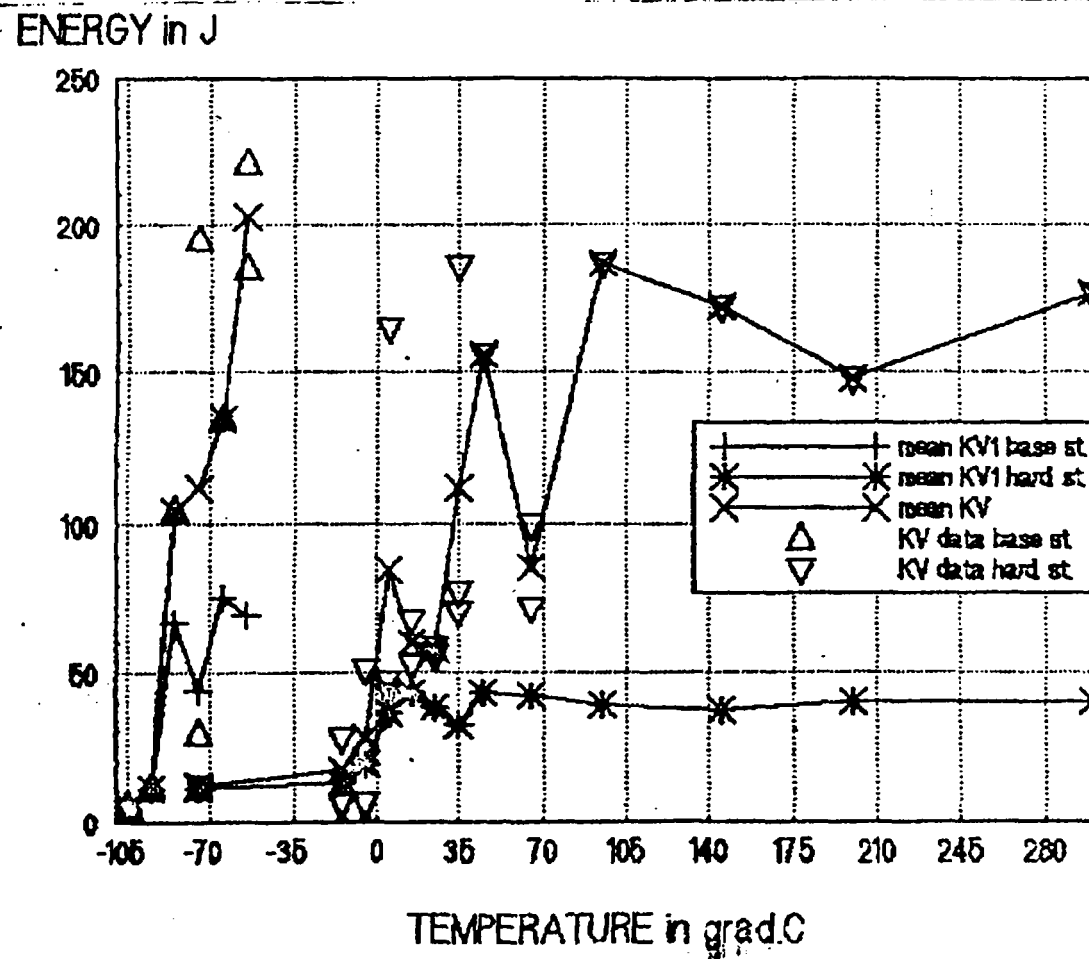


Fig. 5. Characteristic behavior of Charpy-V notch energy as a function of temperature and load deflection curves typical lower shelf, transition and upper shelf behavior.

Fig. 6. Charpy-V notch for 15Ch2NMFAA steel.



ferent states of this steel lower curves is plotted by values of crack initiation impact toughness-KV₁.

It should be noted that KV₁ values are approximately constant beginning from cold brittleness lower shelf independent of a test temperature. The curve bend temperature corresponds to critical temperature of this steel, obtained by the standard method described above. The analysis showed a good agreement of critical temperatures determined by both methods (All are more 30 curves).

2. The second problem Fracture toughnesses.

At this year we in Russian proposed 3 new drafts of standards for determination of fracture behaviour of materials. These standards is conform will Russian standard (GOST 25.506-85), standards of ASTM (E 399, E 1737-96, E 1731-96, E-813), European standard (ESIS P2-92).

Fig. 7. - main validity requirement for determination of plain-strain toughness K_{1C}.

Fig. 8. - method of evaluation a single point fracture toughness value J_C.

Fig. 9. - problem of displacement measurement points for determination of J.

3. The third problem - dimensions of specimen for K_J.

Fig. 10. - different miniature specimens are being evaluated for potential use in determining of fracture toughness.

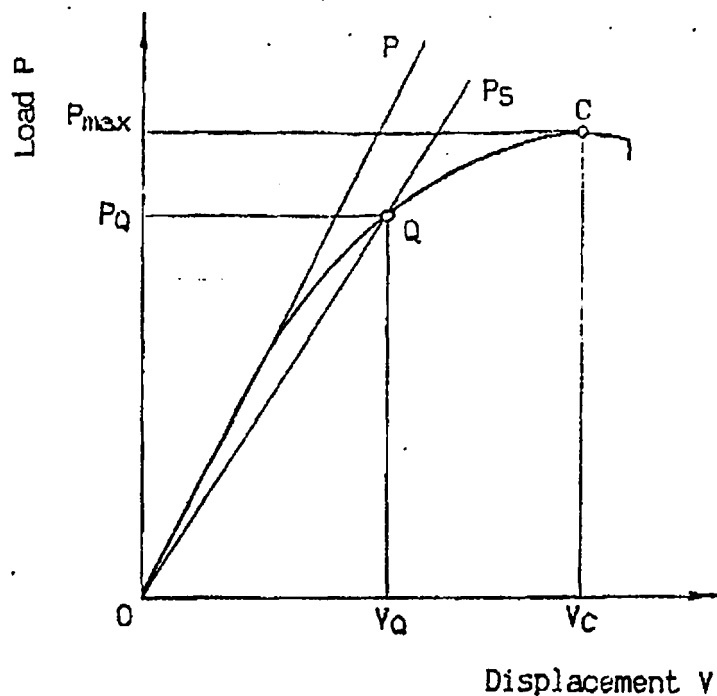
Fig. 11. - areas of corrective determination of fracture toughness. For actual areas of calibration should enough this type of specimens. I taken from report of Mr. Nanstad (ORNL) figures 10 and 11.

4. The fourth problem - postulation of defect.

Fig. 12. - comparison of the regulatory for defining pressure - temperature limits for normal operation (from Gerard). We should like to take for WWR-440/230 this depth of crack - 10 mm.

Finally, I'd like to tell about our project "Methodical development for WWR reactor pressure vessel state and safe operation lifetime evaluation by fracture mechanics criteria". The obtained results and developed approaches methods and techniques

Fig. 7. Test method for plane-strain fracture toughness K_{1C}.



Validity Requirement

GOST 25.508-85

ASTM E 399-90

1. $P_{max}/P_Q \leq 1.1$
and 2(a) or 2(b)

1. $P_{max}/P_Q \leq 1.1$

- 2(a). $B \geq 2.5(K_Q/R_{P0.2})^2$

2. $B \geq 2.5(K_Q/R_{P0.2})^2$
 $a \geq 2.5(K_Q/R_{P0.2})^2$

$$\varphi_c = \frac{B - B_c}{B} \times 100\% \leq 1.5\%$$

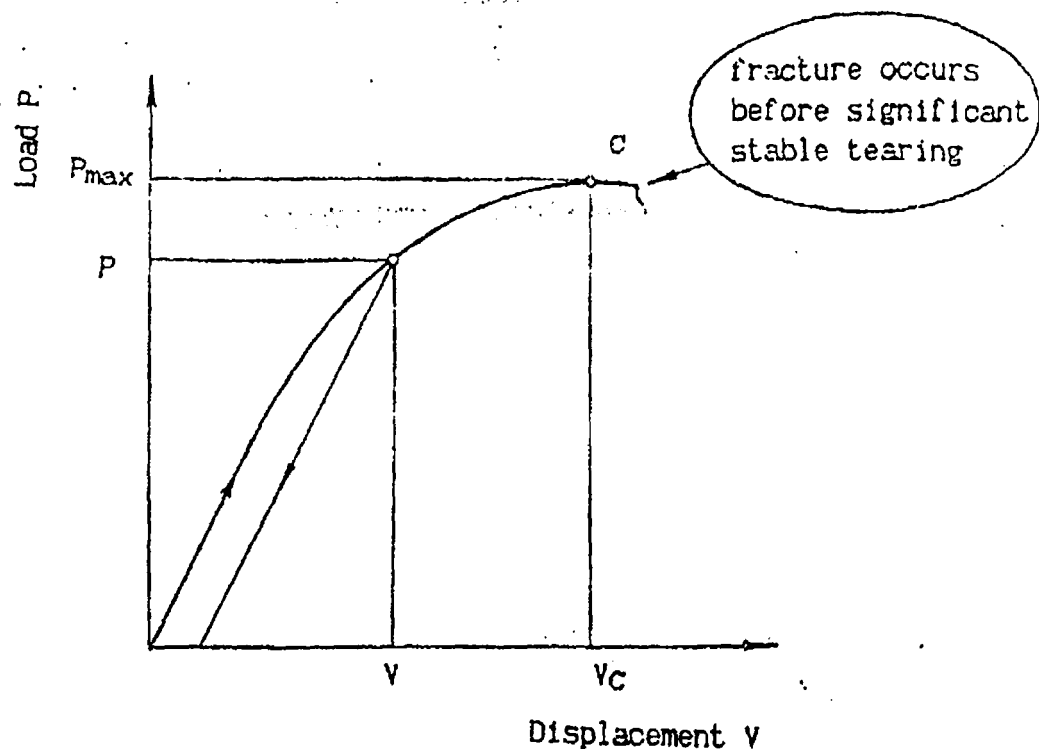
- 2(b). $V_c \leq 1.2V_Q$

$$K_Q = \frac{P_Q}{\sqrt{B B_N W}} f(a/W)$$

(ESISP2-92)

$$B_N = 0.8B$$

Fig. 6. Evaluation of single point fracture toughness value J_C .



$$0,9V_C < V < V_C$$

ASTM E 1737-96

GOST 25.506-85

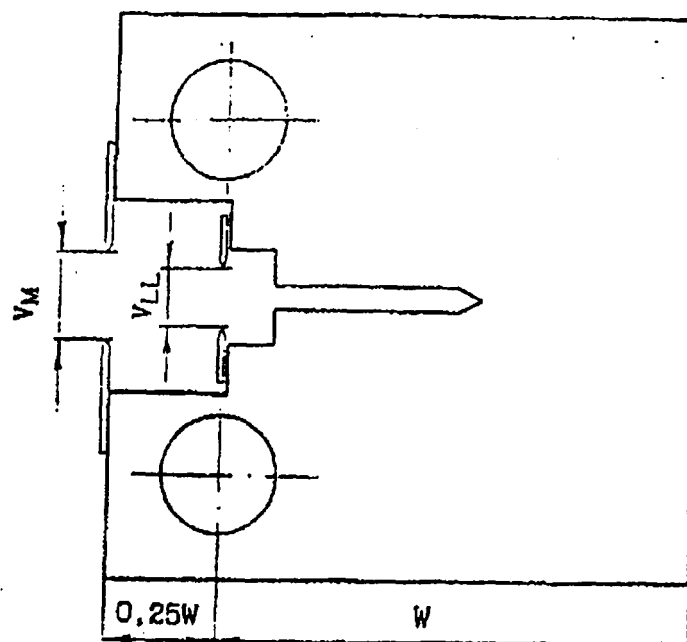
$$\Delta a < 0,2 \text{ mm} + J_{QC}/2R_Y$$

$$\Delta a < 0,3 \text{ mm}$$

$$B, a_0, b_0 > 200J_Q/R_Y$$

$$B > 100J_Q/R_Y$$

Fig. 9. Problem of displacement measurement points.



GOST 25.506-85

$$k = 1 + \frac{0.25W}{a + 0.1(W-a)}$$

$$a/W = C_0 + C_1(U) + C_2(U)^2 + \dots + C_5(U)^5$$

$$U = f(\text{BEV}/P)$$

Meas. Location	C_0	C_1	C_2	C_3	C_4	C_5
V_{LL}	1.0002	-4.0632	11.242	-106.04	454.33	-650.68
V_M	1.0010	-4.6695	18.460	-236.82	1214.90	-2143.60

$$\frac{V_{LL}}{V_M} = 0.7 \pm 0.8 \quad \frac{a}{W} = 0.5 \pm 0.8$$

**AS AN ALTERNATIVE TO THE PRECRACKED CHARPY TEST,
MINIATURE COMPACT SPECIMENS ARE BEING EVALUATED FOR
POTENTIAL USE IN DETERMINING T_{100} AND MASTER CURVE**

- MINIATURE COMPACT SPECIMENS 0.2 in. (5.1 mm) THICK, DESIGNATED 0.2T C(T) UNDER INVESTIGATION - FOR $a/w = 0.5$, LIGAMENT IS SAME AS PCVN
- SPECIMENS FROM HSST PLATE 02 TESTED IN 4TH IRRADIATION SERIES
- SPECIMENS TAKEN FROM BROKEN HALVES OF UNIRRADIATED 1T C(T) SPECIMENS IN T-L ORIENTATION
- SPECIMENS FATIGUE PRECRACKED, THEN SIDE-GROOVED 10% OF THICKNESS ON EACH SIDE WITH 0.005 in. (0.13 mm)

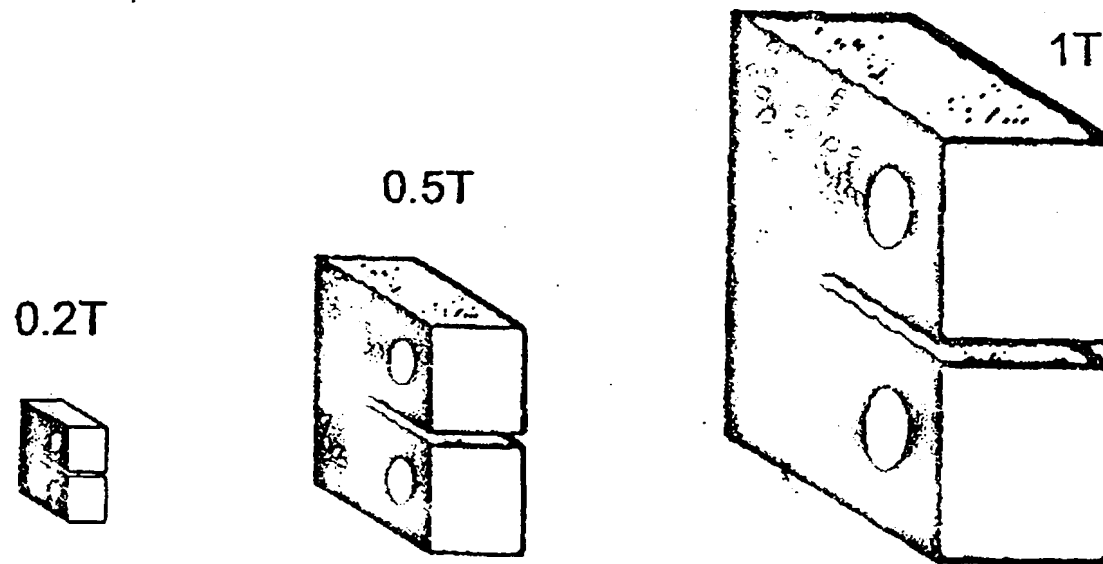


Fig. 10.

SIZE-ADJUSTED K_{Jc} RESULTS OF HSST PLATE 02 FROM
0.2T COMPACT SPECIMENS ARE LOWER THAN THOSE FROM
PCVN SPECIMENS AT -50°C BUT COMPARE WELL AT 0°C

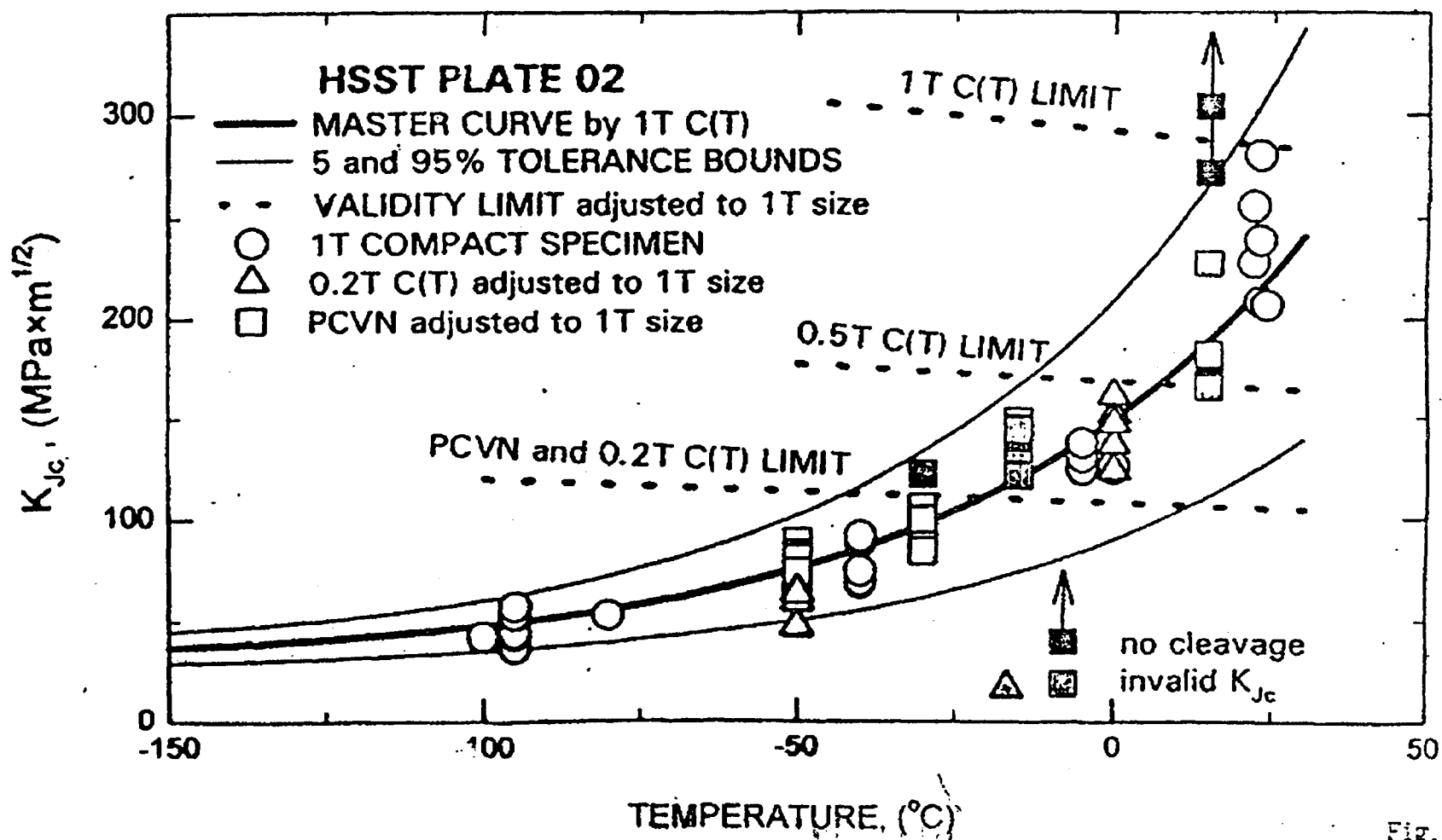


Fig. 11.

Fig. 12. Comparison of the regulatory approaches for defining pressure-temperature limits for normal operation (heat-up and cooldown).

Country	Methodology	Reference flaw			Safety factors			Fracture toughness curve
		Depth "a"	Length "2c"	aspect ratio a/c	On K1 due to primary (pressure) stresses	On K1 due to secondary (thermal) stresses	On fracture toughness	
U.S.	ASME III App.G	1/4 T	1.5 T	1/3	2	1	1	K1a ASME XI app.G G-2110
Belgium	ASME III App.G	1/4 T	1.5 T	1/3	2	1	1	K1a (ASME XI)
Finland	No specific rule	15 mm	50 mm	3/5	2	1.2	1	K1c see § 3.1.1.c
Note: effect of cladding included								
France	RCC-M code Chapter B.3260	1/4 T	1.5 T	1/3	2	1	1	K1a
	First method	15 mm	90 mm	1/3	1	1	T-RTndt < 50°C: 2.50	K1c
	Second method	or					T-RTndt > 50°C: 1.43	K1a
		case-by-case (to be justif.)	case-by-case (to be justif.)	case-by-case (to be justif.)			1.43	K1a K1c K1a K1c
Germany	KTA 3201-2, § 7.9	Modified Porse diagram, base on RTndt and tensile properties only (see § 3.3.1.e)						
	First method							
	Second method	1/4 T	1.5 T	1/3	2	1	1	K1R (KTA 3201-2) see § 3.1.1.e
Russian Federation	PNAE-G-7-002-88	1/4 T	0.75 T	2/3	1	1	1	Specific K1c curve for normal operation (*)
Spain	ASME III App.G	1/4 T	1.5 T	1/3	2	1	1	K1a (ASME XI)
Sweden	No specific rule	To be justified by the licensee						
Netherlands	ASME III App.G	1/4 T	1.5 T	1/3	2	1	1	K1a
U.K.	No specific rule	To be justified by the licensee						

(*): The Russian K1c curves for normal operation include safety factor (The K1c curve is the envelope of two curves, obtained by dividing K1 by 2 for a given temperature, or shifting the initial curve 30°K to the right)

can be applied also by Western companies for evaluating safe operation and lifetime of their own PWR-type reactor pressure vessels including those after the thermal annealing.

The qualified russian scientists and engineers possessing extensive knowledge and practice experience in such areas as equipment development for special purpose nuclear power transportation facilities; design, production and operation of WWER reactor pressure vessels; development and production of nuclear ammunition will take part in this work.

Project has number -797 and is now in International science and technology center. We would like to hope that partners from other countries will take part in this project.

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Session 4

6 MAY, TUESDAY

WWER reactor analysis 1

Petkov, G. Groudev, P. Argilov, J.	Bulgaria	A Procedure for Temperature-Stress Fields Calculation of WWER-1000 Primary Circuit in PTS Event
Perneczky, L.	Hungary	Handling Large Primary to Secondary Leakage Opening of the Steam Generator Collector Cover
Tuomisto, H.	Finland	Overcooling Transient Selection and Thermal Hydraulic Analysis of Loviisa PTS Assessments



A PROCEDURE FOR TEMPERATURE-STRESS FIELDS CALCULATION OF WWER-1000 PRIMARY CIRCUIT IN PTS EVENT

by

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ABSTRACT

The paper presents the procedure of an investigation of WWER-1000 primary circuit temperature-stress fields by the use of thermohydraulic computation data for a pressurized thermal shock event "Core overcooling".

The procedure is based on a model of the plane stress state with ideal contact between wall and medium for the calculation.

The computation data are calculated on the base of WWER-1000 thermohydraulic model by the RELAP5/MOD3 codes. This model was developed jointly by the Bulgarian and BNL/USA staff to provide an analytical tool for performing safety analysis.

As a result of calculations by codes the computation data for temperature field law (linear laws of a few distinguished parts) and pressure of coolant at points on inner surface of WWER-1000 primary circuit equipment are received.

Such calculations can be used as a base for determination of all-important load-carrying sections of the primary circuit pipes and vessels, which need further consideration.

KEYWORDS

Pressurized thermal shock events, WWER-1000 primary circuit, 'core overcooling' transient, temperature-stress fields, RELAP5/MOD3 thermohydraulic model, plane stress state model.

1. INTRODUCTION

The analysis of the real temperature-stress fields of the primary circuit equipment is a necessary condition for the correct assessment of the pressurized thermal shock (PTS) events. Here is presented the procedure of an investigation of WWER-1000 primary circuit temperature-stress fields by the use of thermohydraulic computation data for a pressurized thermal shock event "Core overcooling" (T14 from PSA level 1 of NPP Kozloduy [1]). The computation data are calculated on the base of WWER-1000 thermohydraulic model by the RELAP5/MOD3 codes [2]. This model was developed jointly by the Bulgarian and BNL/USA staff to provide an analytical tool for performing safety analysis [3]. The following initiating events can relate to it:

- steam line rupture of steam generator in the containment;

- opening and stuck open of one steam dump system to the atmosphere (BRU-A);
- opening and stuck open of one steam generator relief valve.

The frequency of this indicator for 5 & 6 units of the NPP Kozloduy PSA at the last revision is 1.0×10^{-3} 1/y and its core melt frequency is 6.0×10^{-8} 1/y (previous revision was accordingly 5.0×10^{-4} 1/y è 3.4×10^{-6} 1/y) [1]. All LOCA initiators have frequencies more than T14. Consequently the PSA study supposes that this event can not lead to a primary circuit rupture. This is an optimistic assumption demanding detailed study of this initiator (one of the most dangerous PTS events).

2. THE THERMOHYDRAULIC MODEL

In the RELAP5/MOD3 model, the primary system has been modelled using two coolant loops with one loop representing three reactor loops and the second loop represented the reactor loop with the stuck opened BRU-A. In the RELAP5 model for WWER-1000/V320 NPP are included (see Fig.1): reactor vessel; core region represented by three channels; pressurizer system including heaters, spray and relief valves; safety system including high pressure pumps (six pumps TQ_3 and TQ_4), four accumulators and low pressure injection pumps (TQ_2). In the model also is presented a make up/drain system including connection (control) with pressurizer.

Secondary side was developed too and is presented by eight Safety valves, four BRU-A valves, four BRU-K valves, steam pipe lines (including main steam header) and turbine including regulating valve in front of the turbine. In the RELAP5 WWER-1000 model the horizontal steam generator (SG) has been developed (see Fig.2). The transient response of horizontal SG can be very different than that of Western type vertical SG due to the larger water mass in horizontal SG. The model of natural circulation in horizontal SG has been presented in this RELAP5 WWER-1000 model. A separator model has been included in the SG model. The perforated sheet has been modelled in SG model, too.

Main cooling pump (MCP) has been developed using homologous curves of real pumps. The core power was calculated using the reactor kinetics model and boron model has been implemented by specifying an initial boron concentration. The reactor core reactivity feedback was estimated, as close to Kozloduy Unit 6, as possible.

3. TRANSIENT EVENT DESCRIPTION

3.1. Opening and Stuck Open of One Steam Dump to Atmosphere Conditions (Transient Calculations of the Thermal - Hydraulic Analyses for the WWER-1000 Model for Kozloduy NPP)

The transient is initiated by a stuck open pressure relief valve, called the BRU-A, on one of the four steam generators of Unit 6 at the Kozloduy Nuclear Power Plant (KNPP).

The purposes of the steam dump control system are:

- a) to permit the plant to accept sudden losses of load without tripping the reactor;
- b) to remove core stored energy and residual heat following a reactor trip and bring the plant to equilibrium no-load conditions without actuation of the steam generator safety valves;
- c) to control the steam generator pressure at no-load conditions, allowing a transition to manual control.

The calculational sequence of events was predicted by RELAP5 Mod3.1.1. This event is analyzed in the beginning of cycle, full power, 4-loop operation.

The actual 4-loop system is modeled by two loops, one of which is the single loop with the pressurizer and the other one lumped loop represent three loops. The BRU-A valve was assumed to stick open at 10 s. The steam generator blows down and results in the reduction of steam generator pressure in the one with the open BRU-A valve. The system automatically reduces the power to 85%. The steam generator is initially isolated from the main steam header by a check valve and , after 114 s , the BZOK (quick-acting isolation valve) closes. The feedwater is also isolated because the steam generator protection pressure of 4,9 MPa is reached and the saturation temperature difference between the primary and secondary sides became greater than 75°C. The operator action used in the scenario, too and he trips the pump in the loop with the stuck open valve to reduce the heat transferred from that steam generator. The other three main circulation pumps continue to operate. After the scram, the makeup and high pressure injection system (after depressurization to 11,0 MPa) is used to relief the pressurizer up to 11m. After the safety margin (i.e. 55°C between the saturation temperature and the hot leg temperature) is established, the operator begins to cool down the reactor system at a rate of 30°C/hr.

3.2. Results

The most important parameters behavior is showing below (see Table 1). The calculation was performed up to 2000s into the transient time.

4. A PROCEDURE FOR TEMPERATURE-STRESS FIELDS CALCULATION

For calculation of the primary circuit equipment temperature we use a procedure based on a model of the plane stress state with ideal contact between wall and medium for the calculation [7].

The thickness of the primary circuit equipment walls is less than the radius of the boundary which allows us to consider the changes of the temperature-stress fields as plane (the closer is the relation between the thickness of the vessel and the diameter to 1, the bigger is the error).

When the thermal shock is characterised by transient temperature field we can use the following differential equation of thermal conductivity:

$$a \partial^2 T / \partial X^2 = \partial T / \partial \tau \quad (1)$$

where *a* - thermal diffusivity, m²/s;
 T - temperature, °C;
 X - linear (heat streamwise) coordinate, m;
 τ - time, s.

Table 1.

Event: Opening and Stuck Open of One Steam Dump to Atmosphere	Time, s
1. Start calculation.	0.0
2. Opening of one BRU-A Valve.	10.0
3. Unloading of the unit up to 85% power rate automatically.	30.0
4. Switch on automatically of Pressurizer heaters group #2, #3 and #4.	38.0
5. Switch off of MCP with open BRU-A conditions by operator.	60.0
6. Reactor power became 2550 MW.	74.0
7. Isolation of the SG #4 from MSH by closing CHV automatically.	76.6
8. Switch off automatically of Pressurizer heaters #3 and #4.	88.2
9. Isolation of the SG with opened steam dump to atmosphere conditions from the feedwater according SG pressure protection at $P_{SG} = 50 \text{ kg/cm}^2$ and $T_{S(I)} - T_{S(II)} > 75^\circ\text{C}$.	92.0
10. Switch off automatically of Pressurizer heater #2.	96.2
11. Switch off automatically of Pressurizer heater #1.	103.8
12. Closed automatically by set point BZOK between SG #4 and Switch on automatically of Pressurizer heater #1.	125.0
13. Switch on automatically of all Pressurizer heaters group #2, #3 and #4 (Pressurizer heaters will continue to work to the end of calculation).	139.5
14. Reactor SCRAM actuation by operator.	140.0
15. Make up pumps (TK) increasing automatically flow rate reaching max. flow rate of $80 \text{ m}^3/\text{h}$ (Intentionally set less than the design flow of $120 \text{ m}^3/\text{h}$) With an assumed density of 997 kg/m^3 , the make up system delivers 22.1 kg/s . Injected water from the makeup system is heated to a fluid density of 797 kg/m^3 by the makeup drain line heaters.	145.0
16. STV (MIV) tripped and isolated Turbine (by the operator).	155.0
17. Start to inject HPIS (TQ14, 24 & 34 DO1) to primary loop by the operator (with flow rate $6.3 \text{ m}^3/\text{h}$).	180.0
18. Open valve #382 and start to injected from cold leg to Pressurizer to decreasing the pressure of the primary circuit up to 100 kgf/cm^2 .	190.0
19. BRU-K (all four) began to open by reaching set point 68 kgf/cm^2 and to keep the pressure about 64 kgf/cm^2 .	209.4
20. Maximum pressure in MSH is reached - 6.67 MPa (68 kgf/cm^2).	210.0
21. BRU-K (all four) closed automatically by reaching set point 63 kgf/cm^2	218.4
22. Minimal water level in pressurizer is reached - 5.45 m .	290.0
23. Start to inject HPIS (TQ13, 23 & 33 DO1) to primary loop. Flow rate of this pumps is function of pressure in primary loop up to reaching 10.0 m water level in the pressurizer and after that (from 472.0 s) operator decrease flow rate to maintaining 11.0 m water level in pressurizer. Start of the cooling down of the primary circuit by TQ pumps. Blow down line stay open ($30 \text{ m}^3/\text{hr}$).	298.0
24. Interruption of the primary circuit injection from TK pumps by operator.	330.0
25. Reducing the flow rate throw valve #382 to maintaining the pressure of the primary circuit equal to 100 kgf/cm^2 until reaching of the safety margin to the boiling temperature of the hot loop 55°C	386.0
26. Operator decrease flow rate of pump HPIS (TQ13, 23 & 33 DO1) to maintaining 11.0 m water level in pressurizer after reaching 10.0 m water level. Continue of the cooling down of the primary circuit by TQ pumps.	472.0
27. After reaching the safety margin to the boiling temperature of the hot loop 55°C close valve #382 and open valve #385 who will dipressurise primary loop up to 70 kgf/cm^2 keeping safety margin 55°C subcooling in hot loops. (Valve #385 is the same valve #382 with another logic - line from cold leg to Pressurizer).	523.0
28. Water level in pressurizer reach 11.0 m .	540.0
29. Start of the cooling down of the primary circuit by steam dump to turbine condenser with speed of $30^\circ\text{C}/\text{hour}$ after pressurizer water level became 11.0 m and maintaining the safety margin to the boiling temperature of the hot loop 55°C .	540.0
30. Flow rate of the opened and stuck opened BRU-A became 0 kg/s .	810.0
31 Dry out of the SG #4 (collapsed water level less than 1 cm . and a void became 0.99 in volume #400 and in another volumes void is equal to 1.0).	1600.0
32. End of calculation.	2000.0

The solution of the equation (1) depends on the initial and boundary conditions. In our case the equation can be solved within the accuracy of such an analysis, if we make the simplest assumptions that: 1) the wall temperature in the initial time moment is a constant quantity and it equals the coolant temperature; 2) there is no conduction of heat on the outside. The initial and boundary conditions are the following:

$$T|_{\tau=0} = \theta_0 = \text{const} \quad (2)$$

$$\partial T / \partial X|_{X=0} = 0 \quad (3)$$

$$\lambda \partial T / \partial X|_{X=\delta} = a [T|_{X=\delta} - \theta(\tau)] \quad (4)$$

where λ - thermal conductivity, W/(m.s.°C);
 $\theta(\tau)$ - coolant temperature in the moment, τ , °C;
 θ_0 - coolant temperature in the initial time, τ_0 , °C;
 δ - thickness of the wall, m.

The change of the vessel wall temperature in dependence of time is described by the following equation:

$$T(X, \tau) = \theta(\tau) + \sum_{n=1}^{\infty} A_n(\tau) \cos(\varepsilon_n X / \delta), \quad (5)$$

where

$$A_n(\tau) = B_n \exp(-\alpha \tau \varepsilon_n^2 / \delta) \int_0^{\tau} (d\theta/d\tau) B_n \exp(\alpha \tau \varepsilon_n^2 / \delta) d\tau, \quad (6)$$

$$B_n = [4 \sin(\varepsilon_n)] / [(2\varepsilon_n + \sin(2\varepsilon_n))], \quad (7)$$

The values of ε_n are found by the transcendental equation $\text{tg}(\varepsilon_n) = B_i / \varepsilon_n$. For $B_i \geq 50$ it can be assumed that $\varepsilon_n = [(2n-1)\pi]/2$, where $n=1, 2$. The temperature-stresses on the outside surface of the k -layer can be found by the equation:

$$\sigma_{Rk}(\tau) = \alpha_1 E / \{(1-\mu)[\underline{t}_k(\tau) - t(x, \tau)], \quad (8)$$

where R_k and r_k are the coordinates of the inner and outer wall and

$$\underline{t}_k(\tau) = [1/(R_k - r_k)] \int_{r_k}^{R_k} t(x, \tau) dx, \quad (9)$$

is the average temperature of the k -layer.

If we assume that the coolant temperature changes by the linear dependence

$$\theta_{\tau} = \theta_0 - p\tau, \quad (10)$$

then the equations (5) and (6) can be substitute by the following ones:

$$T(X, \tau) = \theta_0 - p\tau + \sum_{n=1}^{\infty} A_n(\tau) \cos[(\varepsilon_n X) / \delta], \quad (5')$$

$$A_n(\tau) = -B_n \exp(-\alpha \tau \varepsilon_n^2 / \delta) \cdot p \cdot \int_0^{\tau} B_n \exp[(\alpha \tau \varepsilon_n^2) / \delta] d\tau, \quad (6').$$

The equations (5÷7,10), (5') and (6') the following quantities are used:

θ_0 - temperature in the initial time, °C;

p - parameter, which determines the temperature gradient of the coolant, s^{-1} ;

δ - total thickness vessel wall (including thermal shield), m.

They are received by a diagram of temperature changes during the PTS transient, received by WWER-1000 RELAP5/Mod3 model. The diagram includes different sectors (for the time intervals $[\tau_1^j, \tau_2^j]$), for which we calculate the parameters of the linear dependence of the temperature change. An example is shown on Table 2.

The Reactor Pressure Vessel (RPV) temperature-stresses are received by the formulae:

For $\tau_0 \geq \tau$

$$\sigma_{Rk}(\tau) = A(p/b) \sum_{n=1}^{\infty} \{ (b_n/\varepsilon_n^2) / [\varepsilon_n(R_k - r_k)/\delta] \} \cdot \{ [\sin[(\varepsilon_n R_k)/\delta] - [\varepsilon_n(R_k - r_k)/\delta] \cdot \sin[(\varepsilon_n r_k)/\delta] \} \cdot [1 - \exp(-b\tau\varepsilon_n^2)] \quad (11),$$

and for $\tau_0 \leq \tau$

$$\sigma_R(\tau) = A(p/b) \sum_{n=1}^{\infty} \{ (b_n/\varepsilon_n^2) / [\varepsilon_n(R_k - r_k)/\delta] \} \cdot \{ [\sin[(\varepsilon_n R_k)/\delta] - [\varepsilon_n(R_k - r_k)/\delta] \cdot \sin[(\varepsilon_n r_k)/\delta] \} \cdot [\exp(b\tau_0\varepsilon_n^2) - 1] \cdot \exp(-b\tau\varepsilon_n^2) \quad (12)$$

Table 2.

No.	θ_τ^j - temperature of the j -sector	τ_1^j, s	τ_2^j, s	$\theta_0^j, ^\circ K$	$\theta_0^{j+1}, ^\circ K$
1.	$\theta_\tau^1 = 584 - \tau \cdot 1,357; p_1 = 1,357$	104	146	584	527
2.	$\theta_\tau^2 = 527 + \tau \cdot 4,7; p_2 = 4,7$	146	156	527	574
3.	$\theta_\tau^3 = 574 - \tau \cdot 1,205; p_3 = 1,205$	156	198	574	521
4.	$\theta_\tau^4 = 521 + \tau \cdot 0,8; p_4 = 0,8$	198	208	521	529
5.	$\theta_\tau^5 = 529 - \tau \cdot 0,083; p_5 = 0,083$	208	292	529	522
6.	$\theta_\tau^6 = 522 - \tau \cdot 0,25; p_6 = 0,25$	292	344	522	509
7.	$\theta_\tau^7 = 509 + \tau \cdot 0,238; p_7 = 0,238$	344	365	509	514
8.	$\theta_\tau^8 = 514 - \tau \cdot 0,051; p_8 = 0,051$	365	542	514	500
9.	$\theta_\tau^9 = 500 + \tau \cdot 0,111; p_9 = 0,111$	542	677	500	515
10.	$\theta_\tau^{10} = 515 - \tau \cdot 0,161; p_{10} = 0,161$	677	708	515	510
11.	$\theta_\tau^{11} = 510 + \tau \cdot 0,381; p_{11} = 0,381$	708	750	510	526
12.	$\theta_\tau^{12} = 526 - \tau \cdot 0,011; p_{12} = 0,011$	750	1500	526	518

The undefined quantities in this formulae are the following:

- $\dot{A} = \alpha E / (1 - \mu)$;
- a - thermal diffusivity coefficient, ($5,2 \cdot 10^{-6} m^2/s$);
- $\alpha = 17,5 \cdot 10^{-6} 1/^\circ K$, α - linear thermal expansion coefficient;
- $b = \alpha / \delta^2$;
- $B_n = \alpha \delta / \lambda$;
- \dot{A} - module of elasticity ($0,177 \cdot 10^6 MPa$);
- λ - thermal conductivity coefficient [$0,0057 W/(m \cdot s \cdot ^\circ C)$];
- μ - Poisson coefficient (0,3).

R_k and r_k are measured from the outside surface: R_k - coordinate on the outside surface of the k -layer; r_k - coordinate on the inside surface of the k -layer.

5. CONCLUSIONS

The assumption that the wall width of primary circuit equipment is less than its radius leads to overrating of temperature stresses.

However, such calculations can be used as a base for determination of all-important load-carrying sections of the primary circuit pipes and vessels, which need further consideration.

Primary circuit equipment is a potential object for pressurized thermal shock phenomena in different transient events of nuclear power plants operation. On the basis of temperature-stress fields calculation we should be able to understand and estimate a PTS event and in includes its evaluation in RPV lifetime, integrity and risk assessment.

8. ACKNOWLEDGMENTS

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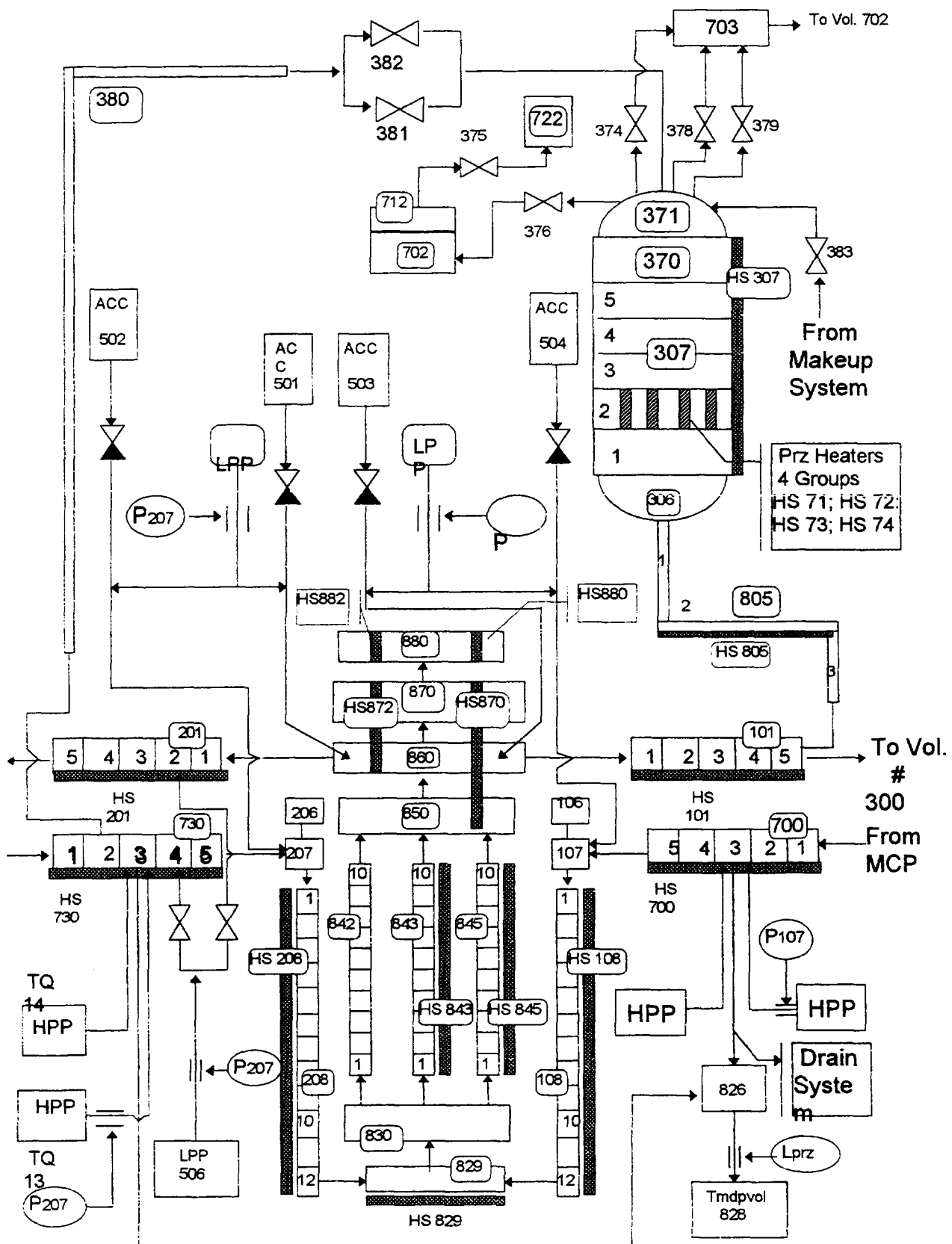


Fig. 1. Kozloduy Reactor and Pressurizer RELAP5 Model



SPECIALISTS MEETING ON
METHODOLOGY FOR PRESSURIZED THERMAL
SHOCK EVALUATION

5 - 8 May 1997, Esztergom, Hungary

**HANDLING LARGE PRIMARY TO SECONDARY LEAKAGE:
OPENING OF THE STEAM GENERATOR COLLECTOR COVER**

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1. INTRODUCTION

Nuclear power plants of VVER-440/213-type have several special features. The opening of the steam generator (SG) collector cover, as a specific primary to secondary circuit leakage (PRI-SE) was occurred first time in Rovno NPP Unit 1 on January 22, 1982 [1]. Similar accident was studied in the framework of IAEA project RER/9/004 [2], [3] in 1987-88 using the RELAP4/mod6 code. In the third IAEA Standard Problem Exercise (IAEA-SPE-3) the rupture was simulated to occur at the top of the hot collector of the horizontal SG of VVER type nuclear reactor [4], [5].

Pressurized thermal shock is a serious problem of the older US and Soviet RPVs as a consequence of the poor material quality. On the broad scale of presumable transients and accidents which may occur during the lifetime of the plant the RPV may suffer a very fast and significantly large temperature decrease while, at the same time the high system pressure may be maintained. According to the regulatory practice it has to be proven that the RPV may not be damaged due to a PTS event during its entire designed lifetime, taking into account the embrittlement due to radiation damage. In spite of the excellent quality of the Paks NPP RPVs the extended accident analysis of the Hungarian AGNES project (Advanced General and New Evaluation of Safety) also covers PTS analyses.

The AGNES project [6] - [8] was performed in the period 1991-94 with the aim to reassess the safety of the Paks NPP using state-of-the-art techniques. The project comprised three type of analyses: Design Basis Accident (DBA) analyses, Pressurized Thermal Shock (PTS) study and deterministic analyses for Probabilistic Safety Analysis (PSA). The thermohydraulic analyses has been performed by the RELAP5/mod2.5/V251 code version. These analyses were continued in AGNES project for evaluating the radiological consequences, or - for PTS study - to investigate the behaviour of the pressure vessel wall from the point of view of fracture mechanics [9] - [15].

The accident scenario from the point of view of PTS following the SG collector failure is presented in the paper.

2. SURVEY OF PRI-SE INITIATING EVENTS IN AGNES PROJECT

The first step of the thermohydraulic analyses in the course of the AGNES project was to determine the initiating events. Based on the many decades of NPP operating and regulatory experience, the list of initiating events as given in the US NRC Regulatory Guide 1.70 [16] covers the set of presumable transients and accidents which are relevant for the safety of the plant and environment, if probabilities and consequences are assessed. In the framework of the AGNES project these initiating events were analysed, supplemented with initiating events which are specific for VVER-type plants, as the SG collector cover opening [6] [7].

In the following tables the 6th group of initiating events of DBA (grouped into seven classes) for decrease of reactor coolant inventory including primary to secondary leakages (Table 2.1), the initiating events of analysed PTS cases (Table 2.2) are presented. It is also shown which cases were deterministically analysed to support PSA analyses (Table 2.3). The "US NRC" column indicates the number of the list [16].

The numbers of the cases show the total number of studied cases and the number of cases calculated by RELAP5/mod2.5.

Table 2.1.

Group 6 of initiating events: DBA cases for decrease of reactor coolant inventory

Initiating event	US NRC	Number of cases
6.1 Inadvertent opening of pressurizer safety or relief valve	15.6.1	2 / 2
6.2 Primary circuit line break outside the containment	15.6.2	1 / -
6.3 Steam generator tube break	15.6.3	3 / 3
6.4 Steam generator collector cover opening	-	3 / 3
6.5 Spectrum of loss of coolant accidents	15.6.5	20 / 12
6.6 MCP leak into intermediate circuit at loss of power	-	1 / -

Table 2.2.

Group 9 of initiating events: Pressurized thermal shock (PTS) cases

Initiating event	Number of cases
9.1 Inadvertent opening of pressurizer safety valve	4 / 2
9.2 Small and medium size LOCAs	4 / 4
9.3 200 % cold leg break	3 / -
9.4 Steam generator collector cover opening	3 / 3
9.5 Steamline break	2 / 2
9.6 Inadvertent ECCS operation	2 / -

Table 2.3.

Group 10 of initiating events: the deterministic analyses in support of PSA

Initiating event	Number of cases
10.1 Rupture of the control assembly housing	3 / 3
10.2 Large break LOCA in the cold leg	4 / 1
10.3 Large break LOCA in the hot leg	4 / 3
10.4 Medium size LOCAs in the cold leg	11 / 11
10.5 Inadvertent opening of pressurizer safety valve	2 / 2
10.6 Spray line break in pressurizer	2 / 2
10.7 Steam generator collector cover opening	3 / 3

3. THE PRESSURISED THERMAL SHOCK STUDY

In the framework of AGNES project the investigated 18 cases well cover the full range of PTS events. The present study aims to analyse the overcooling consequences of the opening of the SG collector cover [11].

The PTS analysis consists of 3 parts. First thermohydraulic response of the system is calculated using the system code RELAP5/mod2.5. When the thermohydraulic calculation reveals flow stagnation then the analysis is continued by means of the REMIX program. As the aim of the calculation is to investigate the behaviour of the pressure vessel wall from the point of view of fracture mechanics the analysis is finished with the ACIB-RPV code [12] calculation based on the downcomer pressure and temperature history given by the thermohydraulic part of the analysis. It should be investigated, whether any crack propagation occurs during the transient, either at the welds 5/6 and 3/5 or at the forged ring of the vessel opposite to the reactor core.

3.1 Definition of the accident

The postulated accident studied is the loss of integrity of the primary circuit by opening of the SG collector cover. After the initiating event the pressure in the system decreases quickly to the saturation temperature of the primary circuit, and reactor scram is initiated. The resulting rapid decrease of the primary side water inventory should be compensated by the actuation of the active and passive Emergency Core Cooling (ECC) systems at full capacity each. The High Pressure Injection System (HPIS) injection into the cold leg and the direct cold water feed from the Safety Injection Tanks (SIT) into the downcomer result in a rapid overcooling of the RPV wall. The consequence of the isolation of broken SG by operator is a rapid repressurisation in primary system.

The effect of the PTS produced by the opening of one SG collector cover has been investigated in 3 cases with different repressurisation times. In Case 1 the damaged SG is isolated by the operator at $t=7200$ s. In Case 2 the separation occurs at $t=1800$ s. In Case 3 it has been assumed that at $t=0$ s the electrical grid is lost and isolation of the leaking SG is executed at $t=3600$ s.

3.2 Acceptance criteria

The postulated accident is analysed from the point of view of the fulfilment of the thermohydraulic acceptance criteria in the AGNES project. There other kind of conservatism was required than in the present PTS study.

The vessel failure during a PTS event is considered to be acceptable, if no crack propagation occurs during the PTS or the crack is arrested and becomes stable before reaching 75% of the vessel wall thickness. If the safety factor for the finally arrested crack is below 1.1 for any of the three crack models used, then the fracture analysis must be repeated by using a finite element code and the arrested crack stability must be verified.

3.3 System initial conditions

The initial parameters have best estimated nominal values, except the ECCS water temperatures:

Core power	1375.0	MW
Core inlet temperature	265.0	°C
Primary pressure	12.26	MPa
Downcomer flowrate	8207.6	kg/s
SG pressure	4.63	MPa
Feedwater flowrate (for 1 SG)	129.0	kg/s
Feedwater inlet temperature	221.0	°C
Pressurizer level	6.9	m
Water temperature in SITs	40.0	°C
Water temperature in HPIS tanks	20.0	°C

Active and passive ECC systems are considered at their maximum capacity, i.e. 3 HPIS and 4 SITs are available.

Core conditions are determined in a conservative way from the point of view of PTS i.e. a BOC distribution has been taken into consideration. The decay power is considered as BOC too.

The hot channel power is calculated by using the following distribution factors:

$$\begin{aligned}k_q &= 1.35 && \text{hot assembly} \\k_k &= 1.15 && \text{hot rod in the hot assembly} \\k_{\text{eng}} &= 1.17 && \text{uncertainty factor.}\end{aligned}$$

The core by-pass flow is 7.3 % of the total downcomer flow.

3.4 Control and protection systems

The first part of the transient process is so fast that the reactor is quickly scrammed and the pressurizer becomes rather soon empty. As a result none of the control systems has influence on the process.

The following protection signals and systems are relevant in the actual scenarios:

Case 1 and 2

pressurizer level < 0.47 m	→	scram + SI signal
scram + 10 s delay	→	turbine trip
SI signal + 16 s delay	→	HPIS start
primary pressure < 5.88 MPa	→	SIT actuation
secondary pressure > 5.4 MPa	→	steam dump control valve actuation
secondary pressure > 5.75 MPa	→	SG safety valve actuation

Case 3

loss of electricity	→	scram+SI signal+MCP stop
scram + 10 s delay	→	turbine trip
SI signal + 16 s delay	→	HPIS start
primary pressure < 5.88 MPa	→	SIT actuation
secondary pressure > 5.4 MPa	→	steam dump control valve actuation
secondary pressure > 5.75 MPa	→	SG safety valve actuation
SG Δp > 5 bar	→	MSIV actuation

3.5 Thermohydraulic models and codes

The analysis for the system thermohydraulic response was performed by the means of the RELAP5/mod2.5 code. The nodalisation scheme for RELAP5 input model is presented in [9].

The coolant temperature calculated by the RELAP code represents the mixed coolant temperature in the downcomer. For the worst case (Case 3) to consider the effect of cold water separation after stagnation (because of loss of electricity for main circulating pumps) the downcomer temperature has been corrected using the results of the REMIX calculation.

From the RELAP and REMIX calculations the following data are transferred for the analysis of fracture mechanics code ACIB-RPV:

- primary pressure
- downcomer flow
- coolant temperature in downcomer
- SIT coolant temperature
- cold leg entrance temperature.

3.6 Results and conclusions of the thermohydraulic analysis

Table 3.1 provides the chronology of the events for the Case 3. The most important thermohydraulic parameters characterizing the postulated accident procedure are presented in [11], from which Figs. 3.1 - 3.6 from Case 1 and Figs. 3.7 and 3.8 from Case 3 are selected for this paper.

The thermohydraulic analysis is used only for preparing data for the PTS study. The assumptions are conservative from the point of view of the PTS event, but they are favourable from the point of view of thermohydraulic parameters. So the results of the thermohydraulic analysis proved again that the ECC system is able to prevent core damage in case of the opening of a SG collector cover.

The calculation showed that after the SITs become empty (Fig. 3.4) a moderate repressurization of the system can be observed (Figs. 3.1, 3.7 and 3.3), but the isolation of the damaged SG results in a rapid repressurization of the system (Fig. 3.1) and a moderate heating rate of the coolant (Figs. 3.6 and 3.8).

The lowest coolant temperature in downcomer was found in case when no forced circulation was maintained (Case 3).

Table 3.1 Chronology of events Case 3

time (s)	
0.0	break on top of SG hot collector
0.0	loss of grid
0.0	MCP run out begins
0.1	reactor scram signal
0.1	SI signal, Diesel start
5.9	primary pressure < 11.8 MPa
10.1	turbine trip signal, isolation begins
10.1	Diesel ready, for LIP t=0
11.1	LPIS pump in operation
15.1	HPIS pumps start
18.0	max. break flow 358.8 kg/s
24.4	pressurizer level < 0.47 m
26.9	pressurizer level < 0.17 m, HPIS-IV open
27.0	HPIS injection begins
32.6	primary pressure < 9.3 MPa
41.1	SG1 narrow range level > 0.2 m
52.1	BRU-A-1 opens
52.6	BRU-A-2 opens
61.0	broken SG1 filled up
63.6	pressurizer level < 0.1 m
122.9	SI Tank No. 1 injection starts
123.3	SI Tank No. 2 injection starts
2725.0	primary pressure minimum (4.803 MPa)
3313.1	SG1 $\Delta p > 5$ bar signal, MSIV closes
3318.0	SG1 safety valve opens first time
3600.0	operator closes the Main Gate Valves in broken loop
3870.0	minimum temperature in DC middle point (306.75 K)
3920.0	primary pressure > 12.26 MPa
4835.0	end of calculation (DC middle point t=350.2 K) (primary pressure p=12.87 MPa)

3.7 Calculation assumptions for PTS consequences

The fracture mechanical integrity analysis of the PTS event was performed by a deterministic analytical calculation method. This method is conservative and fully satisfies the regulatory requirements [13], [20]. The used data is presented in [11] and [15].

Three very conservative postulated cracks were used:

a): Axial semielliptical surface crack $a/c=2/3$, depth is $1/4T = 35$ mm. Location: the forging against the middle of the core.

b): Underclad axial crack in the ferritic welds, 4 mm deep and 50 mm long touching the interface of the clad, which is in complete contact with the vessel wall, and free of defects. Location: weld 5/6; weld 3/5.

c): Elliptical circumferential surface crack with a depth of 4 mm in the 15H2MFA weldment, and the clad is postulated broken. Location: weld 5/6; weld 3/5.

The vessel life time used in the calculations was 40 operational years with the same load and core configuration as realised during the first 5 years of operation. It is a conservative assumption due to the use of the low leakage core configuration in use afterwards.

The screening criteria were checked according to 10 CFR 50 rules and the analysis was performed according to the ASME code Section XI.

An analytical computer program called Analytical Calculation for Integrity of Beltline (ACIB-RPV) was used for the analysis of the stability of defects in reactor vessel walls during pressurised thermal shock.

3.8 Preparing the input data

Investigating the transient in question it was considered three different cases which are in keeping with the thermohydraulic study. In all cases, after some time, the isolation of the break is assumed. The data for the $p(t)$, $T(t)$ transient are based on RELAP5 (and REMIX calculations for Case 3) before the isolation of the break, and on model assumptions, after the isolating event (for the worst Case 3 between $t=3600$ s and $t=4400$ s).

In every case all three crack models were calculated and crack tip intensity factor (K_I) and crack tip temperature were determined for each crack model. The clad effect was taken into account too, as it has strong effects on the results in the colder temperature regions.

The core Beltline is treated as a plate during the calculation of the temperature distribution, and as a thick walled shell during the calculation of the stress and strain distribution. The modelling of the temperature distribution is made by Fourier solutions.

3.9 Results of fracture mechanics calculations

Since according to the results of the thermohydraulic calculation no significant difference was detected in the coolant temperature at the location of welds 3/5 and 5/6, the results are valid for the core zone and for weld 3/5, too.

In case of opening of SG collector cover, the lowest temperature is 23.8 °C. This is quite low compared to the RT_{NDT} of the weld which is 61.5 °C. According to the 10 CFR 50 rules the calculations are required, and the vessel failure risk associated to PTS is considered.

The less safety is in Case 3, in the case of the circumferential surface crack, because the depth of the crack is only 13 mm, and the warm prestressing of the cladding influences the stress intensity factor very strongly, and the high inner pressure has much lower importance.

According to the calculated results, the minimum value of the safety factor during the PTS is very sensitive on the transient behaviour in the colder temperature regions and the crack model used. The minimum safety factor is 1.10 and this means that no crack propagation will be initiated during the transient, and there is no need for continuation of the analysis for crack arrest calculations.

4. CONCLUSIONS FOR SG COLLECTOR FAILURE ANALYSES

Pressurized thermal shock is a serious problem of the older US and Soviet RPVs as a consequence of the poor material quality. In spite of the excellent quality of the Paks NPP RPVs the extended accident analysis of the AGNES project also covers PTS analyses [6]. The investigated 18 cases in AGNES project well cover the full range of PTS events. The following anticipated initiating events were analysed from the point of view of primary to secondary circuit leakages, namely the steam generator collector cover opening:

Case 1 - the steam generator is isolated after 7200 s

Case 2 - the steam generator is isolated after 1800 s

Case 3 - the steam generator is isolated after 3600 s and flow stagnation is assumed

The worst behaviour - lowest coolant temperature in downcomer - was observed in case when no forced circulation was maintained (Case 3).

Since reactor repressurization is a prohibited action as long as the temperature is below the critical temperature of the vessel, these scenarios are considered as a combination of a transient and an operator mistake. It is important to note, however, that there are very many valves in the VVER-440 primary circuit compared to the Western PWRs and the present operating procedures give absolute priority to the isolation of breaks.

The PTS analyses pointed out in a unique and unambiguous way that the isolation of primary circuit breaks should not be given priority over the cold repressurization of the reactor vessel. The necessity to give priority to the leakage isolation in case of a steam generator collector cover opening, represent further problems to be solved, considering new guidances [17] - [19] too.

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AGNES/DBA PAKS UNIT 3

TRASS No: 51
AEKI/01

PLOT No: 1.1.
16. 11. 1993

Opening of SG collector cover (PTS)
Case 1

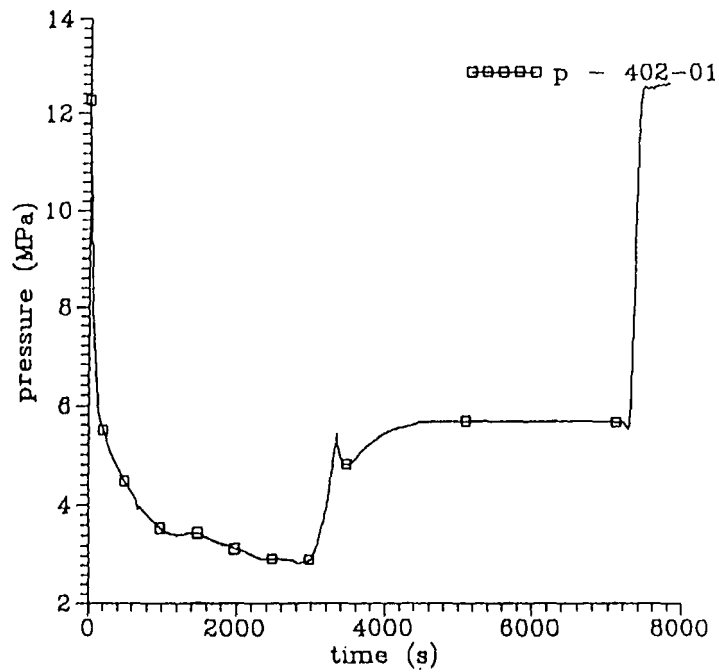


Fig. 3.1 Pressurizer pressure

AGNES/DBA PAKS UNIT 3

TRASS No: 51
AEKI/01

PLOT No: 2.1.
16. 11. 1993

Opening of SG collector cover (PTS)
Case 1

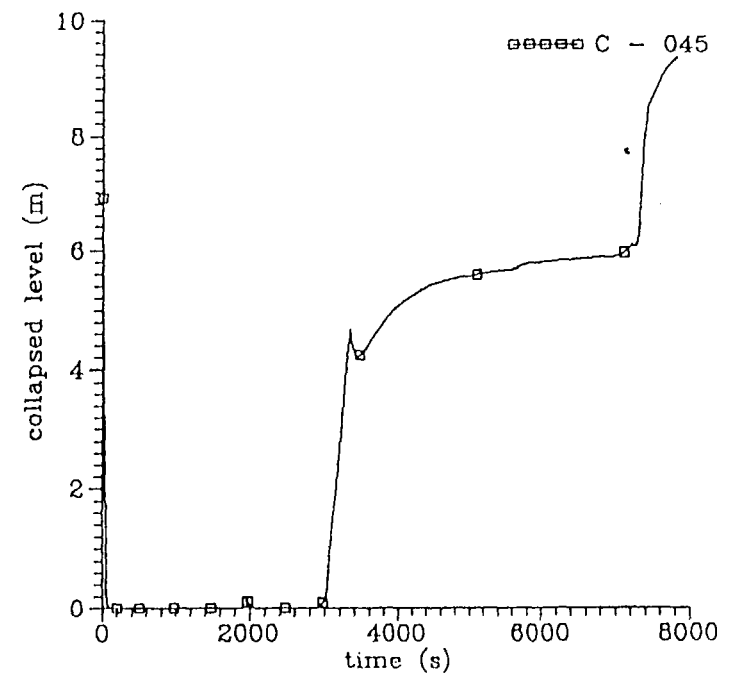


Fig. 3.2 Pressurizer water level

AGNES/DBA PAKS UNIT 3

TRASS No: 51 PLOT No: 9.1.
AEKI/01 16. 11. 1993

Opening of SG collector cover (PTS)
Case 1

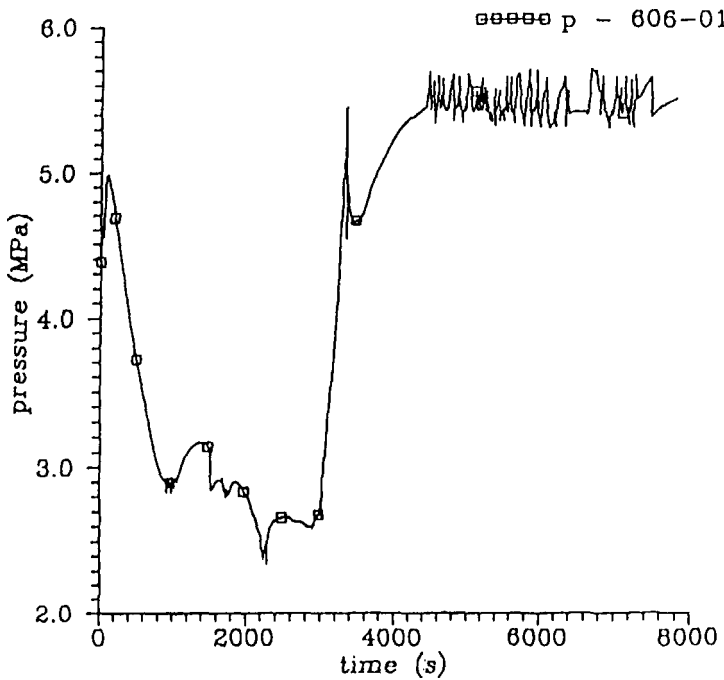


Fig. 3.3 SG pressure in single loop

AGNES/DBA PAKS UNIT 3

TRASS No: 51 PLOT No: 5.10.
AEKI/01 16. 11. 1993

Opening of SG collector cover (PTS)
Case 1

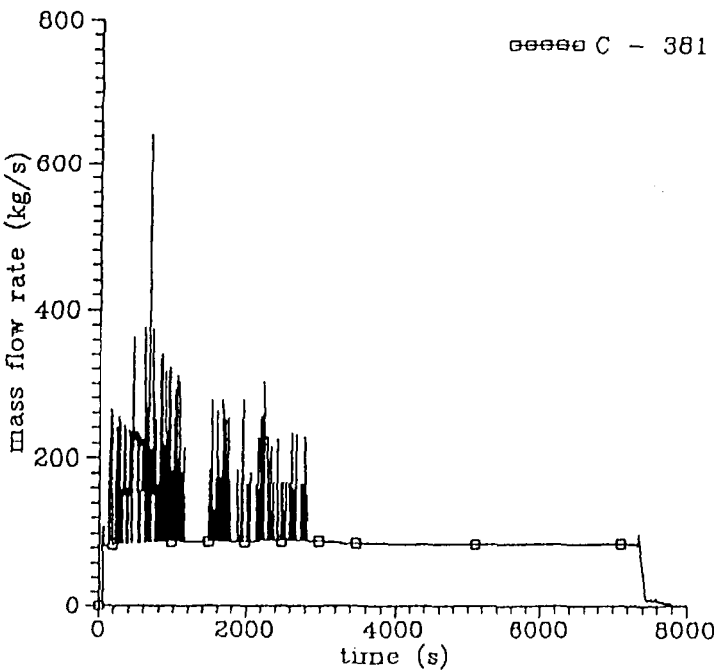


Fig. 3.4 ECC injection rate

AGNES/DBA PAKS UNIT 3

TRASS No: 51 PLOT No: 5.7.
AEKI/01 16. 11. 1993

Opening of SG collector cover (PTS)
Case 1

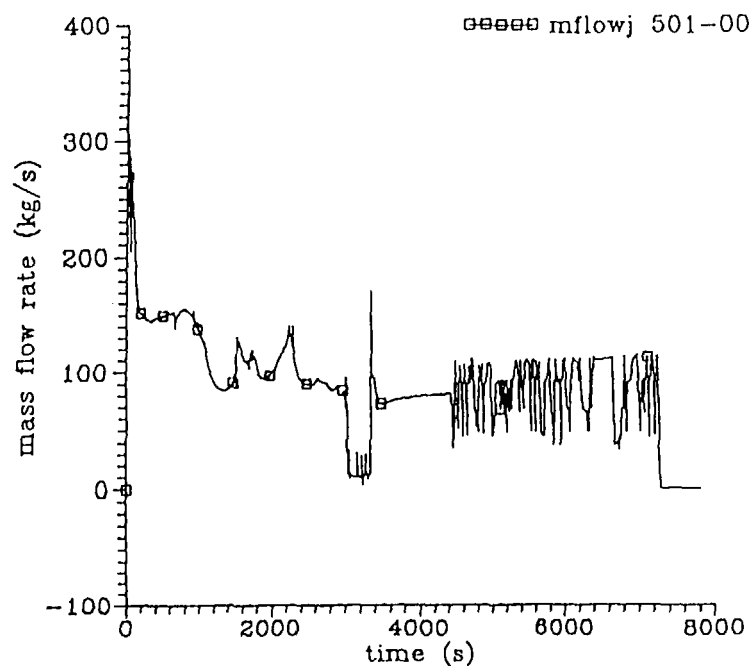


Fig. 3.5 Break mass flow

AGNES/DBA PAKS UNIT 3

TRASS No: 51 PLOT No: 4.8/b
AEKI/01 16. 11. 1993

Opening of SG collector cover (PTS)
Case 1

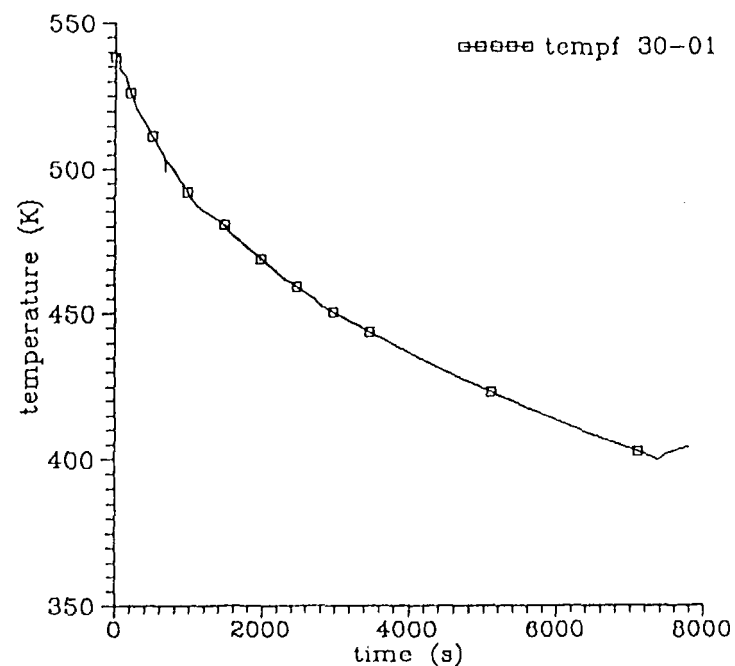


Fig. 3.6 DC middle point temperature

AGNES/DBA PAKS UNIT 3

TRASS No: 51 PLOT No: 1.1.
AEKI/01 07. 12. 1993

Opening of SG collector cover (PTS)

Case 3

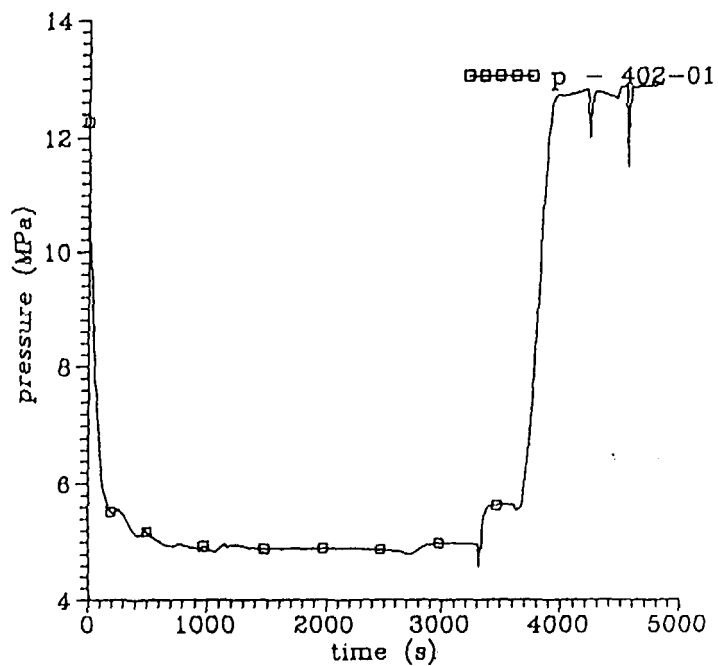


Fig. 3.7 Pressurizer pressure

AGNES/DBA PAKS UNIT 3

TRASS No: 51 PLOT No: 4.8/b
AEKI/01 07. 12. 1993

Opening of SG collector cover (PTS)

Case 3

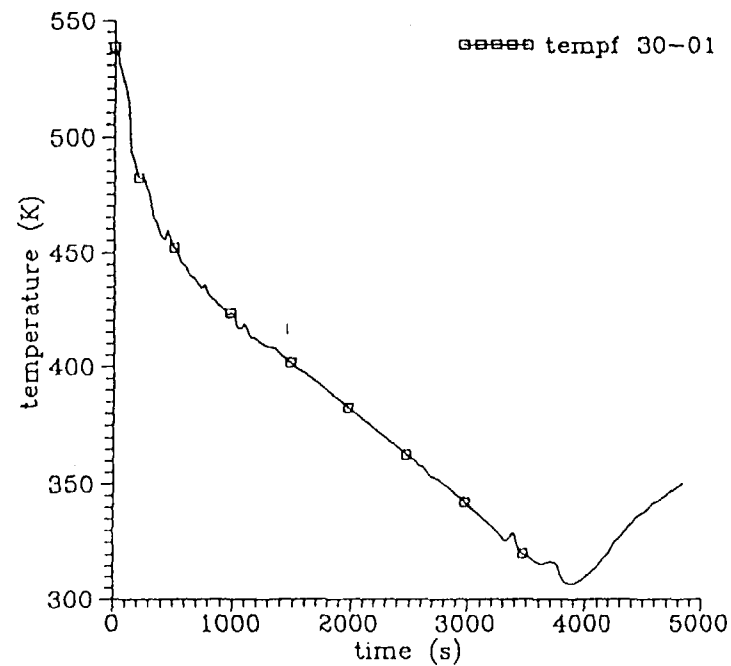


Fig. 3.8 DC middle point temperature



OVERCOOLING TRANSIENT SELECTION AND THERMAL HYDRAULIC ANALYSES OF THE LOVIISA PTS ASSESSMENTS

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ABSTRACT

This paper describes transient selection and thermal hydraulic analyses of various PTS assessment studies performed for the pressure vessels of the Loviisa VVER-reactors. Deterministic analyses have been performed in various stages of the PTS studies and they have always made the formal basis for design and licensing of the reactor pressure vessel. The integrated, probabilistic PTS study was carried out to give an overview of the severity of all different PTS sequences, and give a quantitative estimate of the importance of the PTS issue in relation to the overall safety of the plant. Later, the sequences including external flooding of the pressure vessels were added to the PTS assessments. Thermal recovery annealing of the Loviisa 1 reactor pressure vessel took place during refuelling outage in 1996.

1. INTRODUCTION

Early results in 1980 from the surveillance specimen program of the reactor pressure vessel (RPV) of the Loviisa Unit 1 indicated a higher than expected neutron irradiation embrittlement rate for the beltline region weld [1]. To ensure safe operation of the RPV, several modifications were decided to be accomplished in the plant. The reactor core was reduced in order to decrease neutron exposure on the RPV wall by replacing the 36 peripheral fuel assemblies with stainless steel dummies. The temperature of the ECCS water tank was increased to 55°C, and the temperature in the ECCS accumulators injecting directly to the downcomer was increased to 100°C.

The decisions were based on the brittle fracture calculations assuming a postulated LOCA. The modifications were implemented in 1980 at Loviisa Unit 1, and later gradually at Loviisa Unit 2. Soon after the TMI-2 accident it was realized that potential risk from slower overcooling events characterized by high system pressure (Pressurized Thermal Shock, PTS) might be higher than that from postulated LOCAs.

An extensive PTS analysis for Loviisa Unit 1 was carried out in the years 1982 to 1986 mainly by IVO's own staff. The analysis started with deterministic overcooling scenarios. Deterministic structural assessment is characterized with a detailed 3D fracture mechanics calculations using assumptions of nonuniform temperature and heat transfer fields in the downcomer. The studied transients were selected to represent very conservative cooling conditions. On the other hand, however, it was felt that there are plenty of uncertainties with regard to the realistic plant behaviour.

The complexity of many interacting systems makes it very difficult to evaluate the significance of the limiting transients. That is why the analysis was completed with an integrated, probabilistic PTS study. Probabilistic assessment covered all potential PTS initiators, and all the plant conditions including hot zero power conditions and plant cooldown and heatup phases. Thermal hydraulic and probabilistic fracture mechanics calculations were made for a large number of transients.

The Finnish regulatory authority STUK has required a renewal of the operating license of the Loviisa 1 RPV in every three years. The licensing is based on the deterministic assessments, even though the probabilistic PTS risk is also updated respectively. In 1989, and 1992-1993 a limited number (currently 6 transients) have been selected to represent assumed limiting cases according to the deterministic failure criteria for design basis events. External flooding of the reactor vessel has been included to the considered transients.

The aim of this paper is to follow the development of transient selection and thermal hydraulic analysis criteria during these years. The main part of the paper was already presented two years ago in Piešťany meeting in Slovakia [9].

In Section 2 the thermal hydraulic aspects are reviewed. Section 3 describes the integrated probabilistic PTS study that was carried out for Loviisa 1 in 1984 to 1986. The deterministic licensing of the pressure vessels are the topics of Section 4. External flooding of the reactor vessel brings further complications to the studies. Finally, thermal annealing of the Loviisa 1 vessel is described in Section 5.

2. THERMAL HYDRAULIC ASPECTS

2.1 Thermal hydraulic basis for transient selection

It is important to account for all the different thermal loading mechanisms in the downcomer during the overcooling events when performing the selection of transients. These loading mechanisms are

- the final temperature in the downcomer (this is the most important parameter)
- the rate of the temperature decrease
- nonuniformity of the temperature fields (cold plumes)
- nonuniformity of the coolant-to-wall heat transfer coefficients in the downcomer

Additionally, the mechanical stresses caused by high primary pressure increase the total stress level.

The final temperature in the case of primary system leaks tends to approach the injection water temperature, which has been raised to 55°C in Loviisa as well as in several other VVER-440 units. This as such represents the most obvious choice of a limiting case. Different variations to enter to this final temperature are then obtained by studying the other thermal loading mechanisms. The final temperature level is probably the most important parameter, because the vessel material properties deteriorate in the lower temperatures.

The rate of temperature decrease: In the large break LOCA the cooldown to injection water temperature takes place in the time frame of some minutes. In the case of small and intermediate break LOCAs there are two cases: in case of continuous natural circulation the high-pressure injection (HPI) cools down all the primary system. In case of the flow stagnation the mixing volume consists of the cold legs, downcomer and part of the lower plenum, which cools down with a characteristic time constant.

Nonuniform temperature and heat transfer fields are created by cold plumes resulting from the high pressure safety injection (HPI) to the cold legs. Also accumulator injection to the downcomer can create nonuniformities. In principle such plumes can be created only during flow stagnation conditions, or during steam line breaks. The influence of these nonuniformities is much more pronounced for base material than for the lower core peripheral weld, because the turbulent mixing mechanisms tend to smooth out the temperature differences in the lower parts of the downcomer. In many cases the nonuniform heat transfer field is more significant than the nonuniform temperature field. This is an outcome of the situation where the temperature differences are already smoothed out between the plume and the ambience. At the same time the heat transfer coefficients are much higher in the plume area, because of the higher flow velocity than in the ambient fluid of the downcomer.

High primary pressure occurs during compensated SBLOCAs, after leak isolation in isolatable LOCAs and during other transients such as steam line breaks.

The primary circuit flow stagnation is very important phenomenon during the overcooling transients, because it affects significantly all the above thermal conditions. Therefore a careful treatment of the flow stagnation conditions and criteria is necessary.

Another significant feature is thermal inertia of the thick pressure vessel walls. The cases with the low temperature decrease rate should be analyzed for an extended period.

2.2 Transient selection for the initial PTS calculations

Based on the above loading mechanisms, for the initial PTS studies three deterministic accident sequences were selected in 1983. Each of them included at least three out of the four significant contributors: the final downcomer temperature, fast transient cooldown,

nonuniformities and high system pressure. The first transient was a large steam line break with reduced HPI injection leading later to the stuck-open PRZ safety valve and to its reclosure. The second transient was the stuck-open and reclosing PZR safety valve with maximum HPI injection. The third transient was a medium-size LOCA leading to ECCS accumulator injection.

As a result we obtained a set of limiting conditions, which tend to overestimate thermal loading of the vessel.

2.3. Further assumptions and considerations for thermal hydraulic analyses

Thermal hydraulic analyses of overcooling sequences include many features that are different from the traditional thermal hydraulic accident analyses. First, it has to be realized that perfect operation of some systems leads to more severe overcooling conditions than reduced operation would do. Secondly, for some parameters it is conservative to assume the values from the other end of the uncertainty band than for the traditional DBA analyses. For example pump flow rates should be maximized, and the decay heat power should be minimized.

A special attention should be given for the transients where the primary flow stagnation is possible. The decision of the stagnant flow conditions is more difficult, since the used system codes tend to predict an oscillating single-phase natural circulation flow [3].

Feedback from the stress analysis have shown that the heat transfer coefficient from coolant to the downcomer wall influences the results only when the value is less than 3000 to 5000 W/Km². This is due to the poor thermal conductivity of the stainless steel cladding. With the higher heat transfer coefficient values, the most limiting thermal resistance comes from the cladding.

Because of the complexity of the overcooling transients, it is not straightforward, and often even not possible, to define conservative or limiting conditions. Therefore realistic assumptions should be taken when feasible. This leads, however, to the necessity to perform several calculation runs even for the same transient. It is necessary for creating understanding of the sensitivity to the variations of parameters and assumptions, and to the related uncertainties.

When planning any modifications of the plant or procedures due to the PTS, care must be taken not to forget the primary safety objective of core cooling.

Traditionally only internal cooling of the downcomer has been accounted for. This is consistent with assuming the crack on the inner surface of the vessel wall. The inner surface cracks have been subject to a greater interest, since embrittlement is higher in that region. In case of cooling vessel from the outside, the inner surface stresses are reduced. However, if there is a possibility of cooling the vessel from outside by flooding the cavity, the outer surface cracks should be considered, and cooling from outside should be added

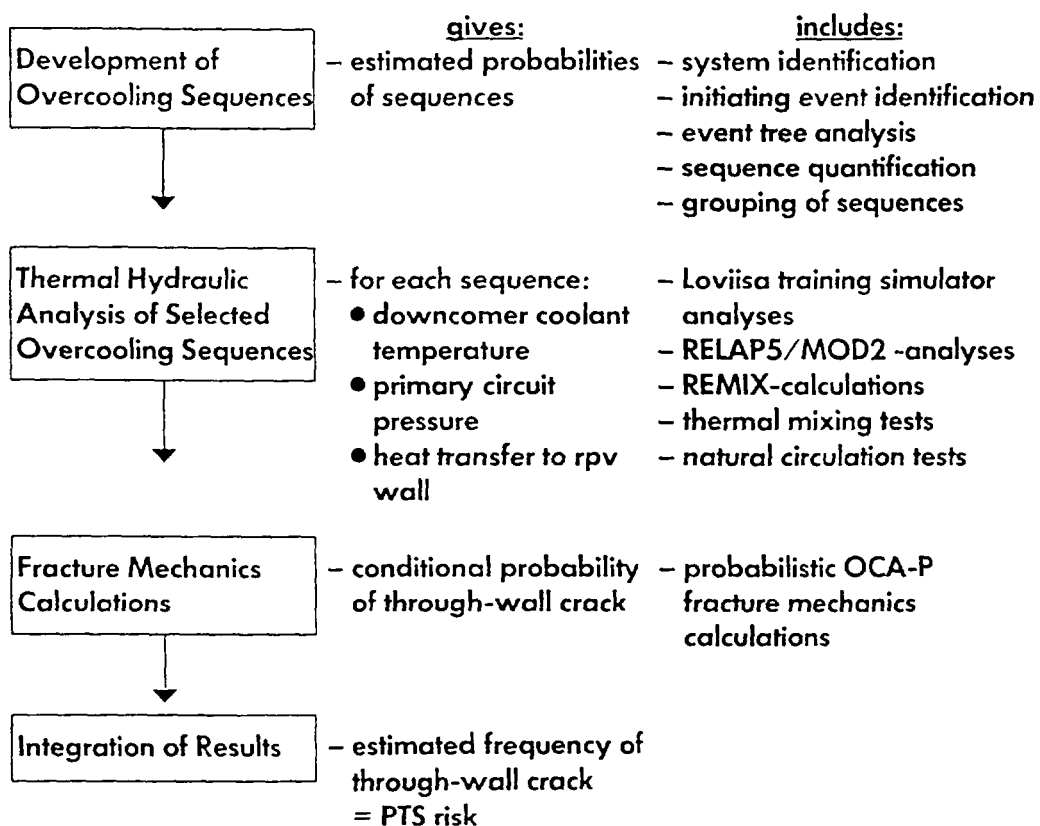
to the thermal hydraulic boundary conditions. For the Loviisa studies these were included only in last few years.

3. PROBABILISTIC PTS STUDY

The structure of the integrated probabilistic PTS study is illustrated in Table I. In principle the study was performed according to the method employed by Oak Ridge National Laboratory [6] in a program sponsored by U.S. Nuclear Regulatory Commission (NRC).

TABLE I The structure of the Loviisa RPV integrated PTS study

PROBABILISTIC PTS STUDY OF LOVIISA NPS



The first step was development of the overcooling sequences. For the selected sequences, thermal hydraulic analyses were carried out to give initial data for stress and fracture-mechanics calculations. Integration of the estimated sequence probabilities with conditional through-wall crack probabilities result in the estimated frequency of a through-wall crack. This point estimate represents the overall PTS risk. The uncertainty and sensitivity analyses formed an essential part of the study.

3.1. Development of overcooling sequences

The development of the overcooling was a very challenging task, as it had to result in complete identification of all classes of events that contribute to the overall PTS risk. The plant-specific study requires a thorough knowledge of the plant systems and their response to abnormal occurrences. The knowledge was readily available, since the IVO's own staff, being very familiar with the plant, carried out the work.

The work started by identifying the systems affecting overcooling transients, and by identifying the important operator actions associated with potential overcooling sequences.

The transients selected as initiating events include those that either directly or through consequential failures lead to downcomer temperature decrease. They included primary pressure boundary breaks, steam system breaks and leaks, spurious starts of high-pressure and high-capacity pumps and so forth.

The plant system response was determined for each initiator employing an event tree analysis. Operator actions associated with initiators were included. To reduce the number of sequences the screening frequency limit of 10^{-7} /reactor-year was defined. The Loviisa training simulator was used extensively to give feedback on the process technology, operator actions and thermal-hydraulics.

The development of overcooling sequences resulted in 21 selected transient classes among 36 identified ones as presented in Annex 1. The number of selected sequences is given separately for every class, total number being 121.

New important sequence classes and other aspects in comparison with previous deterministic studies are such as:

- In many sequences the pressure vessel failure would take place between 1 and 2 hours from the beginning of the transient due to the thermal inertia of the vessel wall. Consequently, the studied accident time was extended to 2 hours.
- A variety of isolatable LOCAs were included in the deterministic analyses. In the VVER-440 reactors there are several possibilities to isolate the primary leak. Reseating of the stuck-open PRZ safety valve brings also here a significant contribution.
- New complex accident progressions were found, which could not be quantified with the earlier analysis. An example of such is the transient starting with a steam line leak, and leading after the SG dryout to the repressurization in the primary circuit and opening of the PRZ safety valve, which could stick open for a period and then reseal.

- Large primary-to-secondary leakage accidents turned out to be an important contributor, since operators have a possibility to isolate the leak after significant overcooling of the primary circuit due to HPI injection.
- Standby, heatup and cooldown conditions bring many new sequences with the stagnation in the primary circuit. New risk sources were also identified from PRZ safety valve testing, and other pressurization sequences during cold conditions.

3.2. Thermal hydraulic analyses

The assumed transient time was 2 hours in all considered transients. The main task of the thermal-hydraulic calculations was to provide the following parameters during the overcooling event for the stress calculations

- downcomer temperature field
- coolant-to-wall heat transfer coefficients in the downcomer
- primary circuit pressure

Temperature and pressure curves were obtained from the Loviisa training simulator and RELAP5-calculations, except in cases of the cold leg flow stagnation, where separate methods were applied.

The flow stagnation was assumed to occur in two different situations:

- 1) If LOCA occurs during conditions where decay heat level is low, the flow stagnation can take place in compensated SBLOCAs.
- 2) During medium size LOCAs flow stagnation can be assumed, when steam enters the hot legs.

The importance of the flow stagnation comes from two aspects. First, the HPI injection causes a higher overall cooldown rate of the downcomer fluid, because the injected water is mixed to a smaller volume (cold legs, downcomer and lower plenum), which in turn creates higher thermal stresses. Secondly, the temperature and heat transfer nonuniformities due to the HPI plumes in the downcomer are also of concern.

The flow stagnation situations require special calculational tools, which have to be assessed against the experimental data. IVO carried first out an extensive experimental research program to investigate thermal mixing in the Loviisa specific geometry [3]. Based on these results, the REMIX thermal mixing program [4] was modified for the Loviisa conditions and assessed for the application calculations [5].

In flow stagnation cases the role of RELAP5 calculations is to estimate the initiation of the stagnation, and give the initial temperature conditions and HPI flow rates as boundary conditions for the REMIX calculations. Only transient cooldown of the whole downcomer was included to the final thermal-hydraulics, since the probabilistic OCA-P structural

analysis could not account for multidimensional effects. Thus a uniform temperature and heat transfer field was assumed for these calculations.

Thermal hydraulic analyses were performed for 55 sequences out of the selected 121 sequences. These transients were stylized and grouped using thermal hydraulic engineering judgement.

Figures 1 to 4 illustrate examples of the downcomer temperature and primary pressure curves for some of the most significant sequences.

3.3. Further plant modifications

In connection to the sequence selection and thermal hydraulic analyses, a need for some plant modifications became evident. The flow capacity and shut-off head the HPI pumps were reduced. The flow capacity of the small relief line from the pressurizer was increased. The main steam line and feedwater line isolation criteria were totally modified to be sensitive for the whole spectrum of main steam leaks. Main changes made to the emergency operating procedures were connected to isolation of primary side leakages. The operators are not allowed to isolate a primary leak, except in case of a primary-to-secondary leakage.

3.4. Integration of results

Starting from the thermal hydraulic results for selected 55 sequences, the probabilistic fracture-mechanics calculations were carried out by applying probabilistic code OCA-P [7]. Because of the long run times of this Monte Carlo -based method, the selected sequences were further grouped and stylized so that finally 26 different transient cases were calculated. The used calculation method OCA-P assumes uniform temperature and heat transfer field in the downcomer and an infinite 2D crack. Therefore the temperature and heat transfer nonuniformities were handled with limiting point value assumptions.

TABLE II PTS risk contribution by transient category

Category	Through-wall crack frequency (10 ⁻⁸ /a)
Primary-to-secondary leaks	21
Intermediate break LOCAs	12
Small break LOCAs	2.4
Large steam line breaks	1.7
Large break LOCAs	1.4
Cold pressurization of primary circuit	1.2
Small steam line breaks	1
Other categories	0.013

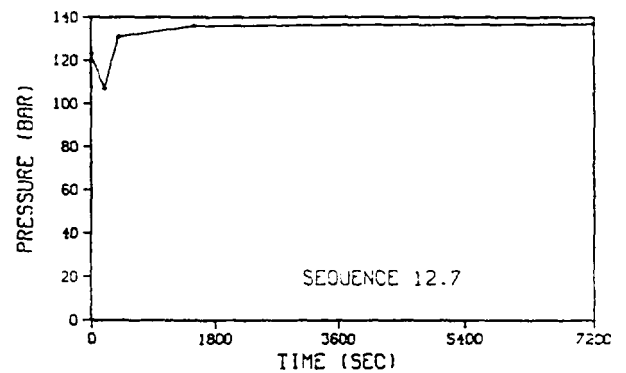
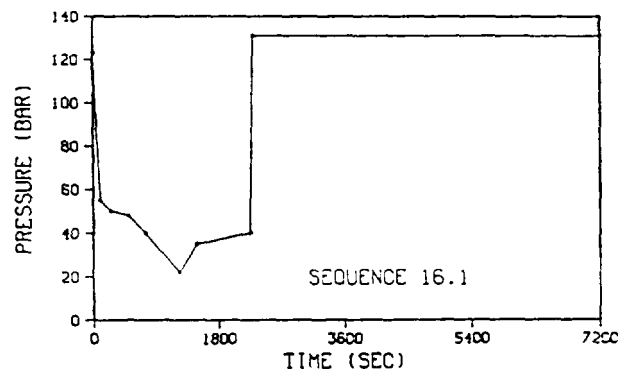
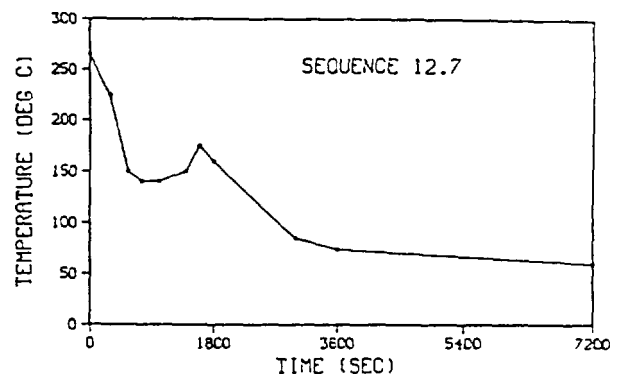
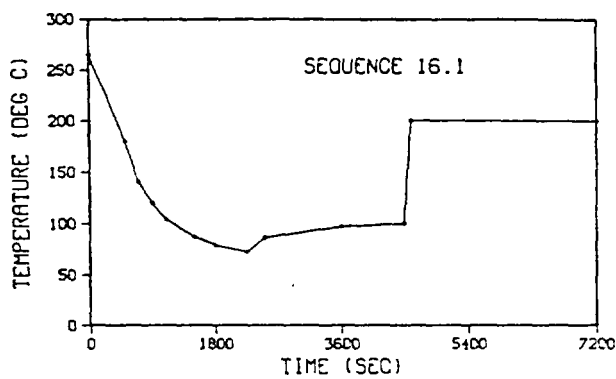


Figure 1. SG collector break, downcomer temperature and primary pressure

Figure 3. Small steam line break, downcomer temperature and pressure

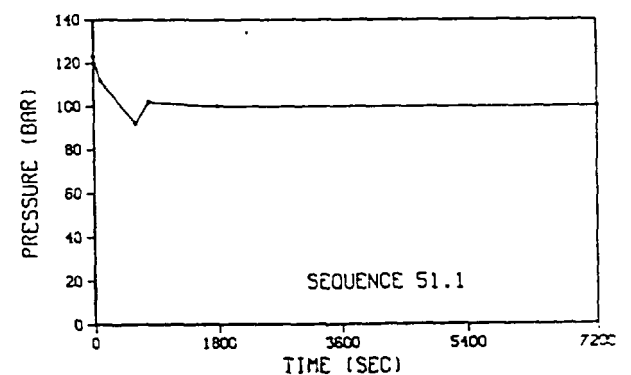
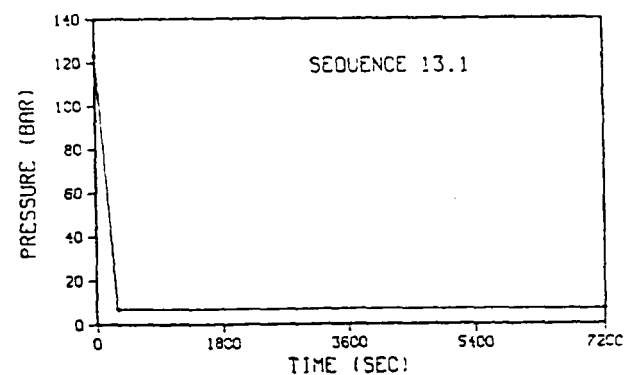
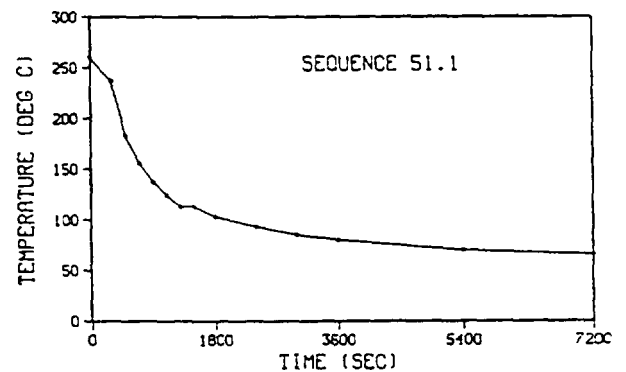
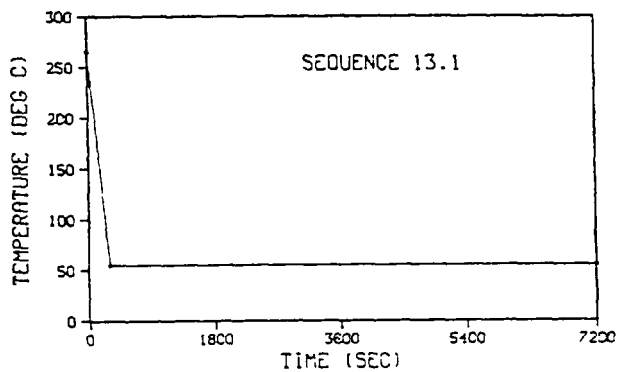


Figure 2. Large break LOCA, downcomer temperature and primary pressure

Figure 4. Small break LOCA (H&C), downcomer temperature and pressure

The contribution of different sequence classes have been gathered to Table II. This is an initial distribution for Loviisa 1 from 1986 published in Ref. 8, and it has been updated twice since then. In later revisions the contribution of pressurization of primary circuit in cold conditions became much higher, and reducing actions were taken correspondingly at the plant. PTS risk contribution by operation mode shows that 68% of the total risk is from transients starting at full power conditions, 5% at hot zero power and 27% during heatup and cooldown phases. It is interesting to note that the time fraction of heatup and cooldown is only about 3%.

3.6 Conservatism, uncertainties and sensitivities

No probabilities are associated to the thermal hydraulics, since it is not feasible to run statistically meaningful number of runs for various sequences. Instead, one tries to make realistical assumptions to obtain a best-estimate history of the sequence. Thus thermal hydraulic part of the probabilistic study differed significantly from the other parts of the PTS study, when sensitivities, uncertainties and conservatisms were estimated.

Table III demonstrates how thermal hydraulic factors together with the material properties dominate the sensitivity of the final through-wall crack frequencies [8]. By far the most important parameter is the downcomer temperature. At the same time the most significant conservatisms assumed were the stagnation predictions and the break isolation predictions.

TABLE III The sensitivity of the total PTS risk to the most important variables

Variable	Sensitivity
Downcomer temperature	10.0
Flaw density	3.3
Frequency of the primary-to-secondary leakage at full power	2.6
Shift in nil-ductility reference temperature (ΔT_0)	2.3
Nil-ductility reference temperature at zero fluence (T_{k0})	1.6
Crack initiation fracture toughness value (K_{Ic})	1.5

The most important source of uncertainty in the isolatable LOCAs is the prediction of the isolation time. This is true for both the stuck-open safety valves and LOCAs isolated by the operator, not depending on the operator action being erroneous or correct. In most cases this problem was overcome by assuming the primary pressure to be at the PRZ safety valve opening pressure throughout the transient. Only for the steam generator

collector break a certain moment (20 min from the beginning of the accident) was predicted for isolation, because the operation action here is quite predictable.

Again, it is of crucial importance to conserve the balance between the objective of preventing PTS and the objective of the core cooling. In some cases the requirements may be contradictory. If one wants to add conservatism to all considerations, it is possible that the outcome is counterproductive from the nuclear safety point of view.

4. DETERMINISTICALLY BASED LICENSING SEQUENCES

4.1. Selection of deterministic sequences for downcomer overcooling

For the deterministic licensing calculations the selection was made by looking for all the sequences resulting from the integrated PTS study with a higher than $10^{-8}/a$ through-wall crack frequency. After deterministic screening (e.g. elimination of those where excessive failures were assumed) and combination of the sequences, the following six transients were selected:

- steam generator collector break (90 cm²) where operator isolates the break after twenty minutes from the accident beginning;
- large break LOCA with no isolation;
- stuck-open pressurizer safety valve (and later reclosing) after repressurization of the primary circuit following small steam line break;
- small break LOCA with very low decay heat, which leads to the flow stagnation;
- medium size isolatable LOCA in hot standby conditions, and
- inadvertent pressurization in the cold state.

Since the fracture-mechanics calculations are made with the 3D methods, the nonuniformity effects are included. The temperature decay in the HPI plume and the transient downcomer cooldown is calculated with the REMIX program. The heat transfer coefficient 5000 W/Km² is applied for the plume region where flow velocities are higher. In the ambient downcomer fluid the heat transfer coefficient value 2000 W/Km² is applied. The IVO thermal mixing experiments support the choice [2]. The values are also consistent with the values reduced from the various thermal mixing experiments.

4.2. External cooling of the pressure vessel

During most of the accident sequences, the Loviisa reactor cavity will be inherently flooded with the water. This water comes from the primary circuit leakage, from the ECCS and spray systems and from the melting ice condenser. Flooding of the reactor cavity might take place also during a spurious operation of the spray system, even though such sequences can be shown to be very unlikely.

The external cooling of the pressure vessel may lead to the integrity problem, if the crack is assumed to locate on the outer surface of the vessel. Fortunately, embrittlement due the neutron fluence is not so severe near the outer surface.

The most limiting conditions are obtained for the medium size LOCA case, where the cavity flooding is added to the sequence. New difficulties and uncertainties appear when trying to estimate the effect of the external cooling. The system code calculations cannot be utilized, because the happenings outside the coolant systems are not modelled. Temperature of coolant flooding the reactor cavity is not easily estimated for the accident situations. The flow paths and heat transfer behaviour in the gap between the thermal insulation and the reactor vessel have to be estimated. Additionally, the assumptions concerning the crack sizes have to be reconsidered, because the inspection work for the reactor vessel outer surface are different.

4.3. Structural calculations

Plastic elastic brittle fracture calculations have been done with finite element methods both by IVO and by Technical Research Centre of Finland (VTT). IVO has used the BERSAFE code with its pre- and post-processing modules, and VTT uses the ADINA and PATRAN codes. Thus, generating the element models and running the codes have been done with two independent calculation systems.

The main aim of IVO calculations has been to predict the crack initiation. VTT has studied also the crack arrest in the large break LOCA case.

For the crack initiation calculations IVO has applied various surface and subsurface crack sizes: 5 x 10; 13.5 x 30; 15 x 30 and 15 x 50 mm, and infinitely long cracks (360°) of depth 5 mm. These cracks have been assumed to locate mostly in weld 5/6 (i.e. the circumferential weld on the lower core region). The orientation of these cracks has been assumed to be along the weld, i.e. circumferential cracks. Thus they are perpendicular to the HPI plumes in the rpv wall.

In addition to the thermal and mechanical loading stresses, the calculations account for stresses caused by nonuniform temperature and heat transfer fields (cold plumes), cladding residual stresses and weld seam stresses.

The used finite element models apply rotation and axial symmetry with rigid boundary conditions. The cracks are modelled with a very fine element structure.

5. THERMAL ANNEALING OF THE LOVIISA 1 VESSEL

Finally, after reconsideration of the PTS transients and the vulnerability of the Loviisa 1 reactor vessel, IVO made a decision to anneal the weld on the lower core level. The annealing took place during refuelling in 1996 [10].

The annealing parameters were established based on material investigations and stress analysis. A target value for the annealing temperature range was 465 - 505°C and for heating/cooling rate 20°C/h. According to the results of the material investigations about 80% recovery of transition temperature shift caused by irradiation embrittlement can be achieved using the above mentioned annealing parameters.

The recovery annealing was performed using electrical heating device owned by Bohunice NPP. It had been previously used for the annealing of two VVER 440/230 units in Bohunice V1 plant. The annealing device is equipped with 13 resistance heated power controlled heating sections arranged in to three groups with total nominal heat output of about 1000 kW. The middle group of heating sections is used for heating of the annealing area and the other two groups for controlling axial temperature gradients.

Temperature distribution during the annealing was monitored with thermocouples installed on the inside and outside surfaces of the RPV. Stresses caused by local heating of the weld area were calculated on-line by means of 3 dimensional Finite Element model. The measured temperature data were used as an input information for the stress analysis. The annealing device as well as the monitoring systems were designed by Skoda Nuclear Machinery Ltd. The proper functioning of the annealing device and the monitoring systems was verified before shipment by a "trial" annealing of a full scale model RPV installed in the Bohunice NPP. In the "trial" annealing the thermal stresses were measured by strain gages to verify the stress calculation system.

Annealing of Loviisa 1 including heating and cooling phases took place between 4 to 8 August 1996. The work was carried out according to the plans with practically no delays or deviations. The total time that the annealing related works kept RPV occupied was 12 days.

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CLASS	TITLE	SEQUENCES
LOSS-OF-COOLANT ACCIDENTS		
11	Small-break LOCA, FP	5
12	Medium-size LOCA, FP	9
13	Large-break LOCA, FP and HZP	3
14	Small-break LOCA, HZP	5
15	Medium-size LOCA, HZP	1
16	SG collector break, FP	1
17	SG collector break, HZP	1
18	SG tube rupture, FP	1
19	SG tube rupture, HZP	1
STEAM LINE BREAKS		
21	MSLB inside containment, FP	9
22	SSLL before MSIV outside containment, FP	26
23	MSLB inside containment, HZP	8
24	SSLL before MSIV outside containment, HZP	15
25	Reactor scram	
26	Loss-of-offsite power, FP	
27	Loss-of-offsite power, HZP	
OTHER CLASSES		
31	Inadvertent spraying of PRZ	
32	PRZ level control failure	
33	Inadvertent operation of make-up piston pumps	
34	Loss of condenser vacuum	
35	Inadvertent MSIV closure	
36	Inadvertent operation of high capacity make-up pumps	
37	PRZ pressure control failure	
INITIATORS DURING HEATUP AND COOLDOWN		
51	Small-break LOCA, H&C	3
52	Medium-size LOCA, H&C	7
53	Large-break LOCA, H&C	3
54	SG collector break, H&C	1
55	SG tube rupture, H&C	1
61	MSLB inside containment, H&C	7
62	SSLL before MSIV outside containment, H&C	10
71	Inadvertent operation of HPI, H&C	
72	Inadvertent operation of make-up piston pumps, H&C	
73	PRZ pressure control failure, H&C	
74	Isolation of primary system letdown, H&C	
75	Inadvertent operation of PRZ spray, H&C	
76	Inadvertent pressurization in cold state	2
77	Inadvertent operation of PRZ heaters	

Abbreviations:

FP full power

HZP hot standby

H&C heatup and cooldown

SG steam generator

PRZ

MSIV

MSLB

SSLL

pressurizer

main steam isolation valve

main steam line break

small steam line break

Session 5

6 MAY, TUESDAY

PTS analysis 2.

Miller, A.	OECD/NEA	PWG-3 Activities in the Field of PTS
Keim, E. Schöpper, A. Fricke, S.	Germany	Fracture Mechanics Assessment of Surface and Sub-Surface Cracks in the RPV Under Non-Symmetric Loading

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OECD Nuclear Energy Agency Activities on PTS evaluation

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IAEA Specialists Meeting on Methodology for PTS evaluation, Esztergom, 5-8 May 1997

Abstract

The Safety Division of the OECD Nuclear Energy Agency provides the secretariat for the Committee on the Safety of Nuclear Installations (CSNI), which deals with technological aspects, and for the Committee for Nuclear Regulatory Activities (CNRA) dealing with regulatory aspects. Under these committees, activities are carried out through five Principal Working Groups (PWGs). The relevant group for PTS is PWG-3 on the integrity of structures and components. There is also PWG-2 on coolant system behaviour, but the thermal hydraulic aspects of PTS have not been considered by PWG-2.

PWG-3 carries out its work in a similar manner to the IAEA IWG LMNPP, by preparing reports and organising round robins, Specialists Meetings and Workshops. The general context of RPV PTS has been considered in several workshops: on the 'Complementary roles of Fracture Mechanics and Non-Destructive Examination in the Safety Assessment of Components' in Wuerenlingen in 1988; on the 'Safety Assessment of RPVs' in Espoo in 1990; and on 'Fracture Mechanics Verification by Large Scale Testing' (joint with IAEA) at Oak Ridge in 1992. Activities specific to PTS have been an international survey on regulatory practices on PTS carried out in 1991, and a series of fracture round robins addressing PTS conditions organised by GRS in Germany and ORNL in the USA.

The first of these round robins was FALSIRE (Fracture Analysis of Large Scale International Reference Experiments). This considered 6 large scale fracture tests with a limited amount of ductile tearing. 37 participants from 19 organisations used a wide variety of analysis methods. The importance of correct structural analysis in the first step was emphasised. Application of fracture methodologies was partially successful in some cases but not in others. There was a workshop in Boston in 1990 to discuss the results, and the final report was issued in 1994.

There was a follow-on FALSIRE II, which addressed 7 reference cleavage fracture experiments, focusing primarily on the behaviour of shallow cracks in the transition temperature region. Included were tests in which cracks showed either unstable extension or two stages of extension. Also included were the effect of clad surfaces and biaxial loading conditions. 30 participants from 22 organisations took part. The structural response scatter was much improved over that from FALSIRE I. In specific cases, the loss of constraint effects observed required a second fracture parameter to be introduced in the fracture model to characterise crack tip conditions. Additional data are required to validate these approaches. A workshop was held in Atlanta in 1994 to discuss the results, and the final report has been issued recently.

The current phase is the RPV PTS International Comparative Assessment Study (ICAS). This is by contrast a purely analytical exercise, considering both the thermal-hydraulic and the fracture mechanical aspects in the integrity assessment of a 4-loop RPV under loading due to emergency cooling. This has just started, with a workshop scheduled in Paris in June 1997.

1. Introduction

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and a series of fracture round robins addressing PTS conditions organised by GRS in Germany and ORNL in the USA.

2. Regulatory practices survey

In the period 1987-1990 PWG-3 carried out a survey of regulatory practices on PTS. The results were issued as a restricted report (1991). The following general observations on the responses from ten countries may be made:

1. Although not formally required to be analysed in all responding countries, PTS is a transient, that in one way or another is taken into account by all countries.
2. Different approaches and FM-Analyses methods are used world-wide to assess the consequences of PTS
3. The only country that has codified the PTS issue in detail is the USA (since July 23 1985). This is stipulated in 10 CFR Part 50 § 50.61 and Regulatory Guide I.~54
4. It should also be noted that a very detailed code of regulations, rules and guides for PTS comparable to those of the USA does not exist on an official basis in other countries.
5. Since "Ageing" plays an important role in PTS, it is obvious that the main concern is related to "old" plants
6. It is also noted that for countries with a great number of plants the generic approach is certainly of advantage, whereas for countries with only a few plants a case by case approach seems to be appropriate.
7. If the plants as they are, cannot satisfy the requirements and criteria, it seems clear that system changes and other measures, where feasible, will be taken to satisfy the requirements.
8. Although probabilistic analysis is not yet generally accepted, it has been used by most countries to a varying degree, either as the main analysis, as a supplemental analysis or as an aid in PTS-Analysis e.g. in choosing the transient to be analysed.
9. The Screening Criterion as it is being applied in the USA is only accepted by 3 countries : Belgium, Japan and Spain. The reasons why this screening criterion is not accepted by several countries, could be the following :
 - a) PTS is considered to be an important transient that has to be included in the specific integrity analyses of the RPV
 - b) A Database specific to US-PWR has been used
 - c) The use primarily of generic embrittlement data (calculated) instead of specific data from the surveillance program
 - d) Only a temperature criterion is employed without also considering the specific "USE" at hand, although this is required in Appendix G of 10 CFR 50
 - e) The uncertainty in NDT-Temperature determination (old ASTM E-208 practice, which has been revised in 1984, pertaining to the deposition of the crack starter weld)
 - f) The surveillance programs of old plants (before ca. 1968) mostly contain only longitudinal Charpy-V specimens instead of transverse specimens. Transverse specimens are presently required for the RTNDT and the RTNDT-shift determination.

3. FALSIRE I

3.1 Description

The Project for Fracture Analysis of Large-Scale International Reference Experiments (Project FALSIRE) was created by the Fracture Assessment Group (FAG) of Principal Working Group No. 3 (PWG/3) of the Organisation for Economic Co-operation and Development (OECD)/Nuclear Energy Agency's (NEA's) Committee on the Safety of Nuclear Installations (CSNI). Motivation for the project was derived from recognition by the CSNI-PWG/3 that inconsistencies were being revealed in predictive capabilities of a variety of fracture assessment methods, especially in ductile fracture applications. As a consequence, the CSNI/FAG was formed to evaluate fracture prediction capabilities currently used in safety assessments of nuclear components. Members are from laboratories and research organisations in Western Europe, Japan, and the United States of America (USA). On behalf of the CSNI/FAG, the US Nuclear Regulatory Commission's (NRC's) Heavy-Section Steel Technology

(HSST) Program at the Oak Ridge National Laboratory (ORNL) and the Gesellschaft für Anlagen- und Reaktorsicherheit (GRS), Köln, Federal Republic of Germany (FRG) had responsibility for organisation arrangements related to Project FALSIRE. The group is chaired by H. Schulz from GRS, Köln, FRG.

To meet its objectives, the CSNI/FAG planned an international project to assess various fracture methodologies through interpretative analyses of selected large-scale fracture experiments. A number of large-scale fracture tests have been performed in recent years in several countries, and an even larger number of organisations have become cognisant of them and employed test results in attempts to verify analytical methods. A survey of large-scale experiments and related analyses was given at the first meeting of the group in May 1988 at Stuttgart (FRG). Priority was given to thermal-shock experiments to include combinations of mechanical and thermal loads. The reference experiments that were selected by the CSNI/FAG at their second meeting in August 1989 at Monterey, California (USA), for detailed analysis and interpretation are given in Table 1.

Before the 1989 Monterey meeting, the CSNI/FAG established a common format for comprehensive statements of these experiments, including supporting information and available analysis results. These statements formed the basis for evaluations that were performed by an international group of analysts using a variety of structural and fracture mechanics techniques. A 3-day workshop was held in Boston, Massachusetts (USA), during May 1990, at which all participating analysts examined these evaluations in detail. Organisations that participated in the workshop are listed in Table 2.

The experiments used in Project FALSIRE were designed to examine various aspects of crack growth in reactor pressure vessel (RPV) steels under pressurised thermal shock (PTS) loading conditions. These conditions were achieved in three of the experiments by internally pressurising a heated cylindrical vessel containing a sharp crack and thermally shocking it with a coolant on the inner (NKS-3 and -4) or outer [PTSE Experiment (PTSE)-2] surface. In the series of spinning cylinder (SC) experiments, a thick cylinder with a deep crack on the inner surface was thermally shocked with a water spray while simultaneously spinning the cylinder about its axis in a specially constructed rig. The Japanese Step B test used a large surface-cracked plate subjected to combined mechanical loading of tension and bending, co-ordinated with a thermal shock of the cracked surface to model PTS loading conditions. Data from the experiments provided the CSNI/FAG problem statements included pretest material characterisation, geometric parameters, loading histories, instrumentation and measured data [e.g., temperature and strains, crack-mouth opening displacements (CMODs), and crack-growth histories]. The analyses have concentrated on the phase of ductile crack growth having a range of 1 to 6% of the initial crack depth in the NKS, SC, and Step B tests. In case of PTSE-2A, the first phase of ductile crack growth was ~35% of the initial depth; in PTSE-2B, the corresponding crack growth was 9% of the initial depth.

Based on the CSNI/FAG problem statements, 37 participants representing 19 organisations performed a total of 39 analyses of the experiments. A breakdown of the number of analyses contributed by the participating institutions and of the analysis methods applied to each experiment is given in Table 3. The analysis techniques employed by the participants included engineering methods (R6, GE/EPRI estimation scheme, DPFAD) and finite-element methods. These techniques were combined with applications of JR methodology and the French local approach. The finite-element applications included both two- and three-dimensional models, as well as deformation plasticity and incremental thermo-elastic-plastic constitutive formulations. Crack-growth models based on nodal release techniques were used to generate both application- and generation-mode solutions for several of the experiments.

For each of the experiments, analysis results provided estimates of variables including crack growth, CMOD, temperatures, strains, stresses, and applied J and K values. Conditions of crack stability and instability were identified in the experiments. Where possible, computed values were compared with measured data.

3.2 Conclusions

Based on results from the Project FALSIRE Workshop, several observations can be made concerning predictive capabilities of current fracture assessment methodologies as reflected in the large-scale experiments described in the previous chapters.

Generally, these experiments were designed to evaluate fracture methodologies under prototypical combinations of geometry, constraint, and loading conditions. However, because complexities of the experiments do not permit a clear separation of the effects of the many variables involved, it has proved difficult to interpret the analyses of those transients for which expected results were not achieved.

Modelling requirements for the experiments incorporate history-dependent mechanical, thermal, and body force loadings; temperature-dependent material and fracture-toughness properties; specially designed materials; residual

stress states; and 3-D effects. Interactions of both cleavage and ductile modes of fracture must be modelled for certain transients. For these reasons, it could be anticipated that comparisons of analysis predictions with available structural data from the experiments would yield results that vary significantly.

The discussion of the analysis results has focused on the discrepancies of the finite-element results and on comparisons with the estimation scheme analyses. Many effects from the comparative assessment of the analysis results have not yet been explained, but with this report there is a data base available for further studies on separate effects. The structural mechanics behaviour of the test specimens could be approximated well in case of the NKS experiments, but not in PTSE-2 (see Table 4). In the SC tests, structural mechanics results could not be compared with experimental measurements. The largest differences are seen to occur in the PTSE-2A transient. On the other hand, recent evaluations indicate a strong axial dependence of measured CMOD values, which has to be investigated further in connection with the loading assumptions. The restrictions in some finite-element codes to input stress-strain curves only by bilinear approximations produced large scatterbands in the results (CMOD and J-integral). The measured onset of yield is very low (70 MPa) compared with the engineering yield stress (255 MPa) quoted for the vessel insert. The value used in the calculations ranges from 200 to 495 MPa, dependent on whether the small strain or the larger strain region of the stress-strain curve is approximated well. Furthermore, an increase in $\sigma_{0.2}$ of 50% from the vessel insert after transients A and B has been found. All the analyses assumed material and physical properties to be independent of temperature because corresponding measured data were not available. These factors may have contributed to the large underestimation of the measured CMOD in the experiment. These analysis results highlight the importance of obtaining high-quality material properties and structural response data (CMOD, strains, etc.) from the experiments to model structural behaviour of the specimen before performing fracture mechanics evaluations. In particular, variables must be carefully selected and reliably measured to provide a minimum set of data for validating these structural models. This requirement was not uniformly achieved in all of the large-scale experiments examined in the Project FALSIRE workshop.

In applications of JR methodology based on small specimen data, all analyses correctly distinguished between stable crack extension and ductile instability conditions for each experiment. These include both ES and detailed FE analyses. However, as a technique to predict crack extension, JR methodology was partially successful in some cases (NKS experiments) but not in others (PTSE-2 and SC experiments). Fracture assessments based on CT specimens overestimated stable crack growth in the case of NKS-4, SC-I and -II, and Step B PTS because the crack resistance in the large-scale test specimens is greater than predicted by small specimens (e.g., CT-25). SC-I and -II fracture results show that crack extension can be described quite well with the J-integral and the JR-curves of the large-scale test specimen. In PTSE-2A, the first phase of stable crack extension is underestimated because the crack loading also represented in CMOD is underestimated. Furthermore, differences between pretest characterisation data and post-test in situ data for material and fracture toughness properties gave rise to questions concerning whether JR curves from CT specimens were representative of the flawed region of the vessel. None of these temperature-dependent JR curves were consistent with all phases of ductile tearing observed in PTSE-2. It should be pointed out that the PTSE-2A transient included load-history (i.e., warm-prestressing) effects that were not incorporated into the JR methodology. A summary of the fracture results is given in Table 5.

The substantial differences between fracture toughness curves generated from the SCs and from CT specimens focused attention on other factors. These included the possibility that crack-tip behaviour in the SC is not characterised by a single parameter fracture mechanics in terms of J. Alternative criteria under consideration include two-parameter models in which K or J is augmented by the next higher order T or Q in the series expansion of the stresses around the crack tip. Other measures considered in dealing with the transfer of small specimen data to large structures include the stress triaxiality parameter q, which is proportional to the ratio of hydrostatic to effective stress. The results indicate that q on the ligament is not sensitive enough to represent changes of stress triaxiality responsible for geometry effects on crack resistance. The temperature dependence of the crack resistance measured with CT specimens shows an increase with increasing temperature only for NKS-4 material but a decrease in the cases of PTSE-2 and Step B PTS. Also, the Local Approach has been applied as an alternative to JR methodology for performing fracture-toughness evaluations in the case of NKS-3. For the SCs, clarification of the initial stress state in front of the crack tip (caused by cyclic fatiguing) may be an important consideration.

A final report was prepared on FALSIRE I, which highlighted conclusions and recommendations derived from interpretations of the comparative fracture assessments. These assessments confirmed the importance of adequately modelling structural behaviour of the test specimens before performing fracture mechanics evaluations. Applications of various single-parameter fracture methodologies were found to be partially successful in some cases, but not in others. Some analyses were performed from a safety assessment perspective to achieve a conservative prediction; the results tended to show significant deviation from experimental data and best-estimate analyses. Proposals for follow-on work in the context of a FALSIRE II project were included in the report.

4. FALSIRE II

4.1 Description

It was proposed that the follow-on FALSIRE II project should emphasise experiments that focus on behaviour of relatively shallow cracks subjected to combined thermal and mechanical loading in the transition temperature region. If possible, experiments for cracks showing two stages of extension (e.g., stable crack extension followed by stable extension) should be included. Investigations of crack initiation and extension in connection with clad surfaces were also proposed. In 1993, these criteria were utilised by the CSNI/FAG to select a new set of experiments for the FALSIRE II project.

The experiments utilised in FALSIRE II examined various aspects of the cleavage fracture process in RPV steels for a wide range of material crack geometries, and constraint and loading conditions. PTS loading transients were applied in three of the experiments by internally pressurising a heated vessel containing a sharp crack and thermally shocking it with a coolant on the inner surface (NKS-5 and 6, from Materialprüfungsanstalt, Stuttgart, Germany) or outer surface (PTS-I/6, from Central Research Institute of Structural Materials, Russia, VTT Manufacturing Technology and IVO International Ltd., Finland). In the spinning cylinder experiment (SC-4, from AEA Technology, United Kingdom), a thick cylinder with two deep cracks on the inner surface was thermally shocked with a water spray while simultaneously spinning the cylinder about its axis in a specially constructed rig. Clad beams DD2 and DSR3, from Electricite de France) subjected to uniform temperature and uniaxial four-point bending were used to investigate initiation of shallow underclad cracks in the base material. The influence of out of-plane biaxial loading on cleavage fracture toughness of shallow cracks in the transition temperature region was studied using a biaxially loaded cruciform beam (BB-4, from Oak Ridge National Laboratory, U.S.). Data provided in the CSNI FAG problem statements for these experiments included pretest material characterisation, geometric parameters, loading histories, instrumentation, and measured results for temperatures, strains; crack-mouth-opening displacements (CMODs), and crack extension histories

More than 30 participants representing 22 organisations from 12 countries performed a total of 45 analyses of the 7 reference fracture experiments in FALSIRE II. These organisations took part in the FALSIRE II Workshop held during November 1994, in Atlanta, Georgia, to assess these analyses and the relevant fracture methodologies. The analysis techniques employed by the participants focused primarily on finite-element methods; these techniques were combined with single- or dual-parameter constraint methodologies for fracture mechanics assessments. A list of Special Requirements (SRs) was prepared for each reference experiment and distributed to participating analysts in advance of the workshop. The SRs comprise a set of quantities that characterise the thermal/structural behaviour of the test specimens and the fracture behaviour of the cracks. Prior to the workshop, participants provided the Organising Committee (OC) with analytical results for the parameters include in the SRs. For each of the experiments, these parameters included temperature, CMOD, strains, stresses, crack loading in terms of J-integral and stress intensity factor, as well as various constraint parameters. Also, conditions of crack initiation were identified in the experiments, and where possible, computed values of parameters were compared with measured data. The analysis results and measured data have been compiled into an electronic data base. For each experiment, the results are available in 40 to 50 comparative plots generated from the data base using a special-purpose evaluation program.

The report provides an overview of the comparative assessments, which are based predominantly on the fracture mechanics results compiled from discussions of each reference experiment at the FALSIRE II Workshop and from the analysis results data base. A comprehensive selection of comparative plots from the data base serves as the focal point for discussion of these assessments. Analyses provided by organisations participating in FALSIRE II are identified by an alphanumeric code to preserve the public anonymity of the contributor.

4.2 Conclusions

Some conclusions drawn from the FALSIRE II Workshop and from an evaluation of the analysis results data base follow:

- The temperature distributions in the specimens loaded by thermal shock generally were approximated with high accuracy and small scatter bands. Discrepancies appeared only for limited time periods during the transients and could be traced to different assumptions concerning the heat transfer coefficients
- Structural response (i.e., CMOD, strains, etc.) of the test specimens was predicted reasonably well from best estimate analyses. This outcome represents a significant change compared with some of the results achieved in

FALSIRE I. In part, the change reflects a more widespread recognition that the assumptions adopted to ensure failure avoidance in safety assessments are inappropriate when attempting to predict final failure.

- Discrepancies that appeared in the structural calculations could usually be traced to the assumed material models and to approximations of material properties (i.e., stress-strain data).

- Calculations of fracture parameters such as J or K , and the parameter CMOD generally showed small scatter bands. Discrepancies could be traced to the differences between elastic and elastoplastic approaches or assumptions concerning material properties.

- The K_I vs temperature diagram combined with material data curves describing fracture toughness vs temperature were determined to be useful for fracture assessments of crack behaviour. Crack initiation could be predicted from a single fracture parameter (K , J , etc.), reasonably well in tests where initiation was not significantly affected by constraint effects.

- When constraint effects become significant, a single parameter is not sufficient to characterise crack-tip conditions, and a second parameter must be introduced into the fracture model. Candidate constraint parameters employed by the participating analysts include Q -stress, stress triaxiality h , local approach of cleavage fracture, and a strain based function of the plastic-zone width in the crack plane. In the SC-4 experiment, constraint effects were quantified using the Q -stress and, to a more limited degree, the triaxiality parameter h . In PTS-I6 and NKS-5, the parameter h showed indications of loss-of-constraint, while the Q -stress was not evaluated. Finally, in BB-4, a shallow crack effect was demonstrated by the computed Q -stress, which indicated a loss-of constraint associated with the departure of in-plane stresses from reference small-scale yielding conditions

- The Q -stress and other stress-based constraint methodologies have been applied successfully to correlate constraint conditions for in-plane (or uniaxial) loading conditions. However, prior studies have determined that stress-based constraint methodologies (such as the Q -stress) are not sensitive to changes in constraint conditions due to changes in out-of-plane biaxial loading. The plastic zone width was employed successfully to correlate changes in constraint conditions for shallow cracks subjected to changes in out-of-plane biaxial loading ratios. Further investigations are necessary to clarify whether one parameter can be recommended or a set of parameters should be computed to assess constraint effects.

- Additional toughness data measured in the transition temperature region using a range of specimen geometries and constraint conditions are required to validate the predictive capabilities of cleavage fracture methodologies that incorporate constraint effects

Simulations of crack growth and crack arrest events (e.g., in NKS-6) showed large uncertainties among the applied fracture methods.

Additional data concerning the HAZ fracture toughness are necessary for further refinement of analyses of shallow subclad flaws.

- Almost all participants elected to use the finite-element method in addressing the problems of FALSIRE II. This represents a marked change from FALSIRE I, which included applications of a number of different estimation schemes. The detailed information that participants were asked to provide from the analyses in FALSIRE II encouraged the use of finite-element methods over estimation schemes. It should not be inferred from the outcome of FALSIRE II that detailed finite-element analyses are always the preferred or necessary technique for structural integrity assessments.

Regarding the original objective of the CSNI/FAG to evaluate the predictive capabilities of fracture assessment methods for nuclear components it has been shown in the frame of the FALSIRE project that crack initiation and ductile crack growth as well as cleavage fracture in large scale experiments can be predicted by fracture methods based on the stress intensity factor calculated by the J -integral within tolerable scatterbands. In some cases which are characterised by strong differences in stress triaxiality between the large scale test specimen and the small scale fracture test specimens used to measure fracture toughness or fracture resistance the methods predict crack initiation at smaller loads or earlier in time. Improvement can be achieved if constraint parameters are included in the methodology of fracture assessment. The attempts to predict crack arrest resulted in large scatterbands which indicate that more effort has to be put on this subject.

5. International Comparative Assessment Study (RPV PTS ICAS)

For future work an International Comparative Assessment Study (ICAS) for a reactor pressure vessel (RPV) under pressurised thermal shock (PTS) was proposed based on the experience achieved in FALSIRE. The RPV ICAS Project is planned for the benefit of organisations concerned with evaluation of fracture methodologies used in RPV integrity assessments. This project is motivated in part by the strong interest expressed by participants in Phases I and II of FALSIRE to proceed with further evaluations of fracture mechanics analysis methods.

The integrity assessment of a RPV is a multi-step analysis including the selection of transients, thermo-hydraulic calculations, structural analysis including fracture assessment based on specified material properties. Therefore, participation in a comparative study concerning RPV integrity assessment on RPVs including structural and fracture, as well as thermal- hydraulic aspects, has been called for.

At this stage the vessels are proposed to be Western type 4-loop RPVs with cladding. Country-specific concerns are of interest. A detailed task matrix is provided with transient thermo-mechanical loading cases due to loss of coolant accidents to be analysed with different assumptions concerning the cooling conditions. Asymmetric cooling conditions will be considered as well as axisymmetric conditions. A set of different cracks assumed at the position of near-core welds will be proposed. The primary focus of the analyses is proposed to be on the behaviour of relatively shallow cracks (underclad and clad-through) under PTS loadings. In the frame of parametric studies different thicknesses of the cladding as well as the influence of residual stresses, the level of yield stress and the influence of elastic versus elastic-plastic approaches will be considered among others. Furthermore, probabilistic tasks are given to analyse the conditional probability of crack initiation and crack penetration.

Concerning the determination of RPV loading conditions due to loss of coolant accidents and the importance for the RPV integrity assessment, special emphasis is given to the interdisciplinary aspects. Especially the calculation of the fluid temperature and the heat transfer to the structure, with consideration of fluid-fluid mixing as well as steam condensation by thermo-hydraulic analysis techniques, will be of interest.

The problem statement is divided in 3 task groups (deterministic, probabilistic and thermal hydraulic) with several main tasks. Additionally parametric studies are proposed to investigate the influence of certain parameters on the results of the main tasks. For the tasks input was given by Siemens, ORNL, EDF and GRS.

The proposed partly fictitious RPVs refer to country specific concerns. In the deterministic task group a typical vessel of German design is proposed. The cladding thickness is proposed to vary in the range of designs used in USA, France, Germany and Russia. The postulated loading transients refer to a small break loss of coolant accident typical for US PWR plants and transients due to leaks with different size typical for German PWR plants. In the probabilistic task group a typical vessel of US construction is proposed loaded by specific transients. In the thermal hydraulic task a fictitious vessel is proposed with wall thickness of a typical German RPV but with internal measurements of the Upper Plenum Test Facility (UPTF) vessel in Mannheim (Germany).

The problem statement was distributed in December 1996. Intermediate results and progress of the work will be discussed at a Workshop in June 1997. Participants in the ICAS will be requested to submit the final analysis results during September 1997 with presentation and discussion on a Workshop scheduled for February 1998. A report will be issued after completion of the analysis results.

References

1991 NEA/CSNI/R(91)7 Regulatory practices on PTS: results of an international survey

1994 NEA/CSNI/R(94)12, OCDE/GD(97)24, GRS-108, NUREG/CR-5997, ORNL/TM-12307 CSNI project for Fracture Analyses of Large Scale International reference Experiments (Phase I) comparison report

1996 NEA/CSNI/R(96)1, OCDE/GD(96)187, GRS-130, NUREG/CR-6460, ORNL/TM-13207 FALSIRE Phase II comparison report

Table 1 Large-scale fracture experiments analysed in CSNI/FAG Project FALSIRE

Experiment	Organisation	Testing country
NKS-3	Materialprüfungsanstalt (MPA), Universität Stuttgart	FRG
NKS-4	MPA, Universität Stuttgart	FRG
PTSE-2A	ORNL	USA
PTSE-2B	ORNL	USA
SC-I	Atomic Energy Authority (AEA), Risley	UK
SC-II	AEA, Risley	UK
Step B PTS	Japan Power and Engineering Inspection Corporation (JAPEIC)	Japan

Table 2. Organisations participating in the Project FALSIRE Workshop, Boston, May 1990

AEA	UK
AS/Electric Power Research Institute (EPRI)	U.S.A.
Babcock and Wilcox (B&W) Nuclear Services	U.S.A.
Battelle Columbus Division	U.S.A.
Central Research Institute of Electric Power Industry (CRIEPI)	Japan
Centre D'Etudes Nucleaires de Saclay	France
Combustion Engineering (CE)	U.S.A.
Electricite de France (EDF)	France
Fraunhofer Institut für Werkstoffmechanik (IWM)	FRG
GRS	FRG
JAPEIC	Japan
Korea Institute of Nuclear Safety	Korea
MPA, Universität Stuttgart	FRG
Mitsubishi Heavy Industries (MHI)	Japan
National Committee for Nuclear and Alternative Energies (ENEA-DISP)	Italy
Nuclear Electric	UK
OECD/NEA	France
Nuclear Installations Inspectorate	UK
NRC	U.S.A.
ORNL	U.S.A.
Paul Scherrer Institut	Switzerland
Southwest Research Institute (SWRI)	U.S.A.
Technical Research Centre of Finland (VTT)	Finland
University of Maryland	U.S.A.
University of Tennessee	U.S.A.
University of Tokyo	Japan

Participants: U.S.A. 17, FRG 5, France 4, UK 3, Japan 3.
Finland 2, Switzerland 1, Korea 1, Italy 1; Total 37

Table 3 Summary of Project FALSIRE analysis techniques

NKS-3 (10 analyses)	NKS-4 (6 analyses)	PTSE-2 (8 analyses)	SC-I (6 analyses)	SC-II (8 analyses)	STEP B PTS (1 analysis)
FE, JR	FE, JR	FE, JR	FE, JR	FE, JR	FE, JR
FE; JR, LA	FE, JR	FE, JR	FE, JR	FE, JR	
FE, JR	FE, JR	FE, JR	FE, JR	FE, ES	
FE; JR, LA	FE, JR	FE, JR	ES	FE, ES	
FE, JR	ES, J/T	FE, JR	ES	ES	
FE, JR	ES, R6/1	FE, JR	ES, WF	ES, R6/1	
FE, JR		ES, J/T		ES	
FE, JR		ES		ES	
ES, J/T					
ES, R6/1					

FE = finite-element method

ES = estimation scheme

A1 = analytic solution with numerical integration

A2 = handbook analysis of statically indeterminate model

JR = R-curve approach

J/T = J/tearing modulus approach

LA = local approach

R6/1 = R6 method/option I

WF = weight function method

Table 4 Comparative assessment of structural behaviour in Project FALSIRE reference experiments

	Availability of mechanical properties		Measured structural data		Scatterband of structural analysis	
	T ^b -dependent	T-independent	CMOD)max (mm)	Strains	ΔCMODmax (%) ^c	Strains (%) ^c
NKS-3	X		1.5	X	17	13
NKS-4	X		0.54	X	8	7
PTSE-2A		X	0.9	X	35 ^d	
PTSE-2B		X	1.6	X	21 ^d	
SC-I	X		e			
SC-II	X		e	e		

a Analysis results with wrong boundary conditions or crack assumptions ignored.

b T = Temperature.

c Relative to measured value.

d Underestimation of measured data.

e Some data of crack-tip opening have been provided after evaluation of the analyses.

Table 5 Comparative assessment of fracture behaviour in Project FALSIRE reference experiments

	Availability of crack resistance curves	Measured crack growth Δa (mm)	Scatterband of fracture analyses	
			J (N/mm)	Δa (mm)
NKS-3	CT-25, T = 160/220°C	3.6 (av.)	410-500	3.0-4.8
NKS-4	CT-50, T = 220°C			
	CT-25, T = 160/240/280°C CT-25 (10 mm thick) T = 160°C	1.5b	180-220h	2.0-3.2h
PTSE-2A	CT-25, T = 100/175/250°C	5.1	100-175	1.0-2.5
PTSE-2B	CT-35, T = 290°C	2.8 (averaged)	470-560	3.2-4.2d
SC-I	CT-25 T = 100/175/250°C	3.7	145-225	1.4-2.9
SC II	SC-I test specimen CT-35 T = 150/290°C SC-II test specimen	0.75	200-490	0.2-0.8d

a Analysis results with wrong boundary conditions or crack assumptions ignored

b Deepest point of partly circumferential crack

c Middle of axial crack

d Determined with J' curves of SC test specimen

FRACTURE MECHANICS ASSESSMENT OF SURFACE AND SUB-SURFACE CRACKS IN THE RPV UNDER NON-SYMMETRIC PTS LOADING

by

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ABSTRACT. One of the most severe loading conditions of a reactor pressure vessel (rpv) under operation is the loss of coolant accident (LOCA) condition. Cold water is injected through nozzles in the downcomer of the rpv, while the internal pressure may remain at a high level. Complex thermal hydraulics situations occur and the fluid and downcomer temperatures as well as the fluid to wall heat transfer coefficient at the inner surface are highly non-linear. Due to this non-symmetric conditions, the problem is investigated by three-dimensional non-linear finite element analyses, which allow for an accurate assessment of the postulated flaws. Transient heat transfer analyses are carried out to analyze the effect of non-symmetrical cooling of the inner surface of the pressure vessel. In a following uncoupled stress analysis the thermal shock effects for different types of defects, surface flaws and sub-surface flaws are investigated for linear elastic and elastic-plastic material behavior. The obtained fracture parameters are calculated along the crack fronts. By a fast fracture analysis the fracture parameters at different positions along the crack front are compared to the material resistance. Safety margins are pointed out in an assessment diagram of the fracture parameters and the fracture resistance versus the transient temperature at the crack tip position.

1. Introduction

Strip and plume cooling during a loss of coolant accident of the rpv leads to a locally increased axial stress distribution compared to the axisymmetric cooling. The higher axial stresses makes the circumferential crack more relevant for the safety assessment. The transient temperatures and stresses in the rpv-wall are calculated by means of 3D finite element (FE) calculations and have to be performed for the most severe loading condition. The relevant transient is selected by according precalculations with 3D FE without crack and the chosen one is handled by an elastic-plastic 3D analysis with the according crack configuration. The safety assessment deals with a safety margin against the material toughness curve or the allowable transition temperature. In this contribution the differences between surface and sub-surface cracks of a cladded rpv are demonstrated and the results are compared to the procedure given in the German guideline for a postulated surface flaw ignoring the cladding.

2. Thermal hydraulics background

The rpv is initially loaded by internal pressure at normal operation, while the emergency core cooling (ECC) due to the postulated thermal shock takes place. This means, the downcomer and the cold legs are filled with water and the ECC-water injection into the cold legs occurs. The complicated phase of fluid-fluid mixing of

ECC-water and ambient hotter water in the cold legs leads to a plume cooling of the rpv wall, Figure 1. The mixing of the colder plume water with hotter ambient water on the way to the lower plenum results in an increase of plume width and temperature. The downcomer water temperature transient is a superposition of a global cooldown and colder plumes. Inside the plume the temperature and heat transfer coefficient are azimuthal Gaussian distributed. A plume interaction of ECC-water injection between neighboring cold legs takes place, Figure 2, and therefore, in the numerical simulation of the most severe transient only one cold leg is considered along the rpv circumference, which leads to a conservative assumption concerning resulting stresses [1]. The outcome of the complex thermal hydraulics calculations are the transient temperature and heat transfer coefficient of the downcomer and the plume. Typical examples are given in Figure 3 for a German 3 loop rpv - the geometry is shown in Figure 4 - and an Eastern type reactor, Figure 5.

3. Fracture mechanics idealization

In the above Figure 6 the idealization of the model is demonstrated, where surface as well as sub-surface cracks are modeled.

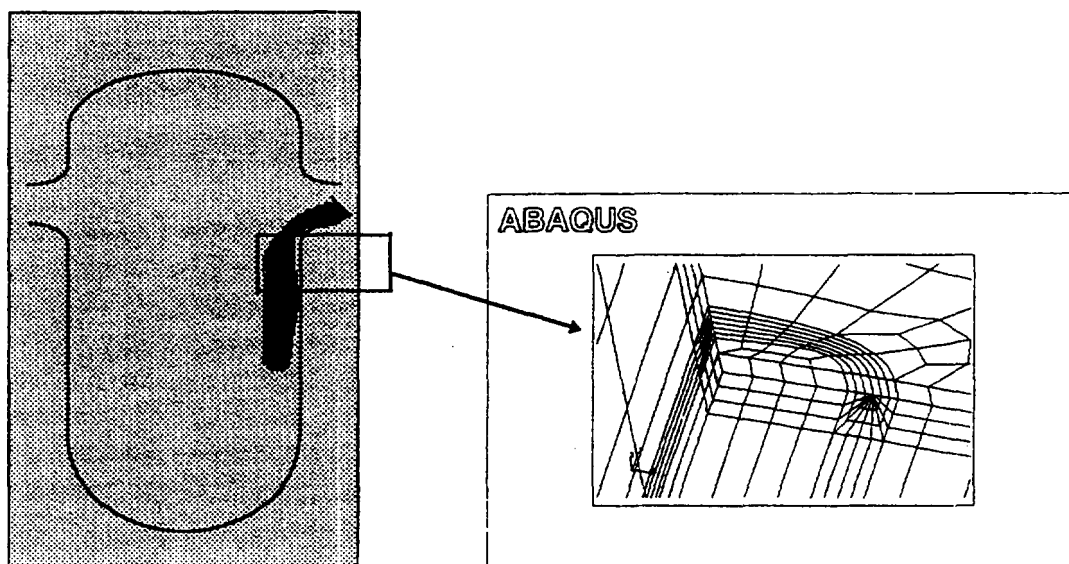


Figure 6: Idealization of the cracked region

The finite element (FE) calculations are performed with the program system ABAQUS 5.5 [2] on a UNIX Hewlett Packard workstation. The geometry of the Siemens type rpv under investigation is shown in Figure 4, whereby the interesting part of the pressure vessel (45°-180° of the circumference) is modeled as a part of the whole structure. The axial height of the downcomer is varied over a range from the whole structure to a section of 500 mm to study influences.

The cylinder consists of ferritic base metal 22 NiMoCr 3 7 with a thickness of 243 mm and is clad with a 6 mm thick cladding of austenitic stainless steel. The material data like Young's modulus, stress-strain curve, thermal expansion coefficients, conductivity and specific heat are included in the model temperature dependent for these two materials, in the thermal as well as in the stress analysis.

3.1 Meshing

Due to the very complex loading situation and for good performance values of the HP workstation a 3D-model is appropriate. So the meshing is done by 20 node bricks with 3*3*3 integration order. In the vicinity of the crack front for estimation of J-integral values collapsed elements with a $1/r$ singularity are adequate. Around the crack front the collapsed elements have multiple node points. Some tests with non collapsed elements show discontinuities in the shape of J-integral curve with the crack angle. At the intersection of cladding to base metal a flaw is postulated with a size of 10 mm in thickness and 60 mm in circumferential direction.

3.2 Boundary conditions and loading

The main loading of the idealized pressure vessel is a thermal shock at the inside of the warm vessel wall. The initial temperature of base material and cladding is 565 K and the initial pressure is the normal loading at operation. During the thermal shock the initial pressure decreases. At the injection nozzles the cooling conditions on the inner surface of the modeled part are realized by an ABAQUS *FILM condition with appropriate values for thermal hydraulics, the time dependent sink temperature and time dependent film coefficient, given in Figure 3. In circumferential direction a Gaussian distribution of the cold water plume is considered for the inner surface convection. The outer surface is supposed to be adiabatic.

3.3 Uncoupled thermal and stress analysis

The thermal and stress analysis will be treated as an uncoupled problem, meaning first the temperature field is calculated and from these results stresses, strains and displacements are determined. Due to the transient load history of the ECC case, the thermal analysis is a transient heat transfer analysis. on the basis of the transient temperature fields, stress analyses are performed either for linear elastic and for elastic-plastic material behavior, to investigate the influence. For linear elastic analysis theory of linear geometry (small deformations) and for elastic-plastic analysis theory of non linear geometry (large deformations) is applied. Initially a stress free condition is supposed at 565 K.

4. Results

As a first result, the displacements of the global structure and the cracked region are given in Figure 7. Due to the multiple points along the crack front, the crack opens in the according manner. The whole structure of the rpv is contracted at the end of the transient. An interesting result is the course of the fracture mechanics parameter J-Integral, which is calculated by the change of potential energy at virtual crack extension, Figure 8. The crack front angle starts at the interface of base metal to cladding and the values are compared for the same crack geometry, where the surface crack depth is longer by the thickness of the cladding. At both transient times, the sub-surface flaw under linear elastic material assumptions results in lower J values compared to elastic-plastic material conditions. At the interface to the cladding some discontinuities are observed by the temperature jump due to the different material properties of base metal and cladding. The higher the accumulated plastic strains, the

higher the discontinuities. While the elastic-plastic calculations leads always to higher values of the sub-surface flaw, this is not true for the surface flaw under consideration. At long transient times, the assumption of linear elastic material behavior leads to overconservative results.

The obtained fracture mechanics values are compared in the safety assessment diagram to the material toughness and to the results of the analytical calculations according to [3] with the plastic zone correction of [4], Figure 9. All the performed analyses up to now (Siemens and Eastern type rpvs) reveals, that the former procedure of calculating the temperature and stresses by linear elastic FE models and afterwards the stress intensity factors by analytical formulas, ignoring the stresses in the cladding, covers the stress intensities of the analysis of a real sub-surface flaw (3D FE under elastic-plastic material behavior).

5. Conclusions

The integrated concept of rpv safety assessment requires the knowledge of the exact material properties at end of life condition (or at the time of interest) and a reliable calculation of the relevant loading path. In this report, the results of some FE calculations of a Siemens type rpv under pressurized thermal shock are shown, which demonstrate the influence of the crack assumption and the material behavior. The so called underclad crack, which is a semi-elliptical sub-surface crack in the interface of cladding and base metal, is in any case covered by a surface crack of the same depth, independent of linear elastic or elastic plastic material behavior for the loading cases under investigation. The former procedure of an underclad crack being simulated by a surface crack with the cladding ignored, is also conservative, that means results in higher fracture parameters than the real sub-surface flaw calculated by FE. But for all investigated sub-surface flaws it could be shown, that only realistic, that means elastic-plastic material behavior leads to conservative results with respect to the safety margin of the loading path against the material curve.

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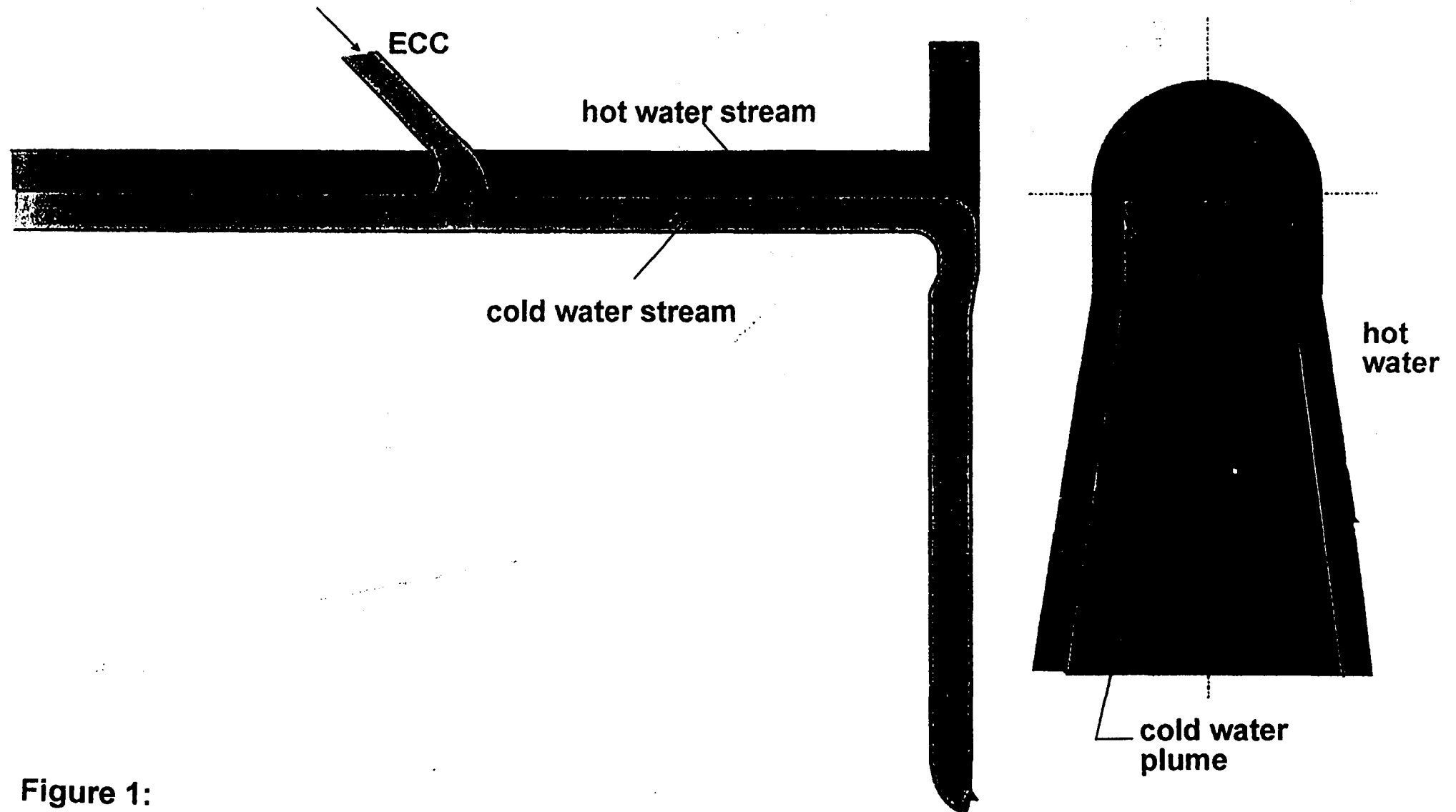


Figure 1:

**Plume Cooling of RPV-Wall:
Fluid-Fluid-Mixing in cold leg and downcomer**

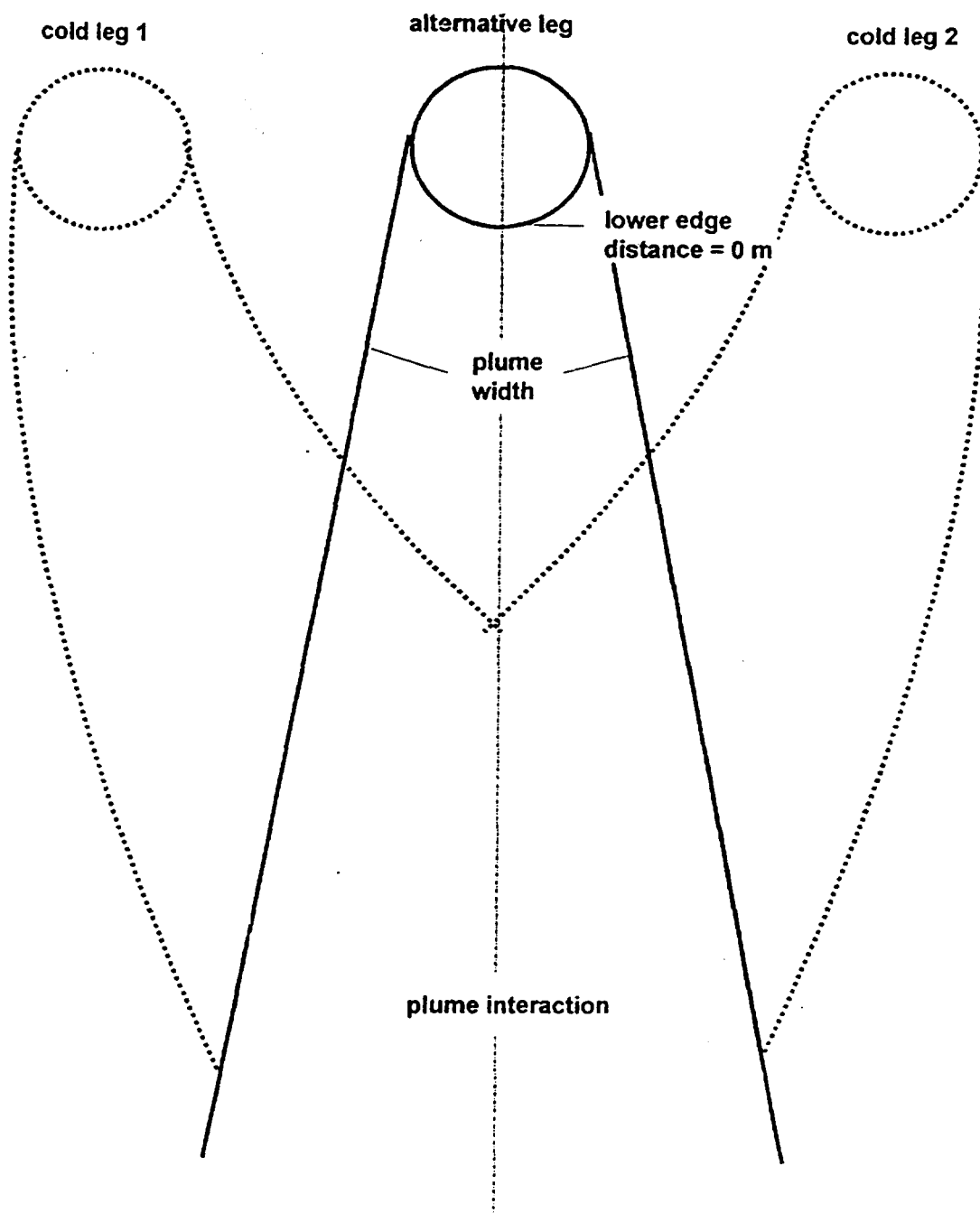


Figure 2: Interaction of neighbouring plumes during cold leg injection

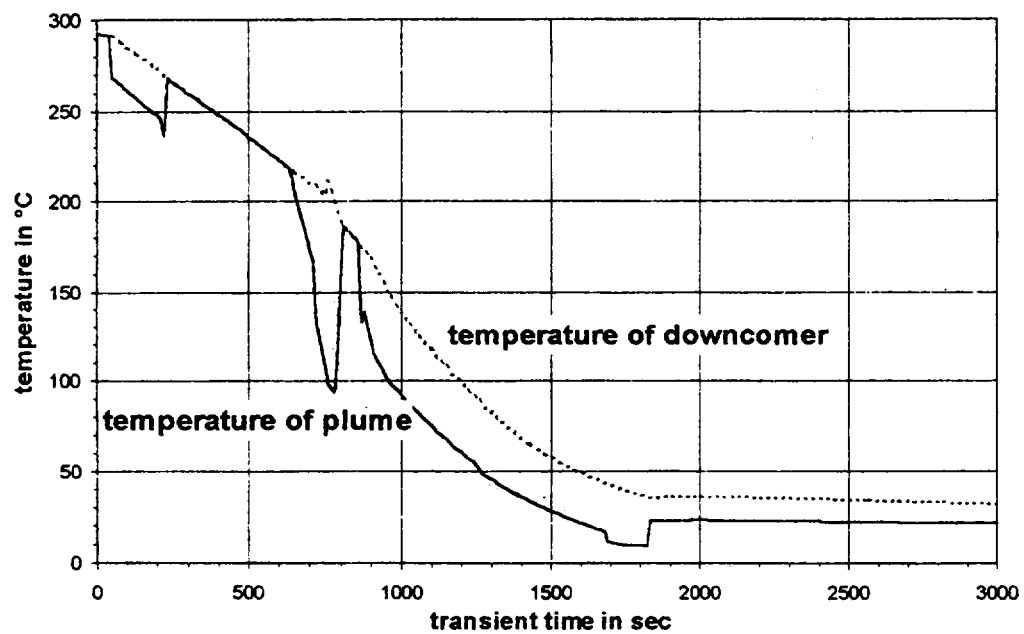
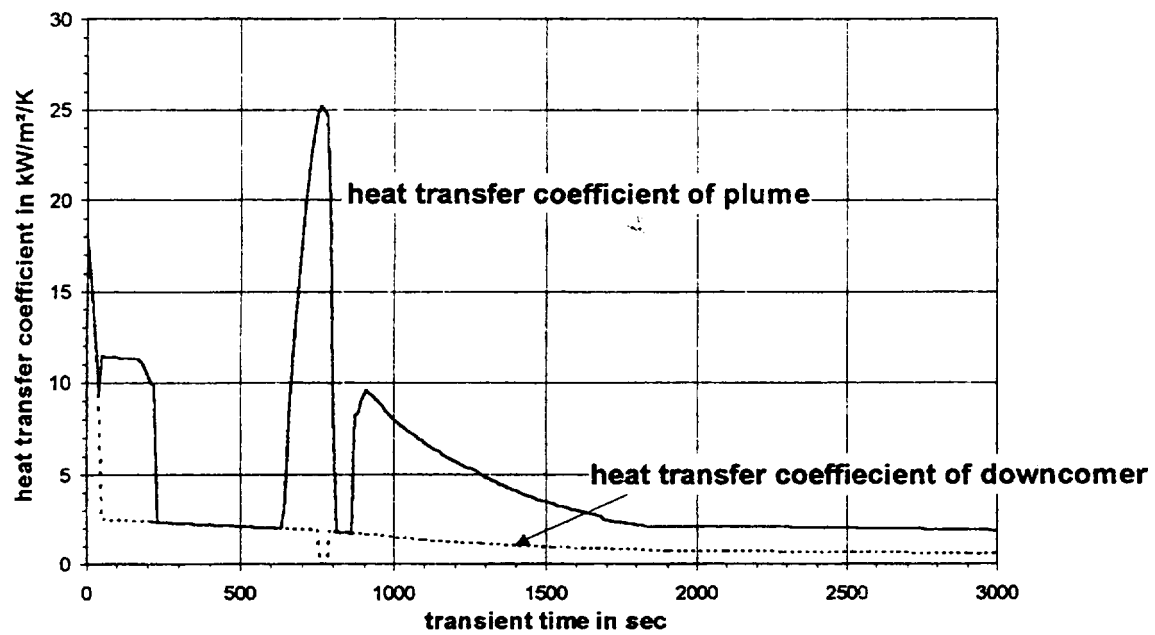


Figure 3: Thermal hydraulics input of temperature and heat transfer coefficient for a Siemens NPP

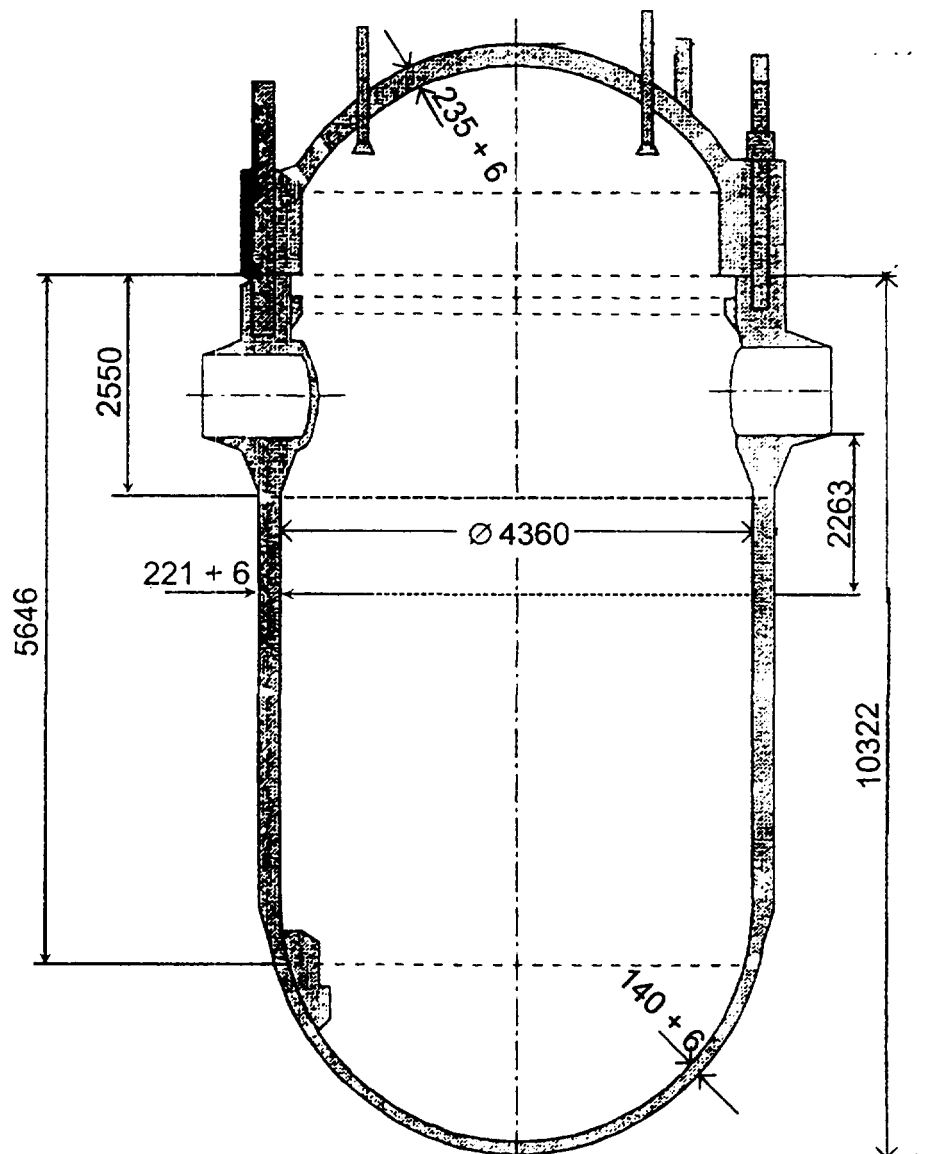


Figure 4: Scheme of RPV German 3 Loop Plant

RDB.DS4
Sch/Kr-01-7

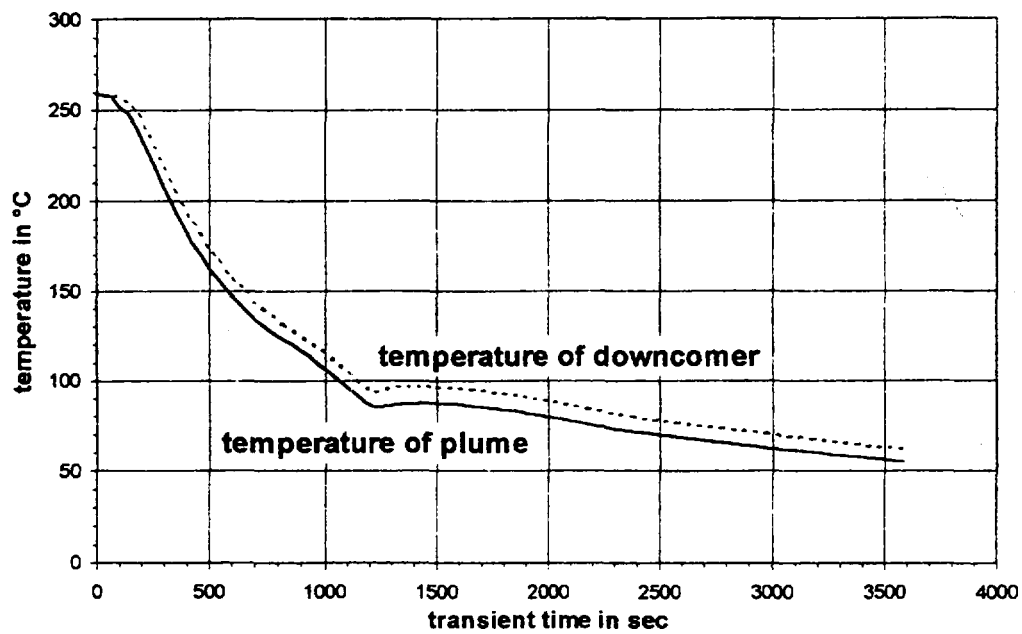
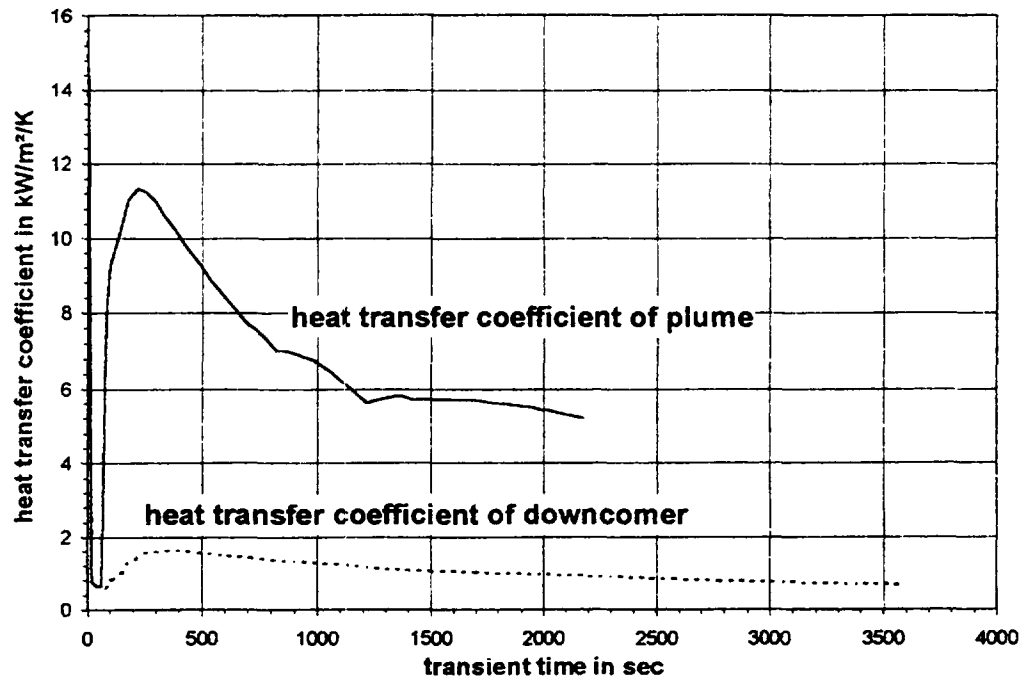


Figure 5: Thermal hydraulics input of temperature and heat transfer coefficient for an Eastern type of NPP

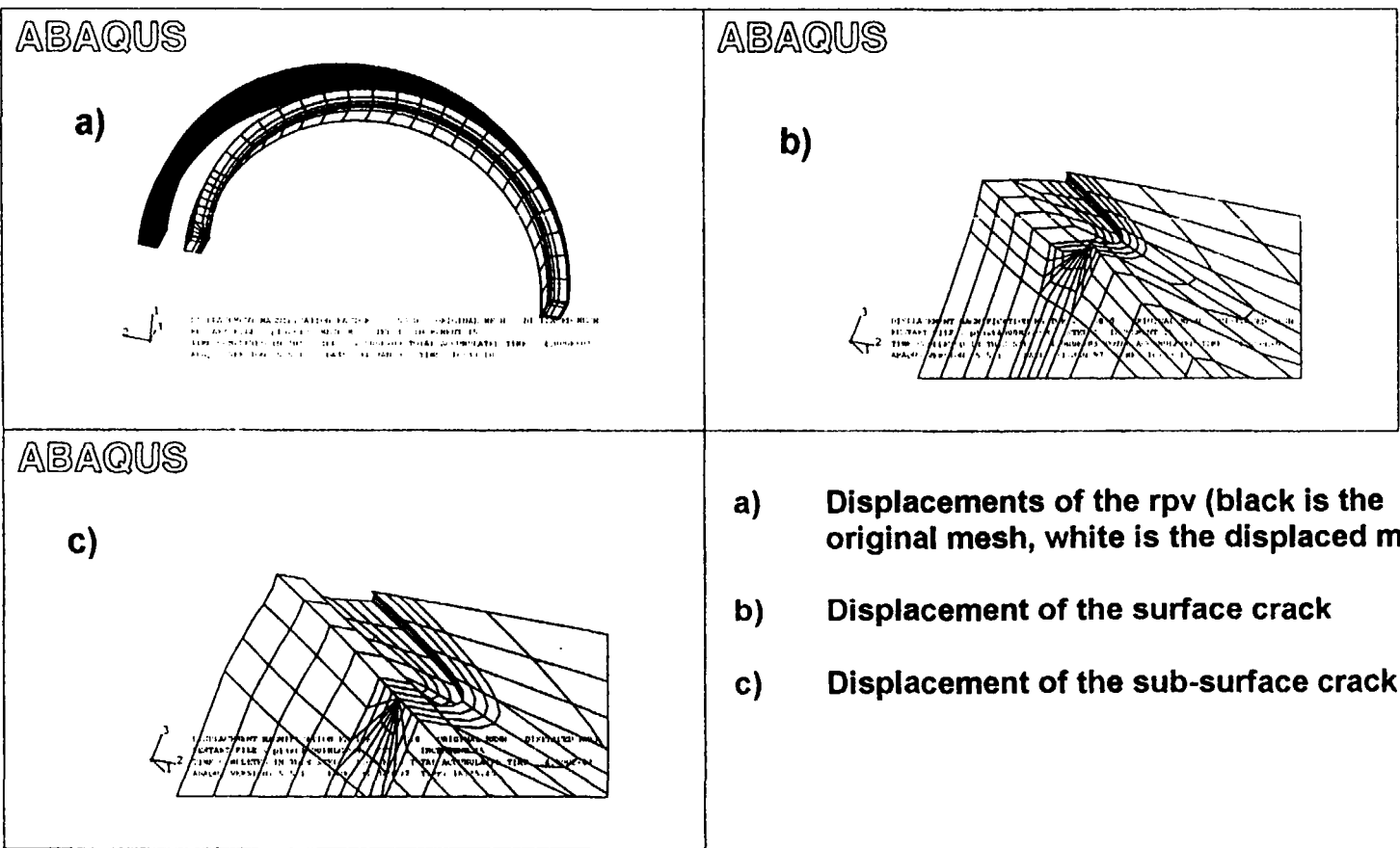
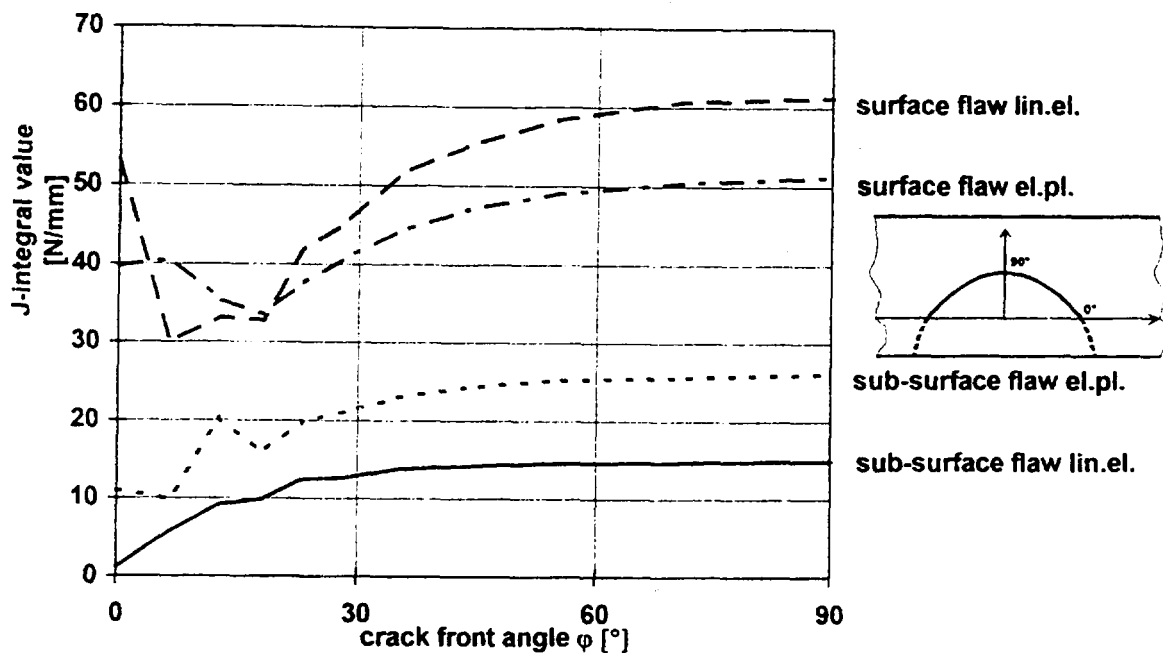
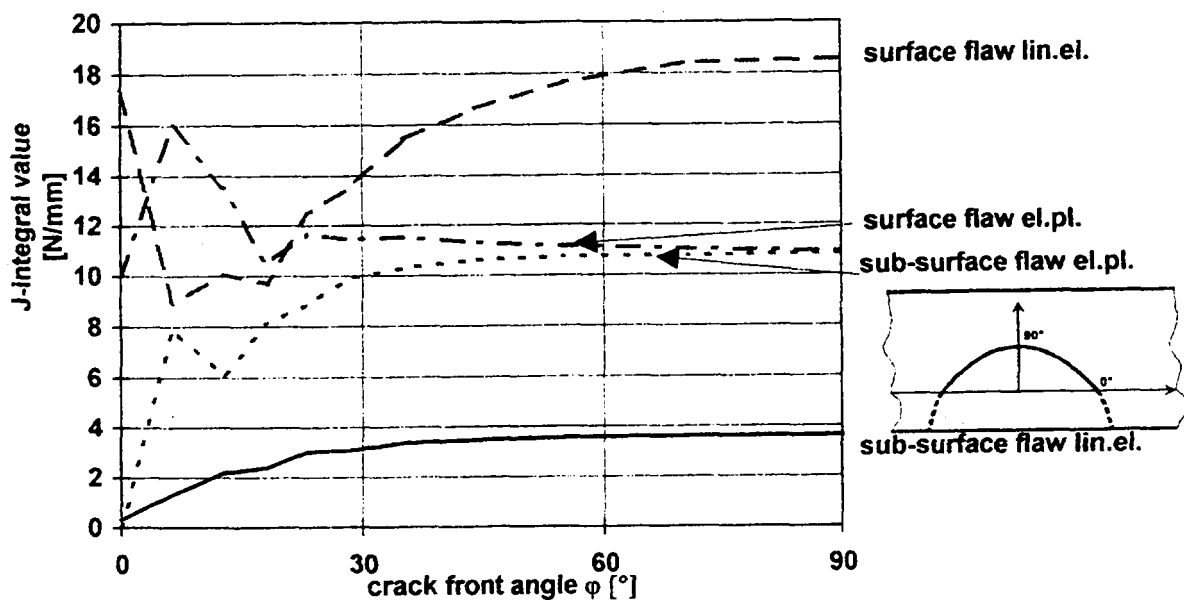


Figure 7: Displacements of the global model and the cracked region after a cooling period of 4000 s (magnification factor 50)



J-Integral versus crack front angle (Siemens NPP, time = 1300 s)



J-Integral versus crack front angle (Siemens NPP, time = 4000 s)

Figure 8: Distribution of J-Integral along the crack front

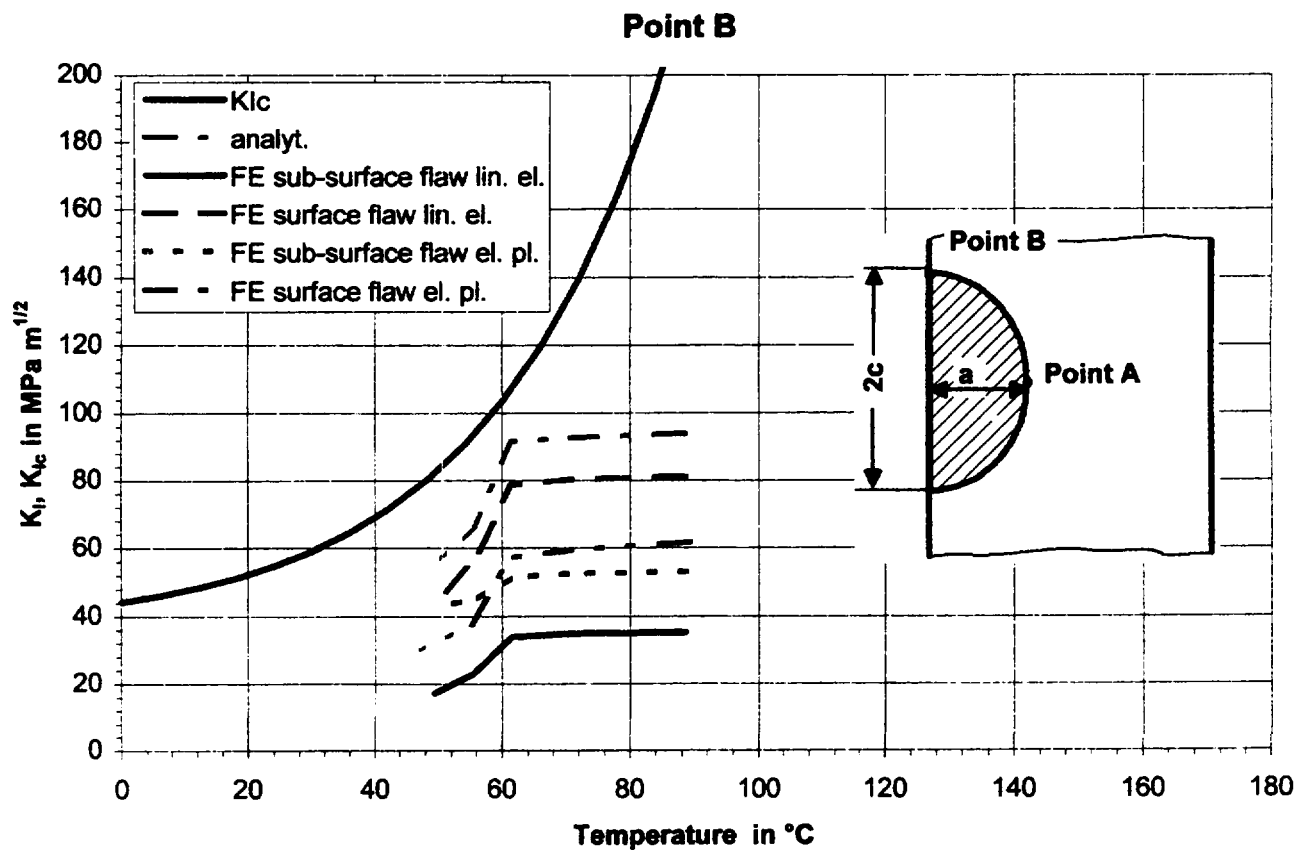
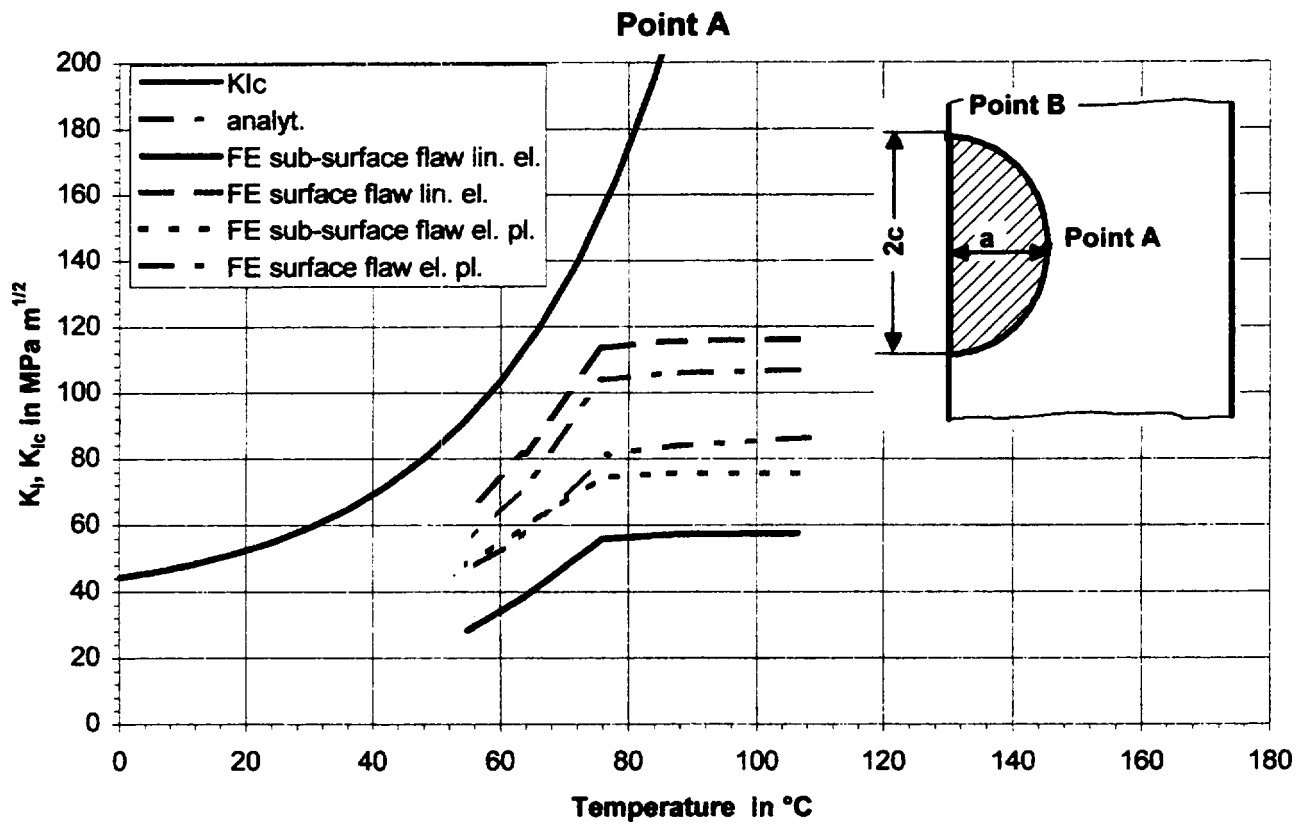


Figure 9: Comparison of surface and sub-surface flaw with analytical results

Session 6

6 MAY, TUESDAY

Database use

Beghini, M. D'Auria, F. Galassi, G. M. Vitale, E.	Italy	Evaluation of the PTS Potential in a WWER-1000 Following a Steam Line Break
Davies, L. M. Gillemot, F. Yanko, L. Lyssakov, V.	UK Hungary Russia IAEA	The IRPVM-DB Database
Davies, L. M. Gillemot, F. Lyssakov, V.	UK Hungary IAEA	PTS and the IAEA Database
Gillemot, F. Davies, L. M.	Hungary UK	Examples of the Use of the Database



XA9744638

EVALUATION OF THE PTS POTENTIAL IN A WWER-1000 FOLLOWING A STEAM LINE BREAK

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ABSTRACT

A qualified nodalization for WWER-1000 is available at DCMN (Dipartimento di Costruzioni Meccaniche e Nucleari) of University of Pisa that is suitable for running with the thermohydraulic system code Relap5/mod3.2. The nodalization consists of about 1400 hydraulic nodes and more than 5000 mesh points for conduction heat transfer. The four loops of the NPP are separately modelled.

Detailed information about the plant hardware has been got from contacts with Eastern Organizations in Bulgaria, Russia and Ukraine. The qualification of the nodalization has been achieved at a steady state level utilizing a procedure available at DCMN and at a transient level on the basis of operational (planned) transients performed in the Bulgarian Kozloduy-5 NPP and of the unplanned transient occurred at the Ukrainian Zaporosche NPP (April 1995). Data measured in steam generators have also been utilized.

The nodalization has been widely applied to the analysis of accident scenarios in WWER-1000, including Large Break LOCA, Small Break LOCA, ATWS, Loss of Feedwater and Station Blackout.

The present activity aims at evaluating the potential for PTS (Pressurized Thermal Shock) following a steam line break accident. The thermohydraulic results were employed as input for a parametric Fracture Mechanics analysis based on conservative hypothesis of the shape and localization of a pre-existing defect. Stress analysis evidenced the effect of partial cooling of the vessel and gave some general indications of the risk for unstable crack propagation under the simulated PTS conditions.

1. INTRODUCTION

The safety of nuclear power plants constitutes a global concern on the planet. This justifies interchanges of competences and information between Western and Eastern Countries in the nuclear technology area. Specifically, applications of advanced best estimate system codes to the evaluation of safety margins of Russian type WWER plants, are part of such a context.

Considering the experience gained in the validation and use of the Relap5 and Cathare codes, e.g. (D'Auria and Galassi, 1990a, D'Auria and Galassi, 1990b, Bernard and D'Auria, 1990) including applications to WWER related phenomena, e.g. (Mavko et al., 1995) (D'Auria et al., 1995) and the scientific cooperation with Energoproekt of Sofia (BG), the decision was taken to develop a WWER-1000 detailed nodalization. In the initial stage of the activity, Relap5/m3.2 (Fletcher and Schultz, 1995) code was selected. The reference plant hardware information is included in (Kolev et al., 1992), (Hinovsky, 1995) and (US DOE, 1987) nominal operating conditions and Emergency Core Cooling Systems are also described into detail in the same reports.

The nodalization was set up in the frame of a cooperation also involving the Italian Licensing Authority (ANPA), the Vendor Ansaldo and the University of Roma 'La Sapienza', e.g. (Gatta and Mastrantonio, 1995), (D'Auria et al., 1996) and (Cerullo et al., 1996) the last one also including the qualification of the Kozloduy transient data (see below).

The resulting nodalization consists of more than one thousand nodes; among the other things, each of the four loops of the plant is modelled independently. The qualification of the nodalization was attained in the frame of the activity documented in the paper by (Aprile, 1996) utilizing previously defined criteria, (Bonuccelli et al., 1993) and the plant transient data described by Cerullo et al., 1996 and by Kolochko, 1995.

Previous activities have been documented by Aprile et al. 1996 and by F. D'Auria et al. 1997a and F. D'Auria et al. 1997b dealing with the evaluation of safety of WWER-1000 from the thermohydraulic side. LOCA (Loss of Coolant Accident) and transients without the loss of integrity of the primary loop, are separately addressed in the two papers above. The main outcomes can be summarized as follows:

- ECCS (specifically SIT conditions) are well designed for Large Break LOCA but inadequate performance may happen following Small Break LOCA;
- peak cladding temperature in the hot rod (alone) has been found to overpass safety limits; however, initial neutron flux distribution was not known with sufficient approximation;
- ATWS transients originated by large break in the secondary side of steam generators (similar to the one here considered) may cause severe damage to the plant;
- accident management recovery procedures based upon the depressurization of the secondary side and/or upon the injection of auxiliary feedwater are extremely effective in recovering primary side owing to the large area for heat transfer available in the steam generators.

The present paper deals with the evaluation of the PTS (Pressurized Thermal Shock) potential in the RPV (Reactor Pressure Vessel) of the WWER-1000. PTS may occur in different accident scenarios involving simultaneous pressurization and cooling of the RPV. Different transients originated in the secondary side of the steam generator may cause primary side pressurization, while RPV cooling may be the result of cold water injection (mostly HPIS = High Pressure Injection System) or of cooling of the primary fluid following anticipated events in the steam generator. Without the availability of a finalized PSA (Probabilistic Safety Analysis), the selected transient is one of those having the potential for PTS. The origin is a large break at the top of one steam generator; scram occurs at 120% core power and all plant systems are assumed to be available (essentially, pump trip, steam generators isolation, pressurizer PORV actuation).

In order to make meaningful the analysis, the downcomer has been split into two parts: one connected with the single broken and the other connects with the three intact steam generators. This has been done both from a hydraulic and a structural mechanic point of view. This hypothesis produced a not axisymmetric cooling of the belt line of the vessel. Both in the downcomer and in the vessel wall, temperature was considered to be not depending on the angular position within each of the two parts.

Experimental and theoretical researches have been conducted in the recent years at the DCMN (see for instance Vitale (1989), Beghini and Vitale (1989), Beghini, Vitale and Milella (1991), Vitale and Beghini (1991), Beghini, Bertini and Vitale (1992)) on the structural behaviour of vessel during PTS accident. In particular, numerical methods were developed and applied for Fracture Mechanics analysis which allow facing these typically multiparametric problems in an accurate and efficient way. Such an approach was applied to this PTS scenario giving indication of the risk for a brittle rupture of the vessel during the transient.

2. OUTLINE OF THE WWER-1000 PLANT

The Kozloduy WWER-1000/320 plant, shown in Fig. 1, and documented in the papers (Kolev et al., 1992), (Hinovsky, 1995), (US DOE, 1987), and (Gatta and Mastrantonio, 1995), basically consists of:

- * a pressurized water reactor vessel of 3000 MWt rated power with 163 hexagonal fuel assemblies, which are open (ventilated type) to permit cross flows among the bundles. The fuel is UO₂ in annular pellet form, clad in zirconium-niobium alloy. There are 10 banks of control rods without fuel followers, located in 61 fuel assemblies;
- * inner radius and wall thickness in the belt line region 2068 mm and 199 mm respectively;
- * four primary loops equipped with horizontal tubes steam generators (Fig. 2) and main circulation pumps of centrifugal type;
- * one turbogenerator with 1500 rpm and 1000 MW electric power;
- * an emergency core cooling system including three High Pressure Injection Pumps, four accumulators (SIT = Safety Injection Tank) and three Low Pressure Injection Pumps;
- * two independent feedwater loops connected with the four steam generators; each loop includes a turbine driven pump and piping connecting the feedwater line to four different locations in each steam generator; several valves are part of the system. The two feedwater loops feed a common header upstream the four steam generators.

3. RELAPS NODALIZATION

A detailed nodalization was developed considering the guidelines of the code User Manual and the criteria by (Bonuccelli et al., 1993). The sketch of the noding scheme can be seen in Fig. 3; information about adopted code resources can be drawn from Tab. I. The vessel and the piping do not represent any particular challenge for the code being quite similar to the corresponding ones in the Western type PWR. However, the bypass flow paths configuration inside the vessel is different: in the case of the WWER-1000 the

upper part of the downcomer is not directly connected with the upper head: instead, a bypass flow path exists from the lower plenum to the upper head through the control rods housing and guide tube. The active core has been modelled adopting four parallel stacks (three core regions and one hot rod) characterized by different linear power.

Steam generator geometry imposes drastic changes in nodding philosophy with respect to what is suitable for the U-tubes steam generators. As "symmetry axis" for the secondary side (and consequently for the primary side), a line was taken connecting the center between the two collectors with the average (virtual) position of the centers of the horizontal tubes with the aim to separate (in the nodalization) the hot and the cold sides of the tubes. In this way, the secondary side of the steam generators was divided into three zones: a) the hot zone including the hot collector, and the hot 1/2 parts of the tubes; b) the cold zone including the cold collector and the cold 1/2 parts of the tubes; c) the downcomer region gathering different zones of the secondary side: essentially, the regions where downflow is assumed (e.g. the boundaries and the location of feedwater nozzles). Four positions were distinguished for the feedwater nozzles as results from Fig. 3. The nodes subdivision in the primary side was a consequence of the above: more nodes are placed on the hot side of the tubes; six axial levels lumping six groups of tubes, can be noted.

The Emergency Core Cooling System (ECCS) consists of three High Pressure, four Low Pressure Injection Systems (HPIS and LPIS, respectively) and four accumulators (SIT, Safety Injection Tank).

These are directly connected with the downcomer and with the upper plenum of the main vessel. All the ECCS have been modelled together with the AFW (Auxiliary Feedwater System) and the relief valves at the top of the pressurizer and in the secondary side of steam generators (PORV and SRV).

3.1 Nodalization qualification

The nodalization qualification process consists of two main steps, concerned with the steady state and the transient level, respectively. In the former, two sub-steps can be identified, see in the report (Bonuccelli et al., 1993): development of the nodalization and achievement of the steady state.

a) steady state level

Criteria like those recommending the maximum node length, the number of mesh points necessary to simulate the conduction heat transfer in the fuel rods and across the steam generators tubes, the connection between neighbouring nodes, etc. have been used. The first sub-step concludes with the check that various dimensions of the nodalization coincide, within an acceptable error, e.g. see (Bonuccelli et al., 1993), with the design or reference values. An example of this is given by Fig. 4 that shows the correspondence between the volume and the height of the secondary side of one steam generator.

The second sub-step deals with the achievement of the steady state. Typically 100 s are considered sufficient, running the code in the transient mode. Again, criteria dealing with the time derivative and the absolute values of relevant time trends are available and have been matched (in the last case, design or reference values are used for defining the acceptable error). Some results are shown in Tab. 2.

b) transient level

The NPP transients adopted for qualifying the nodalization at the 'on-transient' level are outlined in Tab. 3. It can be noted that the Kozloduy transients were 'planned' operational transients (Hinovsky, 1995) while an unplanned event occurred in Zaporosche in April '95, (Kolochko, 1995). In this last case, the intervention of three High Pressure Injection Systems and the accumulators was sufficient to recover the NPP in a few tens of minutes. The available NPP data were qualitatively and quantitatively well predicted by the nodalization, (Aprile, 1996).

The nodalization was also qualified through the comparison of results from the same (i.e. same sequence of events and properly scaled boundary conditions) small break LOCA transient in a Western type PWR and in related experimental facilities, Tab. 3, see also (D'Auria et al., 1996).

4. APPLICATION OF THE RELAP5 NODALIZATION TO THE ANALYSIS OF A TRANSIENT RELEVANT TO THE PTS

4.1 Framework of the activity

The main purpose of system codes is to perform accident analyses in order to confirm the design choices also in relation to the planning of Emergency Operating Procedures, e.g. see Aksan et al. 1996 and D'Auria et al. 1995.

Before giving details related to the transient of interest, it seems worthwhile to show the matrix of accident sequences already studied: the related results are reported in the already mentioned papers.

A matrix of accidents was fixed, essentially consisting of simplified sequences of events, without having in mind the results of probabilistic safety studies, as already mentioned. Rather, the selected events, put a challenge to the code and the nodalization, include a wide range of phenomena, and make possible the comparison with the results of similar transients in Western PWR.

Almost all the selected scenarios can be classified as "beyond DBA - before core melt"; namely "beyond DBA" testifies that not all the Emergency Systems (ECCS) were assumed to operate as from the respective design, and "before core melt" indicates that the analysis is stopped, eventually, when any fuel cladding surface temperature reaches the licensing limit. On these bases, the matrix in Tab. 4 was built up.

4.2 Large Steam Line Break with Scram

The transient here considered (VV-SLB-05) is the counterpart of the one listed as 6b in Tab. 4. An ATWS situation is considered in the test scenario 6b. Relevant boundary conditions for the test scenario are included in the same Tab. 4.

The main result related to the above test 6b is shown in Fig. 5, where the core power history is reported and compared with the same quantity characterizing scenarios 6a, 6c and 6d. It is shown that the lack of scram brings the plant to a dangerous situation for break areas larger than about 0.5 m^2 .

The VV-SLB-05 has the same boundary conditions and relevant imposed sequence of events as the test 6b, with the exception of the insertion of the control rod that is assumed when the neutron flux achieves 120% of its nominal steady state value.

Significant results are shown in Figs. 6 to 10 that depict the thermohydraulic scenario. As a consequence of the break, isolation of the steam generators occurs with consequent secondary side pressure rise (Fig. 6). Scram limits the core power to values of the order of 150% of nominal value (Fig. 8), keeping the DNBR (Departure from Nucleate Boiling Ratio) above the unity value also due to initial fluid temperature lowering and mass flux increase through the core (Figs. 4 and 5). In a few minutes primary pressure reaches the pressurizer PORV (Pilot Operated Relief Valves) opening set point owing to average primary coolant heat up (Figs. 6, and 9). Pressurizer level also increases as a consequence of the above (Fig. 7). Pumps operation has been assumed in order to maximize the potential for PTS.

4.2.1 Information relevant to PTS

In order to make more realistic the data base necessary for calculating the PTS potential of the considered scenario, it was necessary to introduce changes to the nodalization described in the Chapt. 3. Specifically, the three hydraulic nodes in the upper part of the downcomer, connected with the cold legs were split into two parts with flow area equal to $1/4$ and $3/4$ of the total value, respectively. The former was connected to the broken steam generator cold leg and the latter was connected with the three intact steam generators cold legs. The RPV wall structures associated with the hydraulic nodes were subdivided in the same way. Thirty meshes were considered for conduction heat transfer inside the RPV wall.

The temperatures of the meshes associated with the cooled part of the downcomer are given in Figs. 11 and 12: thermal gradients of the order of 70 K are calculated in the RPV wall thickness. The temperatures of the uncooled part of the downcomer ($3/4$ of the total) remain almost unchanged and for simplicity are not shown.

All this information is used as input to the structural mechanics calculation described in the following chapter.

5. FRACTURE MECHANICS ANALYSIS

5.1 Basic assumptions

During previous studies, the suitability of the Weight Function (WF) technique for solving Fracture Mechanics (FM) problems under thermal transients was generally verified. The WF coupled with the nominal stress (i.e. the stress in the uncracked structure) allows evaluating FM parameters straightforwardly by a simple integration. This approach is very efficient from a computational view point and can produce accurate results as compared to complete 3-D Finite Element (FE) analyses.

For a typical vessel geometry several WFs have been proposed both for 2-D and 3-D schemes. The present analysis was focused on the evaluation of the potentiality of the particular thermal transient to produce an unstable crack propagation. Therefore it was decided to limit the number of parameters making some conservative hypotheses about the location and shape of the crack. It was assumed that the temperature distribution obtained by the thermal analysis through the wall thickness could be applied to the belt line region where the most severe irradiation effect is expected. A pre-existing surface defect was assumed in the inner side of the wall. Crack was considered virtually infinite in axial direction and having constant through-thickness depth and was located just under the nozzle where cold water is injected. This allowed assuming a simple 2-D scheme for FM analysis which can be considered reasonably conservative. Indeed, real defects (commonly associated with vessel welds) have limited extensions and can be embedded in the wall.

Nominal stress was determined by assuming for the vessel a 2-D scheme of an infinite long cylinder made by homogeneous material (cladding was neglected) which is cooled in a region having angular extension equal to 90° . Within the cooled region the temperature distribution was assumed independent to the angular position and given by the thermal hydraulic analyses (Fig.11). A

cubic spline interpolation was applied to obtain a continuous function between the nodes. For the remaining part of the cylinder a linear through-thickness temperature was imposed.

Nominal stress was evaluated by an analytical model based on the theory of curved beams in generalized plane strain (Boyley and Weiner 1960) and either temperature distribution and internal pressure were assumed as loading conditions.

Stress Intensity Factor was determined by means of a WF recently proposed by (Verfolomeyev and Hodulak (1997)) on the bases of accurate parametric FE evaluations produced by Bryson and Dickson (1993). Given the temperature distribution, the accuracy in the SIF evaluation can be estimated in a few percent.

A surface crack is usually filled by the coolant. This was considered by adding the inner pressure to the nominal stress in the Stress Intensity Factor (SIF) evaluation (direct effect of pressure on the crack edges). However, the tip temperature was assumed not affected neglecting the refrigerating effect of the small quantity of liquid penetrating the crack.

5.2 Main results

Hoop stress due to thermal loading only is shown in Fig. 13 for several time instants during transient. Maximum value is attained after about 20 seconds and the peak level (170MPa) is comparable to the stress due to primary pressure. After about 40 sec. thermal stress becomes compressive at the inner surface.

If the temperature distribution produced by cooling a limited part of the vessel is applied to the whole cylinder, the result is usually considered to be conservative. In order to assess the level of conservatism, the nominal stress for the present thermal distribution was applied to sectors having different extensions. In Fig. 14 maximum thermal stress is plotted versus the angular extension of the cooled region (assuming the same temperature distribution for any cooled zone). It is worth noting that the hypothesis is conservative only if the cooled sector is lower than about 140°. In the examined case (cooled sector equal to 90°) thermal stresses were only a few percent lower than those evaluated during a complete cooling.

In Fig. 15 SIF evolutions produced by thermal transient was calculated for a few crack depth including relatively shallow ($a=5, 10$ mm), mean size ($a=40, 50$ mm) and deep ($a=100$ mm) defects. Negative values indicate that thermal loading tends to reduce the stress at the crack tip. The combined effect of thermal and pressure loading (total SIF) is shown for the same crack lengths in Fig. 16. By comparing Fig. 15 and 16, it can be concluded that thermal loading is more important for shallow cracks as the SIF is doubled during the PTS transient. On the contrary, for deep cracks the SIF increment during the transient tends to be a relatively small fraction of the regime value (due to pressure).

This is a general conclusion for a PTS analysis which usually indicates relatively shallow defects as the cause of the highest probability of unstable propagation. The condition of propagation for a 5 mm crack can be examined in Fig. 17 where curves of fracture toughness are reported too. The ASMR K_{Ic} reference function was adopted with a few RTNDT value. It can be observed that a very high transition temperature (RTNDT=270°C) needs to promote brittle failure. Similar conclusions were obtained also for other defect sizes.

This observation seems to indicate the examined PTS transient is potentially dangerous for relatively shallow cracks (5-15 mm depth) for a material in a very severe embrittled condition. In these cases, if an unstable propagation occurs, the probability for the crack to pass through the wall is very high as the SIF is strongly increasing with crack depth.

More complete analyses could be obtained by the proposed methodology (also including critical crack depth diagrams and probability estimates) having specific values of fracture strength of the material in the real conditions of the plant. A complete 3-D approach including the limited size of the defect can be made by adopting a suitable WF such as that recently proposed by Beghini, Bertini and Gentili (1997).

6. CONCLUSIONS

Conclusions related to the present study can be distinguished at two levels including the thermalhydraulic system behaviour and the stress response of the RPV walls

From the thermal qualified WWER-1000 plant nodalization suitable for Relap5/mod3.2 was developed in the present framework: steady state and transient level qualification criteria were adopted and matched. Relevant conclusions from previous analyses can be summarized as:

- * the Large Break LOCA behaviour of the WWER-1000 appears good from the safety viewpoint with low value for the Peak Cladding Temperature (PCT <1000 K); the hot rod represents an exception possibly originated from inadequate knowledge of steady state core power distribution;

- * Small Break LOCA without HPIS cause unacceptable rod surface temperatures (see below);

* operating conditions for accumulators appear well suited for Large Break LOCA; in the case of small break LOCA the small gas volume prevents complete liquid discharge into the primary loop;

* increase in the gas volume space of accumulator largely improves their performance, though an optimization process for selecting this quantity has not been performed.

* AM procedures based on delayed actuation of AFW (secondary side feed) and steam generator depressurization (better secondary side bleed) are very effective and should be considered for the Emergency situations in WWER-1000;

* natural circulation performance of the WWER-1000 systems appear suitable from the safety point of view, provided sufficient heat sink is available for steam generators;

* a long time is available to operators (more than 1 hr) before core uncover occurs in the case of steam generator tube ruptures, even in situations where relatively large break areas are considered (i.e. up to 20 tubes, see Tab. 4);

* the introduction of a PWR like neutron kinetic model made it possible to perform ATWS calculations. In the case of LOFW, the increase of the primary system temperature keeps the core power at the decay value starting from about 100 s after the initial event; the consequent slow boiloff scenario does not lead to core uncover (DNB) for at least 20 mins. Limited spurious injection of cold unborated liquid does not cause unacceptable consequences. In the case of steam line break, the capability of the system to withstand the consequences largely depends upon the considered break areas (Fig. 5 and Tab. 4)..

The main result from the system analysis is the demonstration that scram is able to control the reactivity rise consequent to a large break in one steam line, keeping the power to values not endangering the core integrity.

The achieved conclusions should be verified on the basis of a proper uncertainty analysis (e.g. adopting the methodology described by (D'Auria et al., 1995); a better knowledge of all relevant boundary conditions would also improve the reliability of the results.

From the Fracture Mechanics point of view a model based on the Weight Function was applied for analysing the transient. By adopting some conservative hypotheses on the shape and position of the defect a parametric analysis was performed giving the following main conclusions :

* cooling a 1/4 of the vessel does reduce the thermal stress as compared to the complete cooling but thermal stress is no more than a few percent lower. The conservatism of the axisymmetric loading hypothesis is not excessive in this case ;

* Thermal Shock increases the Stress Intensity Factor more significantly for shallow cracks than for deep defects as for those pressure loading is prevailing ;

* temperature reduction of the crack tip region is not particularly severe and a high level of material embrittlement is required for promoting an unstable crack propagation.

More complete analysis could be performed if specific data about material fracture properties and typical shape, size and location of defects were available.

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PARAMETER	NPP KOZLODUY
NUMBER OF NODES	1102
NUMBER OF JUNCTIONS	1148
NUMBER OF HEAT STRUCTURES	1028
NUMBER OF MESH POINTS	5590
NUMBER OF CORE ACTIVE STRUCTURES	42
NUMBER OF CONTROL VARIABLES	1107
NUMBER OF TRIPS	58

Tab. 1 - Nodalization resources.

VVER1000 PLANT	UNITS	EXP	NEW NOD
Primary side pressure	MPa	15.7	15.7
Hot Leg Temperature	°C	320.1+3.5	322
Cold Leg Temperature	°C	289.8+2	287.2
Primary Side Mass	Kg	240000	235100
Prz level	m	8.77	8.75
Core Power	MW	3000	3000
Reactor Flow	Kg/s	15704	15450.6
SG Power	MW	750	748
Secondary Side Mass	Kg	160000	159900
Feedwater Temperature	°C	220	220
Secondary Side Pressure	MPa	6.28	6.27

Tab. 2 - Nodalization qualification at steady state level: calculated and measured boundary condition values.

TRANSIENT	PLANT	TYPE	DESCRIPTION	INITIAL POWER	INITIAL PRESSURE
KZ-1	Kozloduy-5	planned	1Pump trip	72.	15.7
KZ-2	Kozloduy-5	planned	2Pumps trip	54.	15.7
KZ-3	Kozloduy-5	planned*	Partial loss of feedwater	72.1	15.8
ZAP	Zaporosche	unplanned	PORV stuck open	1.0**	18.8
PWR	Krsko	calc.°	Small Break LOCA	100.	15.

* unplanned sequence of events

** estimated

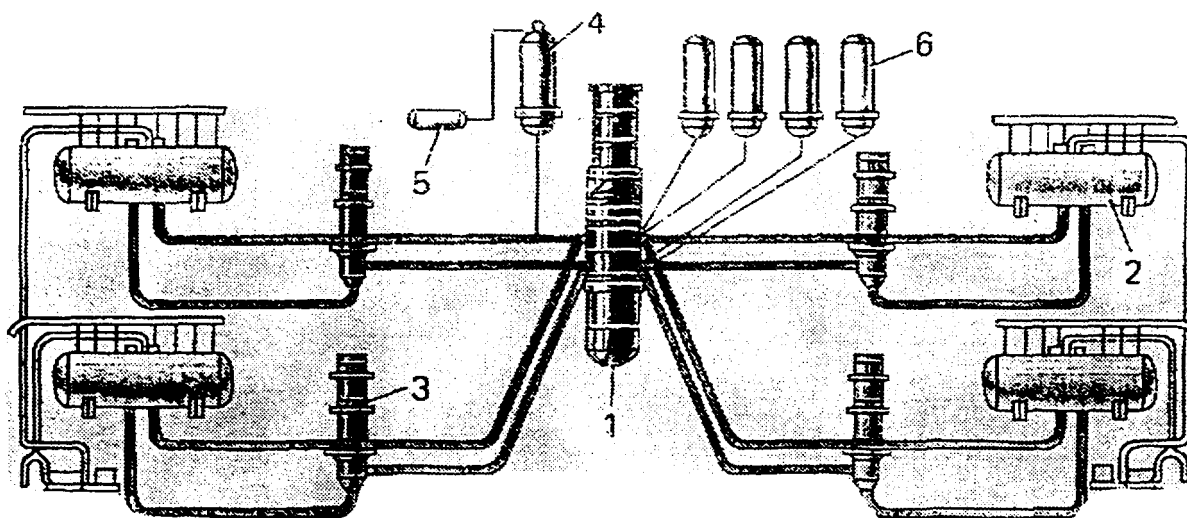
° with the support of experimental data in Test facilities

Tab. 3 - Transients utilized for the 'on-Transient' qualification of the Relap5/mod3 WWER-1000 nodalization.

N°	NPP TRANSIENT	TYPE	SG SIGNIFICANT CONDITIONS	HPIS	ECCS LPIS	SITS	RCP	AM PROCEDURE
1a	VV-SGT-01	SGTR 1 tube	Isolation at scram (AFW off)	off	in CL at 2.15 MPa	4 at 5.9 MPa	off	-
1b	VV-SGT-03	SGTR 5 tubes	Isolation at scram 30k/hr (intact SG)	off	in CL at 2.15 MPa	4 at 5.9 MPa	off	-
1c	VV-SGT-04	SGTR 20 tubes	Isolation at scram 40k/hr (intact SG)	off	in CL at 2.15 MPa	4 at 5.9 MPa	off	-
2a	VV-LFW-01	LOFW	Isolation at scram AFW delayed	off	off	4 at 5.9 MPa	on	AFW
2b	VV-LFW-02	LOFW	Isolation at scram AFW delayed	off	off	4 at 5.9 MPa	off	AFW
3	VV-PTR-01	LOFA 4 pumps	Isolation at scram off	off	off	off	off	-
4a	VV-SBP-00	PORV Stuck Open	15K/hr after scram AFW off	at 8.9 MPa	in CL at 2.15 MPa	4 at 5.9 MPa	off	-
4b	VV-SBP-01	PORV Stuck Open	15K/hr after scram AFW off	off	in CL at 2.15 MPa	4 at 5.9 MPa	off	-
4c	VV-SBP-02	PORV Stuck Open	15K/hr after scram AFW off	off	in CL at 2.15 MPa	4 at 5.9 MPa	off	SIT* init. cond
4d	VV-SBP-03	PORV Stuck Open	SG SRV opening after dryout AFW off	off	in CL at 2.15 MPa	4 at 5.9 MPa	off	SG BLEED
5a	VV-LFW-A1	LOFW ATWS	Isolation	off	off	4 at 5.9 MPa	on/off	-
5b	VV-LFW-A2	LOFW ATWS	Isolation	off	off	4 at 5.9 MPa	on	HPIS unborated
6a	VV-SLB-01	SLB/ATWS BA=0.0022 m ²	Isolation	off	off	4 at 5.9 MPa	on	-
6b	VV-SLB-02	SLB/ATWS BA=0.98m ²	Isolation	off	off	4 at 5.9 MPa	on	-
6c	VV-SLB-03	SLB/ATWS BA=0.5m ²	Isolation	off	off	4 at 5.9 MPa	on	-
6d	VV-SLB-04	SLB/ATWS BA=0.1m ²	Isolation	off	off	4 at 5.9 MPa	on	-

*N₂/H₂O ratio changed from 1/5 to 7/5

Tab. 4 - List of performed RELAP5 calculation of WWER-1000 transient scenarios: sequences of assumed main events.



- 1 - reactor pressure vessel;
- 2 - steam generator;
- 3 - primary coolant pump;
- 4 - pressurizer;
- 5 - quench tank;
- 6 - emergency core cooling accumulators

Fig. 1 - WWER-1000 NPP: Primary system

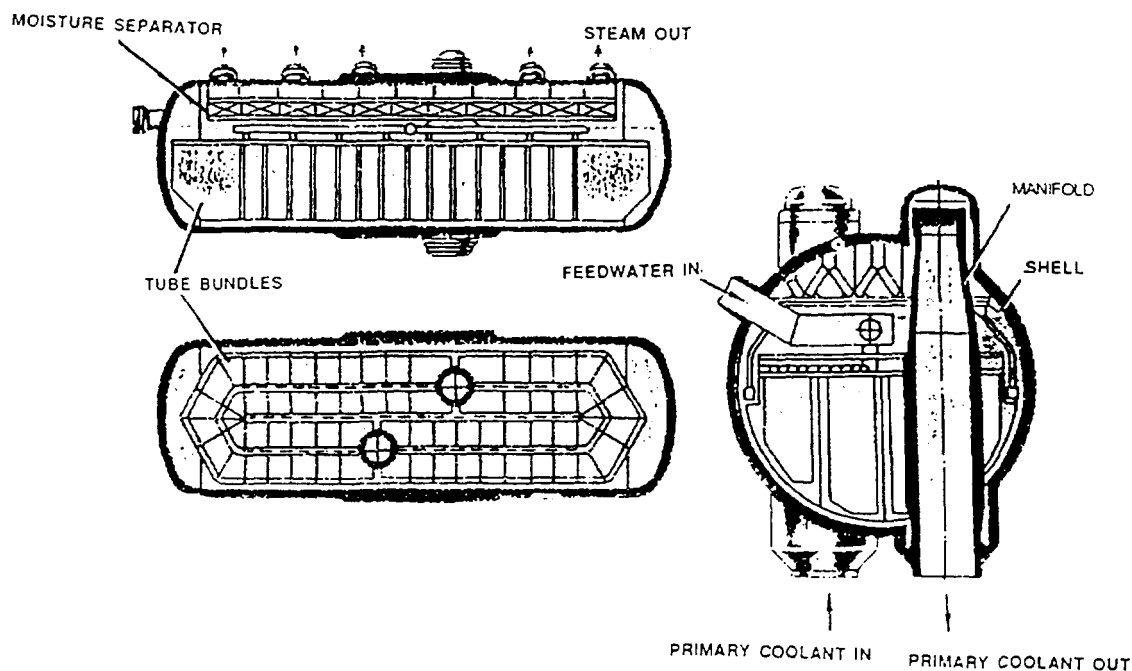


Fig. 2 - WWER-1000 NPP: Steam Generator detail.

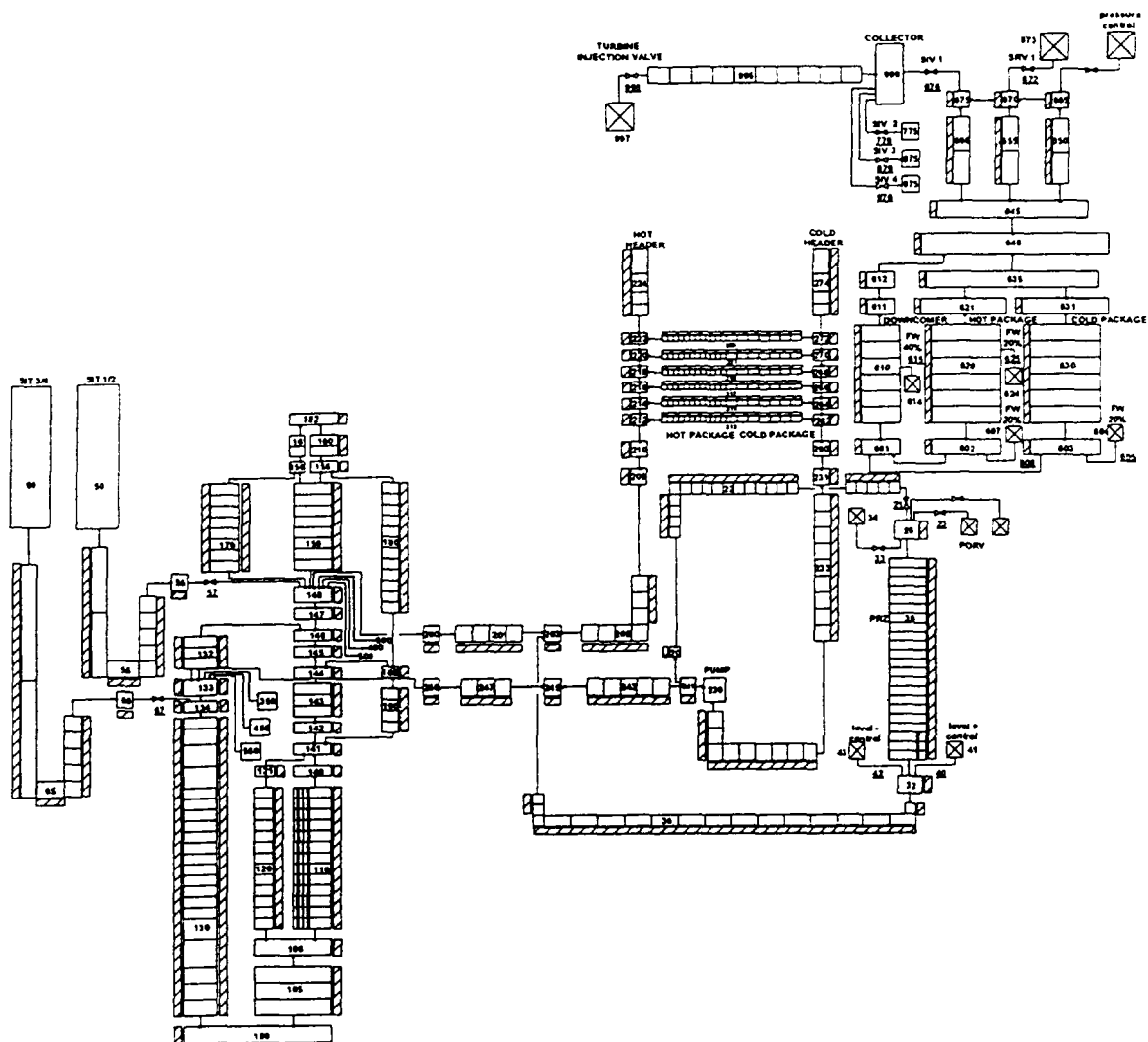


Fig. 3 - WWER-1000 NPP: Noding scheme for Relap5/mod3.2

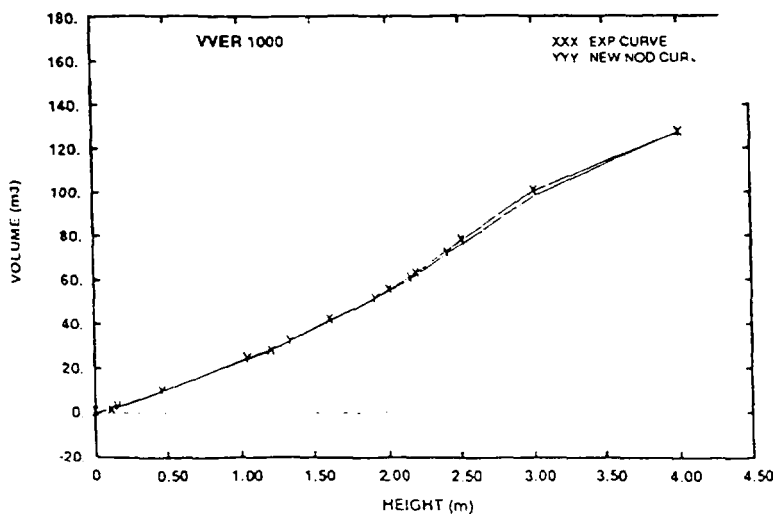


Fig. 4 - Nodalization qualification at steady state level: calculated and measured volume/height curve for SG.

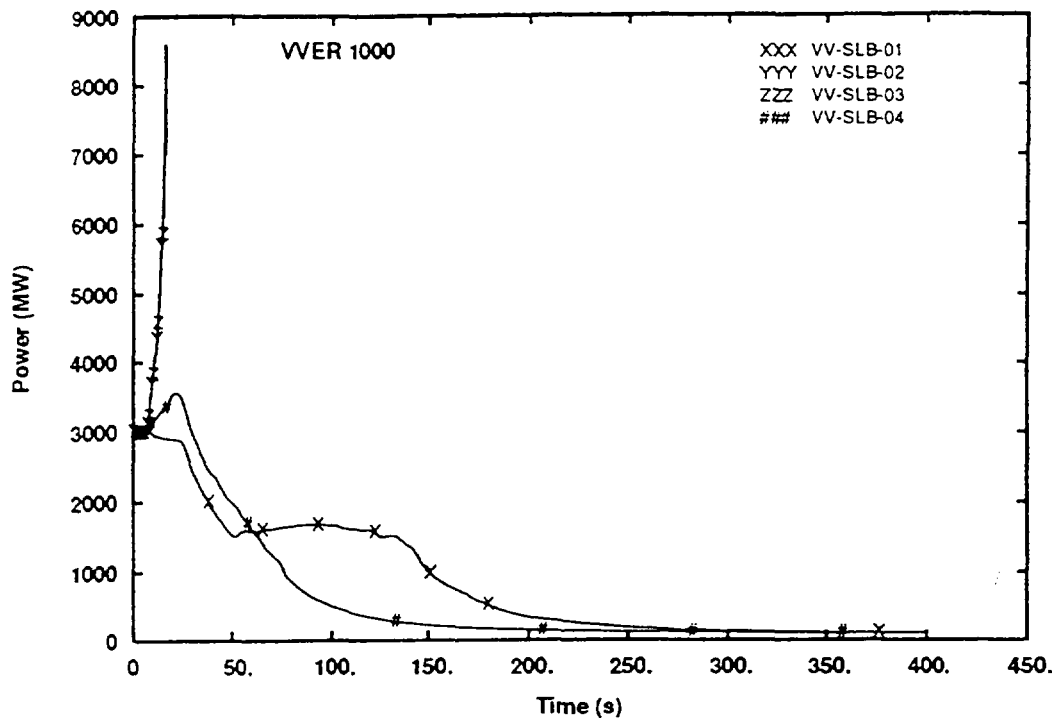


Fig. 5 - WVER-1000 SLB/ATWS: core power (list of transients in Tab. 4).

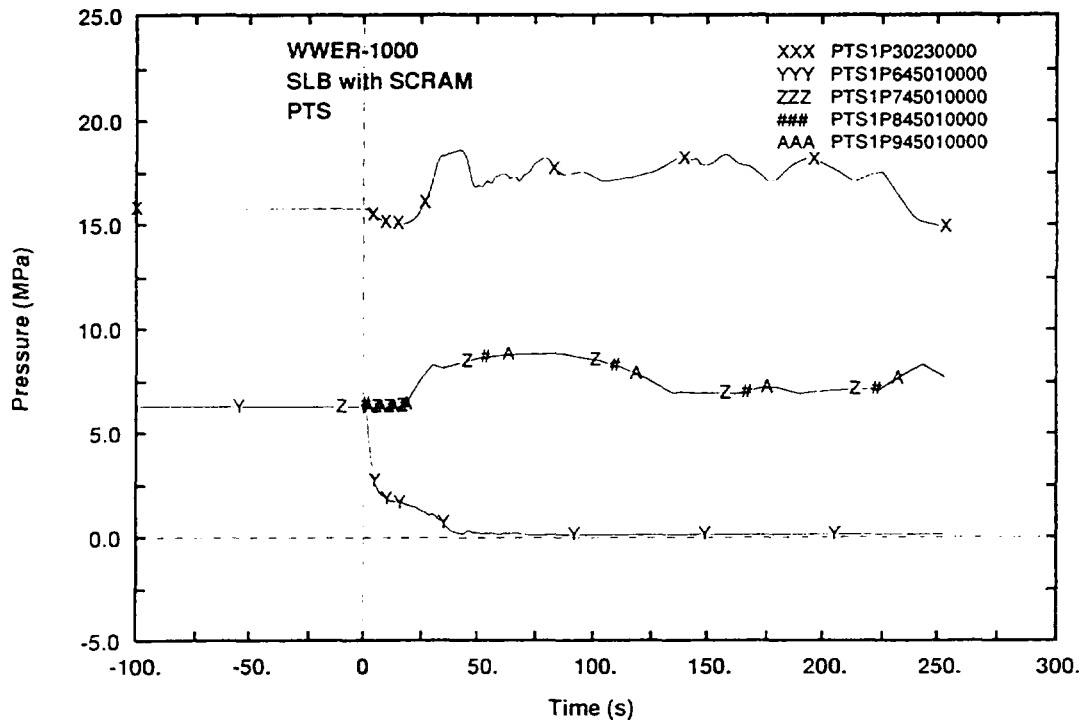


Fig. 6 - WVER-1000, SLB, PTS calculation: primary and secondary side pressures.

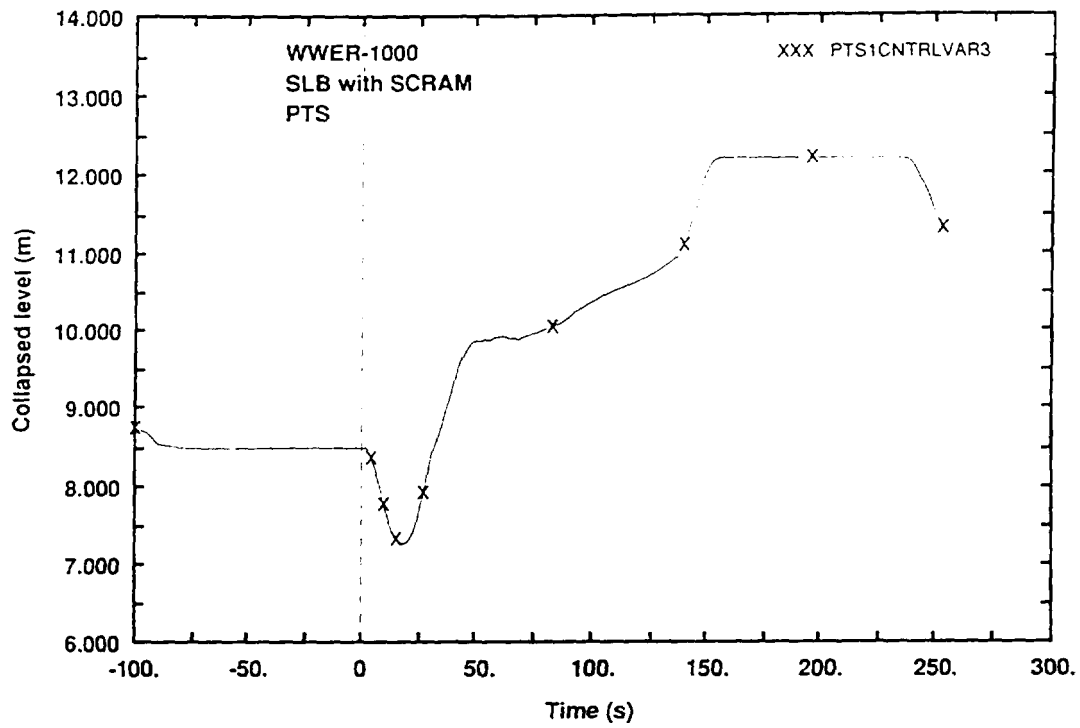


Fig. 7 - WWER-1000, SLB, PTS calculation: pressurizer level.

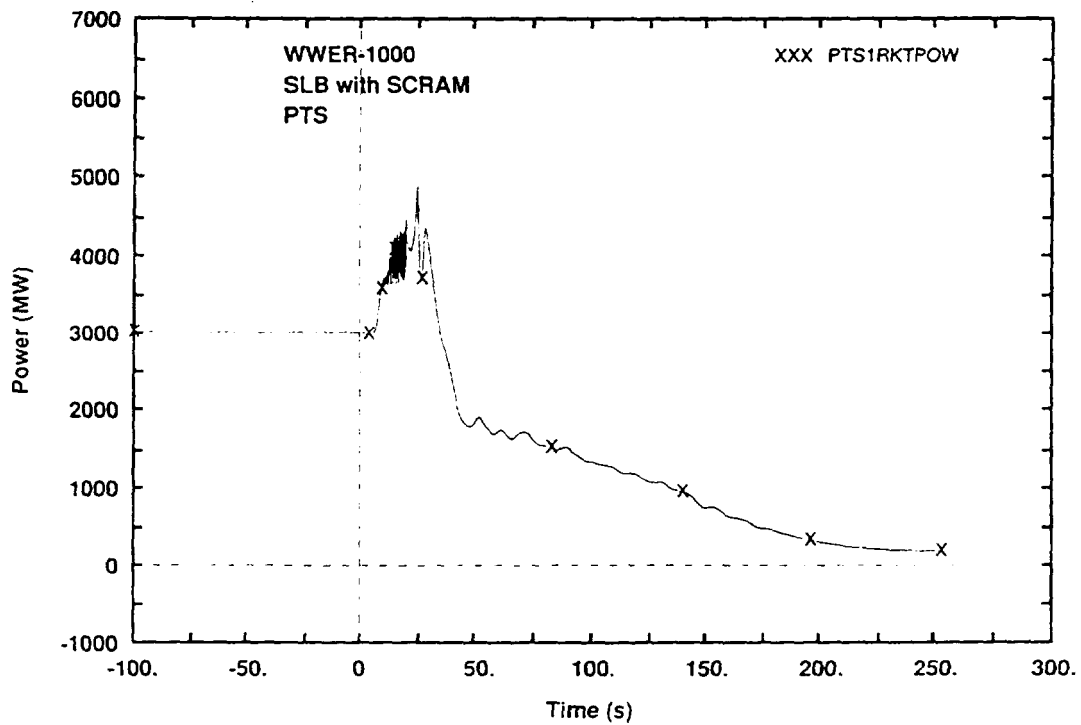


Fig. 8 - WWER-1000, SLB, PTS calculation: reactor power.

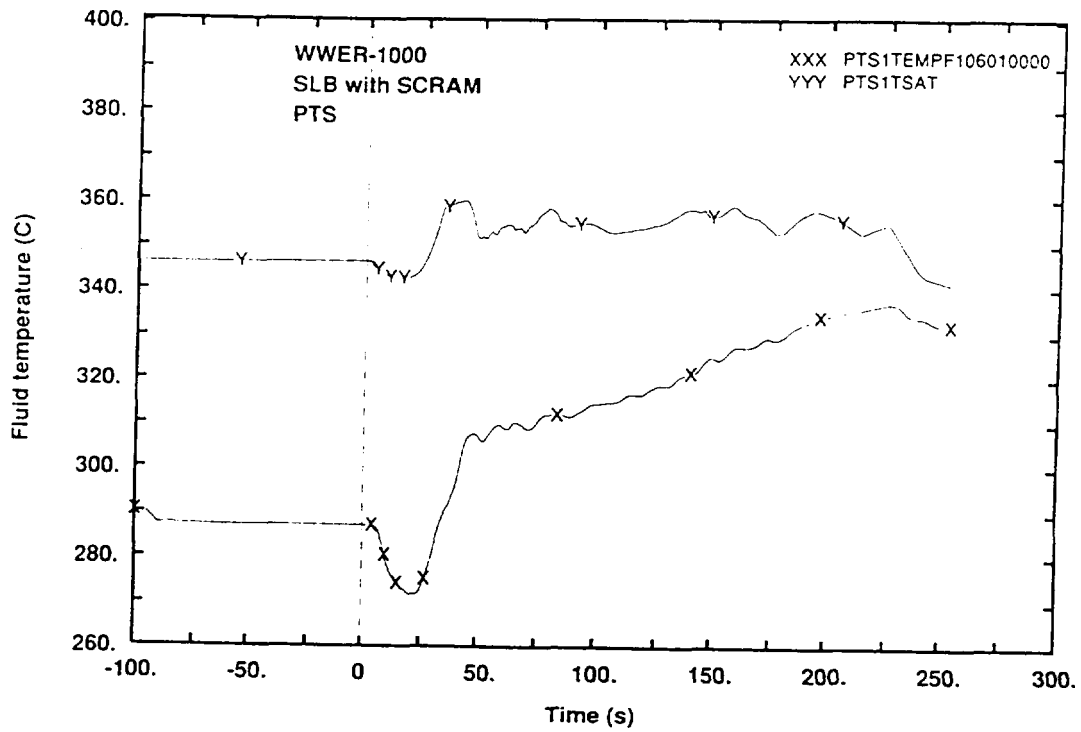


Fig. 9 - WWER-1000, SLB, PTS calculation: fluid temperature in the LP compared with saturation temperature.

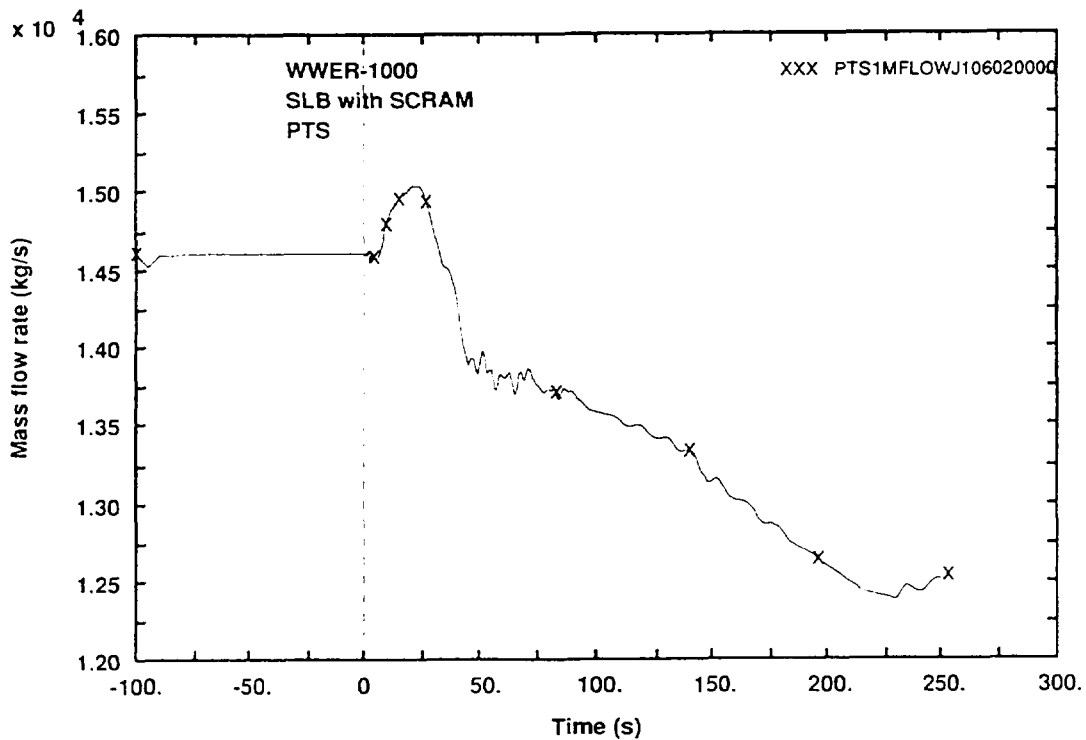


Fig. 10 - WWER-1000, SLB, PTS calculation: mass flow rate at core inlet.

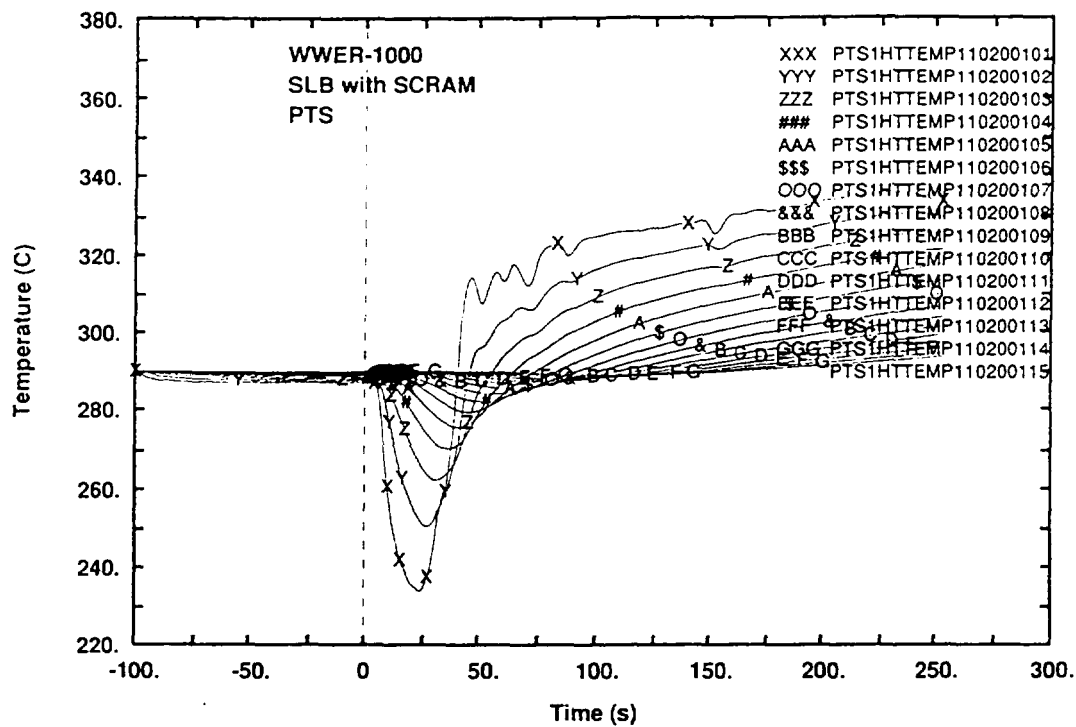


Fig. 11 -WWER-1000, SLB, PTS calculation: temperature distribution inside the vessel wall (meshes from 1 to 15), affected SG side.

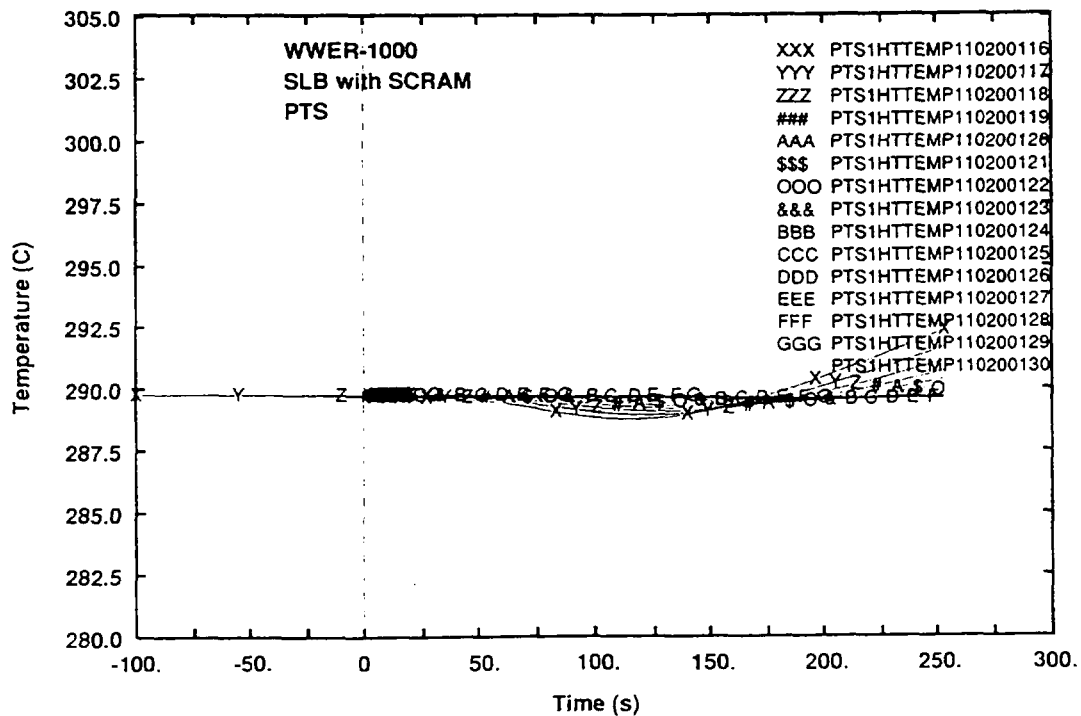


Fig. 12 -WWER-1000, SLB, PTS calculation: temperature distribution inside the vessel wall (meshes from 16 to 30), affected SG side.

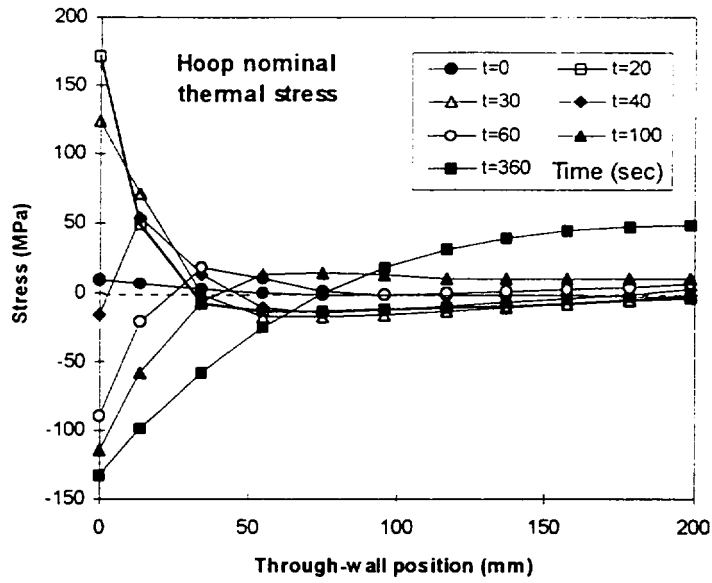


Fig. 13 - Thermal stress distributions in the cooled zone of the belt-line by assuming the cooled part equal to 1/4 of the cylinder.

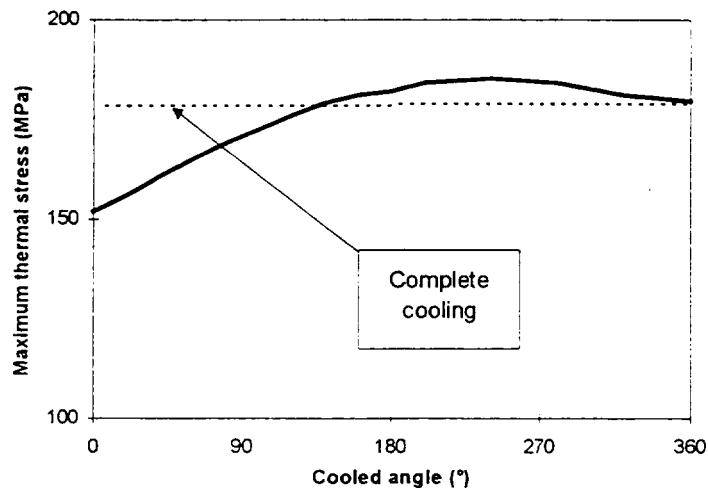


Fig. 14 - Maximum thermal stress in a cylinder by imposing in a sector the through-wall temperature distribution equal to that evaluated at 20 sec. in the analysed transient.

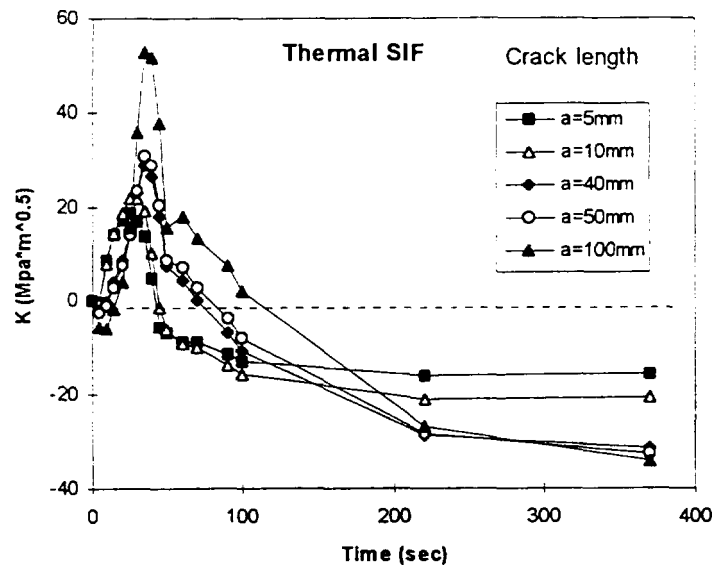


Fig. 15 - SIF produced by thermal loading only during the PTS transient for some crack lengths .

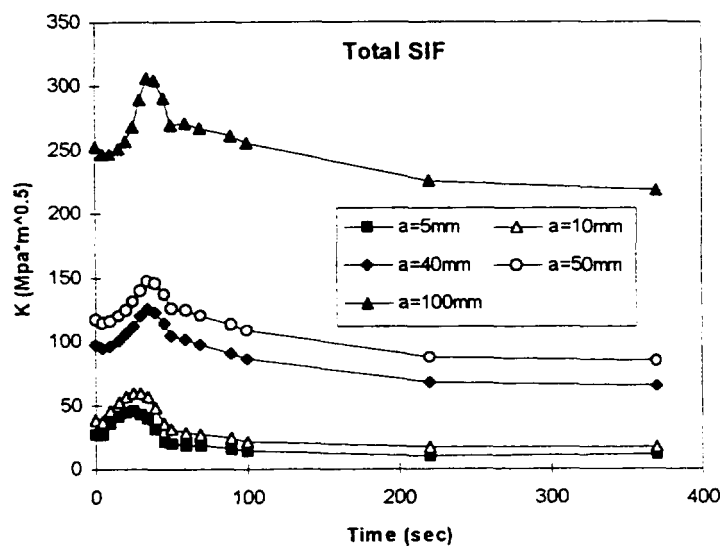


Fig. 16 - SIF during the PTS transient for some crack lengths under the effect of pressure and thermal loading.

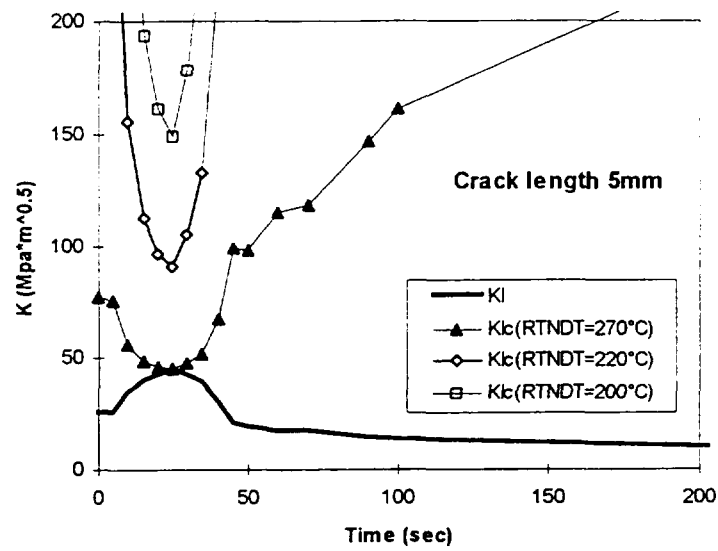


Fig. 17 - Comparison of SIF and fracture toughness for a shallow crack ($a=5\text{mm}$) during the PTS transient for three values of the brittle-ductile transition temperature.



THE IRPVM-DB DATABASE

by

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Abstract.

The IRPVM-DB (International Reactor Pressure Vessel Material Database) initiated by the IAEA IWG LMNPP is going to collect the available surveillance and research data world-wide on RPV material ageing.

This paper presents the purpose of the database; summaries the type and relationship of data included; gives information about the data access and protection; finally it summaries the state of art of the database.

Keywords: *Reactor pressure vessel, Database, Irradiation embrittlement, Pressurized thermal shock.*

1. INTRODUCTION

In this paper we will be concentrating on one aspect of the IAEA international data base-on neutron irradiation effects on the mechanical properties of RPV steels and welds. This is a subject which continues to be significant in the field of integrity and PTS (Pressurized Thermal Shock) assessment.

2. THE ORGANIZATION OF THE DATABASE

There are two specific features of this Database which influence the Organization of the Database, its membership and management. The data to be included is very expensive and it has been generated in national and international multimillion programs on reactor materials. The requests from potential Database members based on the above mentioned fact provide special conditions on the membership (please see 2.4). The second point is that some data especially that which provides a link between material properties and a particular utility or manufacturer could have a commercial nature and therefore should be treated by the Agency as confidential.

The Database organization includes: IAEA, Database Custodian, Steering Committee and the Database members.

2.1 The Role of the IAEA

The IAEA organizes the Database according to requests from Member States and recognizing the importance of such activities.

The IAEA will:

- act as coordinator to compile, maintain and manage the database;
- manage the Database to ensure that its rules and procedures are correctly and efficiently implemented taking into account the interests of all members in managing the operation of the database;
- control access to the data through a system of confidentiality and data protection;
- develop in consultation with Database members, standards, formats, definitions, rules, procedures and guidelines to be used for the preparation and processing of input and for the creation and utilization of output;
- prepare input of literature published by the Agency and other UN organizations;
- arrange meetings of the Steering Committee of the Database.

2.2 The Role of the Custodian

The Agency will identify and appoint the Database Custodian.

The Custodian will act as the agent for the IAEA in operating and maintaining the database and providing an effective interface for Member States/Participating organizations. The Custodian will also arrange for data gathering from Member States and Database members, and will assist with data evaluation and distribution as appropriate.

2.3 Steering Committee

The Steering Committee will consist of one representative from every big data supplier Member States of the International Database. The representative will be appointed by the national authority of the Member State. Members of the Steering Committee shall elect from among themselves a chairman.

The responsibilities of the Committee are:

- recommendations of the procedures for regulating the operation of the Database;
- preparation of a progress report for the Agency's International Working Group on Lifetime Management of Nuclear Power Plants (IWG-LMNPP) meetings;
- discussion of proposals from Members on the International Database.

2.4. Database Members

The Database Members will include persons or organizations from Member States that provide and are entitled to use the database as well as to receive database information. Each Database Member will be responsible for data gathering, as well as validation and verification.

3. PRESENT STATUS OF THE DATABASE

The preparatory work of the Database included:

- development of the Database Specification;
- development of the Database Agreement;
- preparation and distribution of official requests to Member States to join the Database;
- expanding the Database to include degradation mechanisms other than irradiation and other components important to safety and reliability;
- elaboration of new features as to include visual information (diagrams and metallography pictures);
- elaboration of software (in MS Access) to provide easy use for participants;
- include the database of the IAEA coordinated research program "Optimizing of Reactor Pressure Vessel Surveillance Programs and their Analysis" results;

- preparation of CD ROM distribution for participants
- 10 countries and 3 international research programs are already joined. The database presently includes the IAEA research program on radiation embrittlement (CRP-3), and data from 2 countries (~ 4500 Charpy data, 150 tangent hyperbolic curves, 200 Charpy curves etc.) According to the existing agreements with the members this data amount will be multiplied until the end of 1997.

The document "International Database on Nuclear Power Plant Life Management - Database Specification" has been published by the IAEA as "Working Material - IWG-LMNPP-95/4" in September 1995.

This document provides information on objectives, scope, data collection and management requirements. The main part contains background information and information of generic interest, the appendices provide component specific requirements for some NPP components. In particular, Appendix A provides a detailed description of the International Database on Reactor Pressure Vessel materials, still in Dbase format. Appendix B describes Primary Piping Database Requirements for Plant Life Management, Appendix C addresses Steam Generators and Appendix D covers Containment.

4. PARTICIPATION AND DATA ACCESS

Membership of the Database shall be restricted to States which are Members of the Agency, and organizations in Member States, recognized by those states, which are in possession of data relevant to the International Database. International research programs - recognized by the IAEA - also can join to the database, but they can access only research data. This is an important statement and a specific feature of this database is that only those organizations which contributed to the Database can join it and get all the benefits of being a Database Member.

Every member State of the Agency which is a member of the International Database shall appoint a Liaison Officer to act as a focal point. The liaison officer is expected to serve as a key person in the country for all interaction with the Agency regarding the Database.

The Member of the Database is expected to:

- collect, categorize and prepare reactor pressure vessel material data on a "best efforts basis" as well as data validation at its own expense;
- contribute advice and recommendations on matters relating to the maintenance, improvement and development of the Database;
- provide information services to a monitoring contact with, to the extent practicable, users of the database within its territory and representing user's news at meetings of the Database;
- obtain clearance from the Agency Database members before providing information derived from the International Database to non-members of the Database.

Database Members who have fulfilled all the Database requirements and have supplied data shall have full access to all non-confidential information from the International Database.

The Agency may release information from the Database to non-members only after obtaining clearance from the data supplier.

Some data in the Database will be treated as confidential. In the developed database format the Agency suggests to include such data in specially marked (italic printed and shaded) areas. The Agency will prepare a special coding system to protect this data. A Database member may, at its option, prepare its own coding system for its confidential information. Access to confidential data shall be only through the Agency. Such data can only be released with the express approval of the Member who supplied the data. The Agency shall have the same rights of access to the International Database as a Member. The Agency may use only non-confidential data in the International Database for elaborating guidance and recommendations for developing countries and without releasing the data.

5. TECHNICAL FEATURES

5.1. The database structure

The "International Database on Reactor Pressure Vessel Materials" is a research database as it derives from the purpose and tasks of the IAEA. The new enhanced database has 20 files as follows (note that the CRP-3 research database consisted only of 6 files).

MATERIAL IDENTIFICATION information

Type of information	File name
<i>Material code, type</i>	RPV_MAT
<i>Manufacturer; utility data</i>	RPV_GEN; RPV_UTIL
<i>Technology; welding</i>	RPV-TEC; RPV-WEL

AGEING HISTORY information

<i>Irradiation history</i>	RPV_IRR
<i>Thermal ageing</i>	RPV_THR

MECHANICAL TESTING

<i>Tension tests</i>	RPV_TEN
<i>Charpy testing</i>	RPV_CV
<i>Static fracture testing</i>	RPV_SFR
<i>Dynamic fracture testing</i>	RPV_DFR
<i>Hardness testing</i>	RPV_HRD

EVALUATED DATA

<i>Constants of exponential curve fit on fracture toughness data</i>	RPV-EXP
<i>Charpy transition temperatures and constants of the fit tangent hyperbolic curves</i>	RPV-TT

REFERENCES

<i>References</i>	RPV-REF
<i>Related documentation</i>	RPV_REL

VISUAL DATA

<i>Metallography, fractography in TIFF. format</i>	RPV_MET TIFF picture files
<i>Spectra, flux distribution, instrumented impact, tensile, static fracture, J curves, etc. in scanned pictures</i>	RPV_PIC TIFF picture files
<i>Spectra, flux distribution, instrumented impact, tensile, static fracture, J curves, etc. in digitised form</i>	RPV_DIA TIFF picture files

The size of the future Database on Reactor Pressure Vessel Materials is limited due to the availability of the data. Presently the biggest data bases in this field are less than 20 MB. The RPV ageing data base is expected to be less than 50 MB - with only data- and 1-5 GB when including visual data (photomicrographs and a measured curves section).

To ensure the security of the data owner only the technical data will be accessible, all data which are particular to the owner will be withheld by the Agency, and data will be secretly coded. The coding system will support selection and grouping of the data.

In the case of a research database there is no need for an on-line data access, as generally there is time enough to distribute the data by post. These conditions allow the use of diskettes (CD ROM), which means that every participant could use the database without special equipment. At the same time the use of CD diskettes significantly reduces the possibility of unauthorised persons accessing the data.

5.2 Special features

Existing data bases on RPV ageing include data mostly in number format only. One of the main purposes of the data base is to enhance the understanding of ageing mechanisms and to help the development of effective methods for the timely detection and mitigation of ageing effects. Mathematical analysis of the mechanical testing data on new and service aged materials may serve new mechanistic models, and may enhance the design curves. This is a very important benefit of the data base, but is in itself not enough to understand the ageing mechanism. A new metallography section and previously collected testing curves are included as an extension to the IAEA International Database on "Ageing Management and Life Extension of Reactor Pressure Vessel Materials".

Metallography section

There are two possible levels for the metallography section.

- Level one is a simple reference file of the available photomicrographs. Such a file has been included into the CRP-3 database, and is also included into the new IAEA International Database on "Ageing Management and Life Extension of Reactor Pressure Vessel Materials".
- The enhanced level also includes the collection of the digitised pictures. Several hundred high resolution photomicrographs can be collected on an easy to use and cheap CD diskette. Some typical photomicrographs are shown as examples in Fig. 4.

The enlargement of the database by the metallography section provides the possibility of common evaluation of strength and structural changes. Evaluation of ageing in practice is a very difficult task. During site testing in situ metallography or the use of replica testing is an available testing method. But evaluation of the pictures can be conducted only on the basis of extended practice, and the results are subjective. If the results are compared with database pictures the life evaluation becomes simpler and more precise.

Databases which include mechanical testing, chemical and metallographic information together are not common. As a consequence of these features the IAEA International Database of Reactor Pressure Vessel Materials could become a leading material ageing database world-wide.

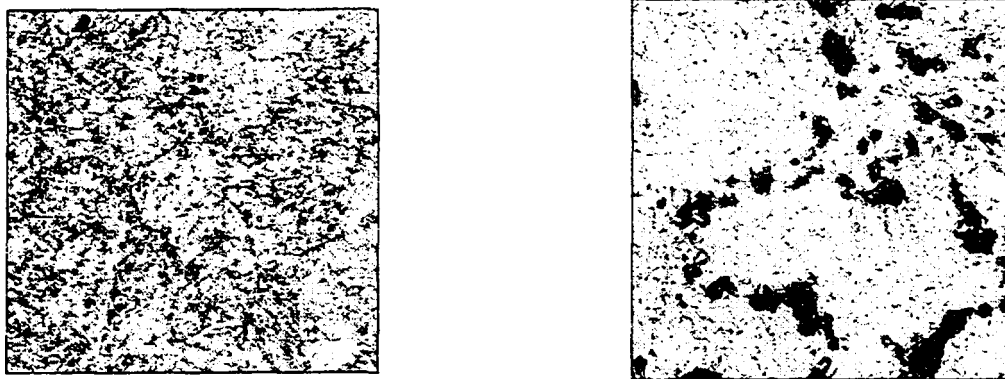
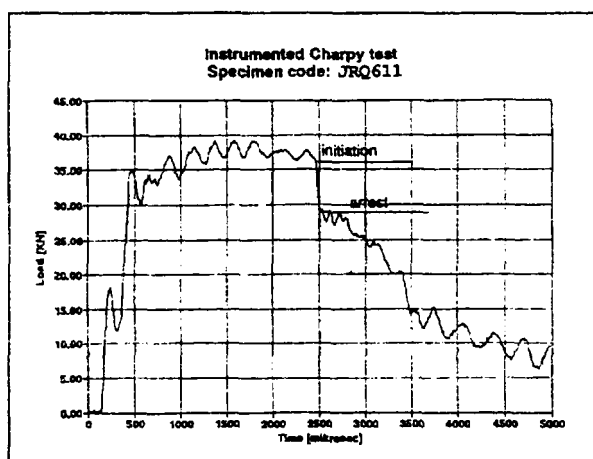


Fig. 1. Digitised metallography pictures reprinted. Cr-Mo-V materials a.) as produced b.) aged at 680C 2000 h.

Measured curve section. During mechanical testing of structural materials testing diagrams are obtained. In case of traditional evaluation of the results only some specific points of the curves are used. The measured curves, however, include much more information. For example, the VTT have proved that J integral measurements can be compared, only if the original measured curves are evaluated by the same method and in the same laboratory. In the case of instrumented Charpy testing the crack initiation and propagation energy can be separated, even crack arrest information can be obtained from the measured curves, which is generally only stored, but never used for practical purposes. Application of the "Local Approach" method needs correct flow curves, not only tensile data. All of these features justify the storage and acquisition of the original curves from mechanical testing. Similarly the neutron spectra cannot be characterised with a single number, but can easily be described by a diagram. All of these examples justify the use of digitised diagrams within a material ageing database.



Time	Load
[μsec]	[kN]
...	...
550	22,15
552	22,43
554	22,65
556	22,91
...	...

FIG. 5. Reprinted instrumented impact testing curve, and a part of the ASCII file .

Technically the acquisition of curves doesn't require any extra investment. Most of the up-to date testing equipment uses digital data acquisition and storage methods. The curves could be acquired and transformed to a standardised form ASCII file. Since the size of the stored records is generally less than 10 KB, any type of traditional diskette can be used as a data carrier, and any mathematical software package used in a normal PC can convert them into curves again. Fig. 5. shows a part of a digitised data file, and the printed curve.

6. THE DATA IDENTIFICATION AND CODING SYSTEM

The coding system

The coding system ensures the anonymity of the data sources as well as the connection among the data files, and facilitates the selection and use of the data. The identification codes include 1-3 part. Every group of data (data characterizing a certain heat of a material and collected from the same data source e.g. one utility, or laboratory) has a different Mcode. During elaboration a separate Mcode will identify the data groups belonging together.

IAEA_MCode:(9 characters) the material identification code is given randomly by the IAEA. This code will identify all of the processed data to be supplied to the participants.

This code is built up from the following characters (digits):

Characters 1-6 = reference identification number given randomly by IAEA

Characters 6-9 = material type

FOR	Forging
WEL	Weld metal
HAZ	Heat affected zone
PLA	Plate
LAB	Laboratory heat
LBW	Laboratory welding
CLA	Cladding
CAS	Casting
OTH	Other

The following files include general information connected with material production and the data sources: RPV_MAT; RPV_GEN; RPV_TEC; RPV_WEL.

Multiple information can belong to the same heat of materials. In this case a second identifier the IAEA_NUM is used for the correct characterization. IAEA_NUM is a three digit random or artificial number, individually identifying the data or specimens belonging to the same group.

RPV-CHEM; RPV_REF and RPV_REL fields are characterized by MCode and NUM.

Information characterising a group of similarly aged material identified by MCode and ACode: RPV_IRR; RPV_THR; RPV_EXP; RPV_TT; RPV_MET; RPV_DIA; RPV_PIC.

Finally the specimen data are characterised by Mcode+ACode+NUM. This way every individual data can be separately stored and identified. The following data files are using all three identifier: RPV_CV; RPV_SFR; RPV_DFR; RPV_TEN; RPV_HRD.

IAEA_ACODE (3 digits)

In case of aged material the **IAEA-ACode** characterizes the type of the ageing.

IAEA ACode	type of ageing
NUL	zero level testing
IRR	irradiation in research reactor
IRA	irradiation in research reactor and annealing
IPR	irradiation in power reactor (surveillance)
IPA	irradiation in power reactor and annealing
IAR	irradiation and ageing in research reactor
IAP	irradiation and ageing in power reactor
TMP	template cut from power reactor wall
CAV	irradiation in power reactor cavity
THA	thermal aged in laboratory
HFA	high cycle fatigue
LFA	low cycle fatigue

Remark: further ageing codes can be included

8. TECHNICAL FEATURES OF DATA ACCESS, DATABASE SOFTWARE

Any IBM AT 486 or bigger capacity PC could be used for data analysis of the RPV ageing database. For analysis of structural damage (metallography) a PC 486 or a larger PC including a CD drive is recommended with a high resolution screen and with a 600 dpi capacity printer. The new generation of the widely used scanners and printers generally fulfil these requirements, which means that the enhancement of the database with metallographic information doesn't require extra investment, only some additional effort from the participants.

The data are stored in MS ACCESS format. The database software which is provided for participants together with the data by the IAEA is able to export the selected data in Dbase, Foxpro, Paradox, Lotus, Microsoft Excel, Microsoft Word, Text formats. The data are stored in ISI units. The database software is able to export data converted into, US, or user defined units. Specialists using the data do not have to learn a new computer language, or the use of any new software, and the data can easily be included and compared with any national database. These new features makes for the easy use of the database for all participants.

Any participant or group of participants who wish to evaluate the database, is entitled to get the updated and processed version of the database on diskettes, or on CD ROM. In case of special request the data can be filtered, sorted and grouped according to the requirements.

9. ACKNOWLEDGEMENT

The developments of the IRPVM database represents the work of several people. In particular it is a pleasure to recognise the great contribution of T. Griesbach, F. Cam; M. Brumovsky; V. Lyssakov; L. Ianko, V. Kiselev.

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PTS AND THE IAEA DATABASE

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1. Pressurised Thermal Shock (PTS)

The International Atomic Energy Agency Data Base has been described previously [1][2] at this meeting. It is the purpose of this paper to briefly describe PTS and to draw attention to the relevance of the International data base in the area of irradiation effects.

PTS, in a Pressurised Water Reactor Pressure Vessel [3], is a strain which derives from an overcooling transient happening at the same time as/or followed by, re-pressurization. Rapid cooling of the vessels surface results in a tensile stress at the inside surface, the magnitude of this stress being dependant on the temperature gradient as a function of time. The often quoted example is the incident which triggers the safety injection system while coincidentally a significant pressure is still being maintained or re-established in the system. Other transients can be initiated by instrument and control system malfunctions which can include stuck open valves in primary or secondary systems and postulated accidents such as the 'small break loss of coolant accidents', 'main steam line breaks' and 'feed water line breaks'.

PTS as evaluated in the US and in those countries which follow the US approach will be described elsewhere in this Specialist Meeting

The risk to the vessel in terms of the impact of pressurised shock on its integrity is assessed in two ways:

a) **Deterministic**-using combinations of defect sizes and transients and applying minimum safety factors.

b) **Probabilistic**-using distributions of flaw density, flaw size RT_{NDT} etc. and calculating the conditional probability of failure from a given transient. The product of this probability of failure and the probability of occurrence of the transient for all PTS transients leads to a global probability of failure from PTS. In the US this failure probability must remain below a certain limit.

If the probability and location of the occurrence of a critical defect is low and if the fracture toughness of the pressure vessel is sufficiently high then its integrity is not challenged by the coincidence of the transients. However, the selection of assumptions for a PTS integrity analysis is not specified in the rules but do usually require to be justified.

It follows that the generic behaviour of NPP of a particular type can be described and in the US approach to the PTS rule a 'screening criterion' can be evaluated in terms of the degradation in the mechanical property of the pressure vessel caused by irradiation.

The RTPTS is defined as:

$$RT_{PTS} = I + \Delta RT_{PTS} + M$$

where I is the unirradiated or initial reference temperature (RT_{NDT})

ΔRT_{PTS} is the increase in RT_{PTS} caused by irradiation

M is the margin to allow for uncertainties in the initial properties, copper and nickel content, fluence.

It follows that, for this US PTS case, because it uses the predictions of the Reg. Guide 1.99 Rev 2 that the higher the copper, nickel and neutron fluence then the higher the shift in RT_{PTS} .

The magnitude of the margin, M , is dependent on whether the pressure vessel material is weld or base material. It is also dependent on whether the unirradiated reference temperature value is derived from test results or from generic data. The increase in RT_{PTS} could be derived from surveillance data or from Reg. Guide predictions.

In the US (and only in the US) the a screening criterion is defined from a generic probabilistic analysis which specifies limit values of RT_{NDT} below which no specific analysis is necessary.

Importantly, the flaw density function, flaw depth density function, fracture toughness curves and analytical techniques used have to be justified. Cladding must be taken into account in the thermal and stress analysis.

2. Approach to PTS and irradiation effects internationally

Gerard [4] has surveyed the national regulatory requirements and, in relation to this paper, PTS. Except for those countries which follow US practice, like Belgium and Spain, the approach is different in the methodology

reflects the national Regulatory and engineering approaches ranging from those countries which have no prescriptive regulatory requirements to those where a more sophisticated approach is adopted.

In the consideration of irradiation effects we have already seen that in the generic case the Regulatory Guide 1.99 Rev 2 is used to predict the degradation of mechanical properties during neutron irradiation. This version relies on the copper and nickel concentration. Other countries use different trend equations to predict irradiation degradation [5]. For steels made and used in other countries different deleterious elements are identified in the trend equations. For example, in France and Japan phosphorus is included in the trend equation. In Russia, for example, phosphorus is a dominant embrittling agent. Prediction of irradiation therefore becomes unique to a particular national data base and the derived trend curve. Differences with other trend curve data bases may reflect:

- i) data scatter
- ii) differences in irradiation temperature
- iii) differences in composition and irradiation behaviour
- iv) inadequacy of the national data base

In a comparison of Eastern and Western steels [6] it was concluded that using national-but different trend curve equation predictions, that the steels of known 'chemistry factor' exhibiting highest irradiation sensitivity showed similar behaviour (see Figure 1). The essential difference in irradiation response was the End Of Life neutron fluence which was higher for the WWER steels. Conversely, at lower concentrations of deleterious elements, the irradiation sensitivity was reduced. This illustrates that there are differences which may or may not be significant in national PTS evaluations. Conversely, a generic approach can be adopted and the input parameters (including of course national data and assessments on transients and NDE as well as the irradiation effects) derived in a country with large numbers of NPPs. However a plant specific analysis would have to be conducted in those countries where there is little generic or reliable data.

3. The IAEA data base.

The International database on Reactor Pressure Vessel Materials Development has been described in other papers at this meeting [1] [2]. Part of the data base activity is to do with irradiation effects and that data-base is being set-up at this time.

It was appreciated at an earlier stage that the creation of this data base as well as the analysis of the data collected will help substantially:

- *generating utilities* in a more sophisticated, supportive
- analyses of surveillance data or in the provision of generic data-for RPV integrity, and operational lifetime assessment for plant operational life assurance .
- *design authorities* in the provision of data of materials behaviour during operation and for early consideration design or mitigating features to extend the designed operational life time.
- *safety authorities*, in some countries, to prepare information in order to assess safety margins as well as an aid in licensing processes and safety report analyses.
- 'researchers' in assisting the understanding of damage mechanisms producing degradation in mechanical properties.

It can therefore be seen most readily that the operation of this aspect (irradiation behaviour of RPV steels) of the IAEA IRPVM-DB database will support its participating organizations in a most fundamental way. It can add to the treatment of national data which can then allow enhancement in the assessment of PV integrity for PTS and other operational conditions, operational life assessment of a 'big ticket' item of plant with regard to operational plant life assurance and assist in the understanding of degradation mechanisms in PV steels.

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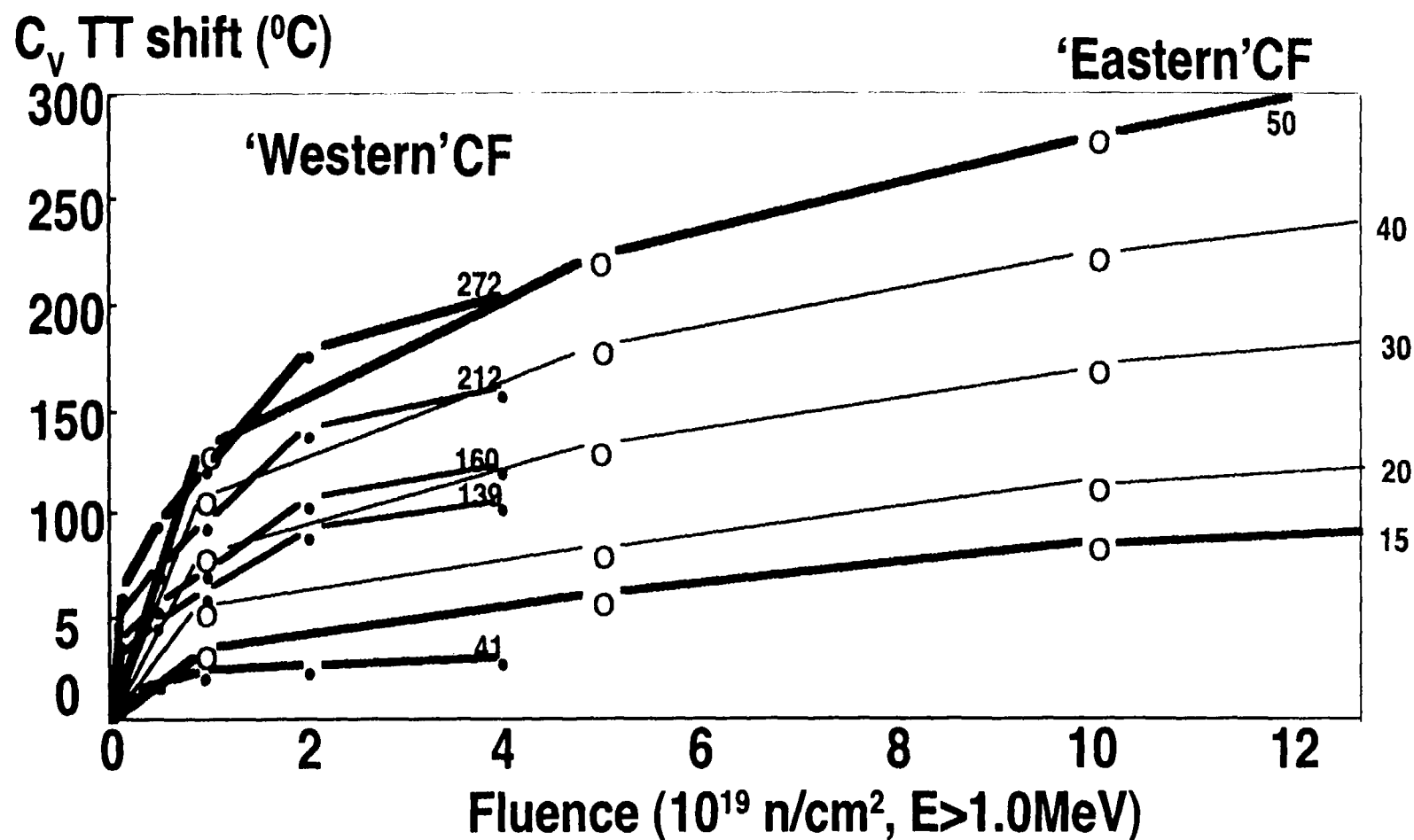


Figure 1. Shift in the transition temperature versus neutron fluence ($E > 1\text{MeV}$) to the End of Life for an appropriate range of 'chemistry factors', from Russia and the US, encompassing the compositions of 'real' Eastern and Western steels. Note that the Western curves are truncated at a fluence of $\sim 4 \times 10^{23}$ n/cm 2 which is the limit of the US data base underpinning the USNRC Reg Guide 1.99 Rev 2.



EXAMPLES OF USE OF THE DATABASE

by

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Abstract

Databases on ageing are generally used for elaboration of trend curves, and development of new steel types. Moreover they can be used for enhancing PTS evaluations. By more detailed PTS evaluation the calculated lifetime will be longer and resulting in the utilities being able to decrease the cost of life management efforts.

The paper introduces three examples of database use related to PTS evaluation.

Keywords: Database, PTS assessment, Fracture toughness, Thermal ageing

1. INTRODUCTION

It is a well known fact that large databases on ageing can be used to elaborate new trend curves. Sometimes they are also used for the comparison of surveillance results obtained at different units. These types of applications are frequently considered to be in the interest of the steel producers and the safety authorities. Utilities tend to have the opinion that they supply the data which is eventually used against them. Of course the safety and the elaboration of good and realistic trend curves is in the common interest of both the utility and the authority. In this paper some practical examples are given on the utility and research oriented application of databases on ageing demonstrating their necessity.

In the case of reactor pressure vessels the most severe event what the RPV must survive is the so called thermal shock. If any failure occurs in the primary cooling circuit resulting loss of cooling, the emergency core cooling system start to fill the system by water. The cooling water rapidly cool down the RPV inner surface. The outer surface follows it by a big delay, and high thermal stresses are occurs. The thermal stresses are accompanied with the stresses caused by the inner pressure and with the residual stresses. If the resulted local stress peaks are in the region of a defect edge, the defect may start to propagate. Ageing of the material results toughness reduction, that is smaller stress peak is enough to start the crack initiation.

To study the material ageing properties, to evaluate the distribution of the measured data, elaborate ageing trend curves big databases are needed. The IAEA initiated and run IRPVM-DB database to satisfy this requirements. The data base collects the worldwide existing data on neutron irradiation effects and on the mechanical properties of RPV steels and welds.

2.) EVALUATION OF SURVEILLANCE PROGRAMS.

Updated PTS analyses also require an enhanced database. If the scatter of the material data is large, the results of the analysis will be unreliable regardless of the calculation method.

The testing of irradiated specimens is difficult. Due to the remote control handling of the specimens and the testing equipment the probability of erroneous measurements is increased. Because of this situation the uncritical use of the surveillance results may cause errors. As an example table 1 shows a series of impact results on surveillance specimens.

Table 1. Charpy testing results obtained on surveillance specimens

T=[°C]	-200	-100	40	40	40	60	60	60	80	80	80	200	200	200
KV=[J]	3	5	158	20	27	68	150	30	86	130	50	110	166	121

The impact data in the temperature range of 40-60 °C show very different values. Because of this the fitting results will diverge, that is no real tangent hyperbolic curve can be fitted for these points. If the high point at 40 °C (158 J) is deleted the fitting will be a correct tangent hyperbolic curve, but considering the measured data at the transition region the curve seems to reflect an optimistic estimate (See Fig. 1. curve 2)

If the second diverging data point (60°C, 150J) is also deleted correct and pessimistic tangent hyperbolic fitting can be obtained. (See Fig. 1. curve 3)

The difference between the results of the latter two solutions is 23 °C in the transition temperature, which may result in a 12-20 years difference in lifetime.

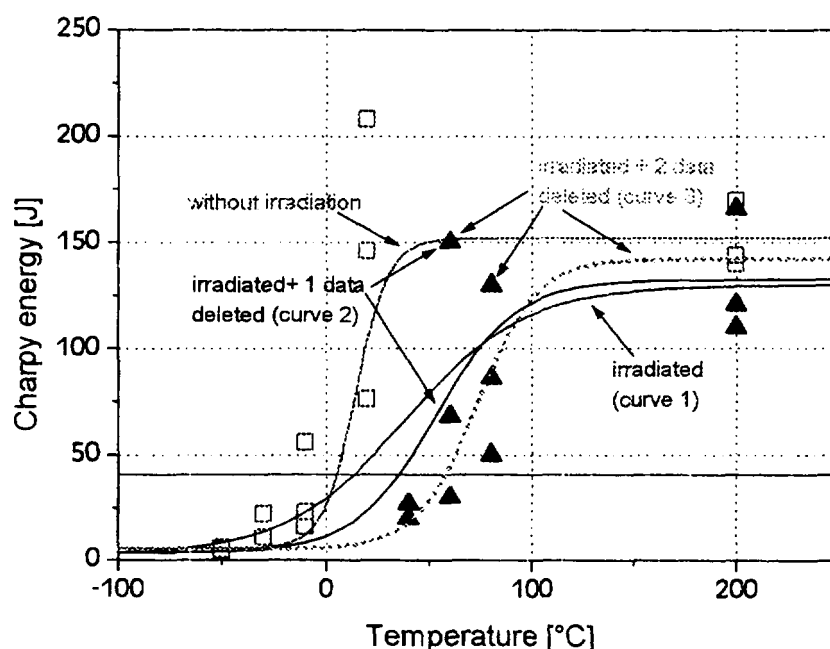


FIG. 1. The effect of deleted data on tangent hyperbolic fitting

The selection of the point which may be deleted during evaluation must be based on mathematical statistical methods. The application of these methods requires correct information on the normal scatter of data obtained from irradiated specimens, and this information should be derived from a large database.

3.) DATA FOR PROBABILISTIC ANALYSIS

Probabilistic analyses need a mean K_{Ic} curve fitted on the existing data (See Fig. 2.). Presently the 95% probability lower bound curves for K_{Ic} data of the reactor materials are available, but the mean K_{Ic} curves are missing from the Codes or Normative documents. Generally no single research program or surveillance program can supply enough data to get a widely acceptable mean reference curve. Using the mathematics of probability it easily can be understood that in case of a low number of data the scatter band belonging to the curve will be wide. A wide scatter band increases the probability of sometimes using very low toughness data during the probabilistic analysis, and this results in an unacceptably high failure probability of the analyzed vessel.

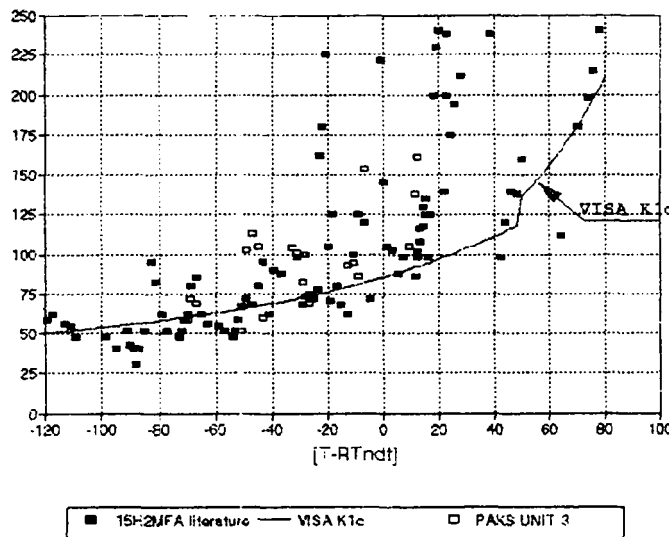


FIG. 2. Fracture toughness curve for probabilistic analysis

4.) THE EFFECTS OF THICKNESS

Studies of the thick forging and plates used for the nuclear industry have shown that the material properties, specially the transition temperature measured by Charpy V impact testing (TTKV) shows variation in the function of the thickness. Several good examples were presented during the International Atomic Energy Agency Coordinated Research Program (IAEA CRP) on "Optimizing of Reactor Pressure Vessel Surveillance Programs and their Analyses" phase III. (generally called only CRP-3) program. German researchers have shown the property change in the JRQ (IAEA reference) steel [1]. In the program database the specimen location was characterized by the distance from the nearest original surface and called "depth" [2]. Figure 3. shows the TTKV temperature distribution in JRQ material using the CRP-3 program data.

Theoretically the three dimensional tensile stresses are bigger in large size specimens, and valid K_{Ic} , which is a critical value can be measured only by them. In

databases many test series show that the results obtained in 1 CT specimens are worse than the results of 6 CT specimens. This can be explained by the large variation in the transition temperature.

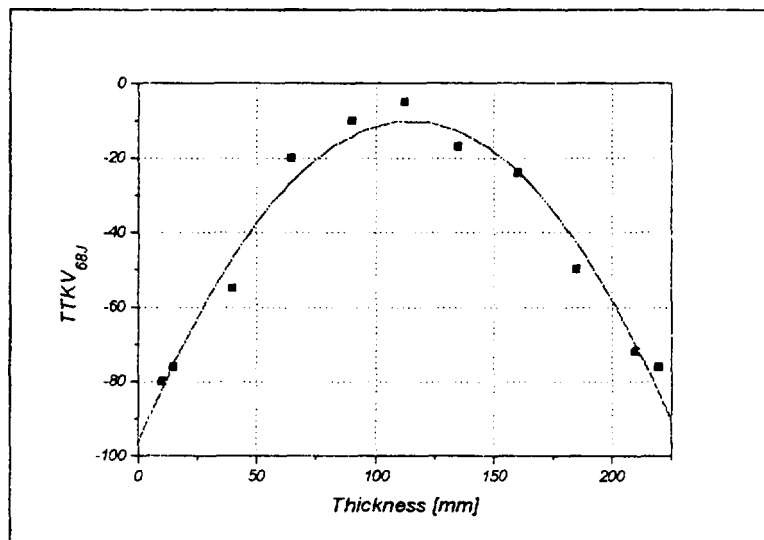


FIG. 3. TTKV temperature distribution in JRQ material as received

5.) THERMAL AGEING EFFECTS

It is widely discussed whether thermal ageing exists at the operational temperature of the nuclear units or not. In the literature different results can be found about the effect of low-temperature ageing. Some results show that there is no thermal ageing effect below 400 °C. Others expect the effect to manifest itself even at such a low temperature as 250 °C. To separate the low temperature thermal embrittlement from the scatter of the measurements a large database is also needed.

As an example a thermal ageing study on 15H2MFA steel is introduced. The purpose of the study was to understand the low-temperature thermal ageing, and to check whether thermal ageing occurs at the operating temperature of the reactor. The normal operating temperature for reactor walls is 270-300 °C. To simulate 80000 hours of ageing at 270 °C we used accelerated ageing at 350 °C, and the calculated ageing time was 2000 hours. The ageing process was paused at weekends due to safety reasons. The numbers of the coolings and heatings up are near to the stoppage numbers of nuclear units after 10 years (80000 hours) of service. This fact renders the test conditions even more realistic.

5.1 Metallography testing

Several metallography specimens have been cut from the original and aged materials. Some are shown in Figures 4-5.

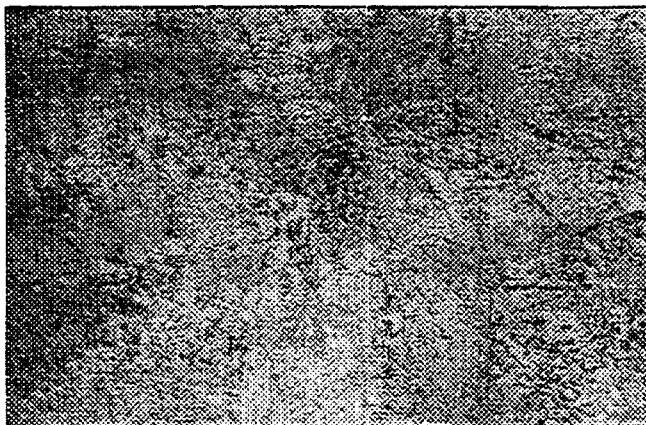


FIG. 4. 15H2MFA steel as received. The sample was cut in 10 mm depth. Magnification 350 x .

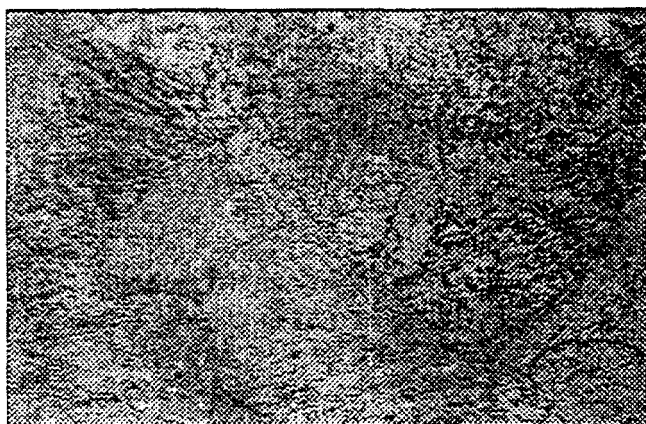


FIG. 5. 15H2MFA steel after 2000 hours ageing at 350 °C. The sample was cut in 10 mm depth. Magnification 350 x

The results of the metallography test have shown, that the grain structure of the 15H2MFA steel samples aged at 350 °C show only slight changes compared with the original material samples. After ageing the grain boundaries became slightly thicker, showing the initiation of the diffusion processes. The same effects can also be observed on the electron microscope pictures. The microsonde analysis didn't reveal significant changes at the grain boundaries, showing that the rate of the segregation at the boundaries, and even the thickness of the grain boundaries is small enough to be beyond the sensitivity of the microsonde.

5.2. Impact testing

Transition temperature change was measured in the function of the material thickness (depth) on 15H2MFA steel in as received condition. The measurements were repeated on aged 15H2MFA steel.

Tangent hyperbolic fitting was applied to calculate the NDT (Nil-Ductility-Temperature) values and 68 Joule energy criterion as well as 0.9 mm lateral expansion criterion were used. During the tangent hyperbolic fitting artificial points were used at -200 °C. The tests have been evaluated in the function of thickness. FIG. 6-7 shows the fitted tangent hyperbolic curves in the different layers of the 15H2MFA steel in aged and as received condition. FIG. 8. shows that the NDT temperature increased only slightly in the middle section of the forging, but considerable thermal ageing could be observed in the near surface layers.

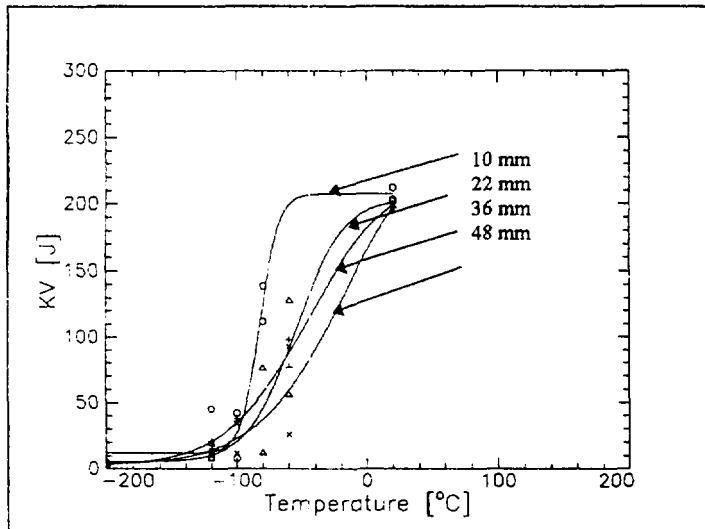


FIG. 6. Tangent hyperbolic fit on impact energy results of 15H2MFA forging at 10, 22, 36, 48 mm depth

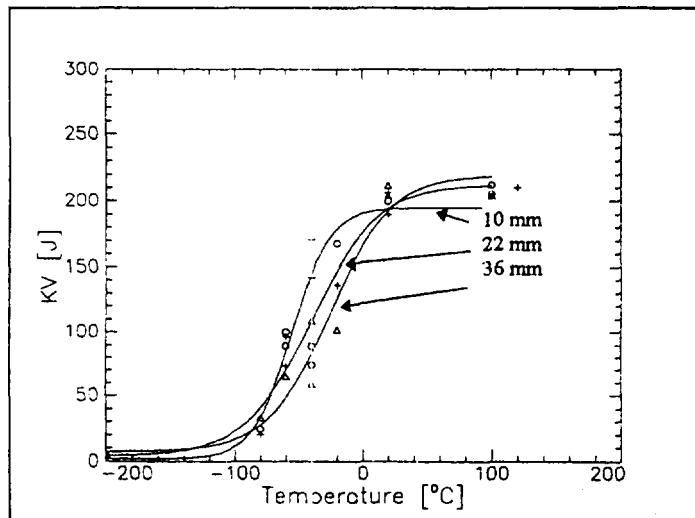


FIG. 7. Tangent hyperbolic fit on impact energy results of 15H2MFA forging aged 2000 hours at 350°C at 10, 22, 36 mm depth

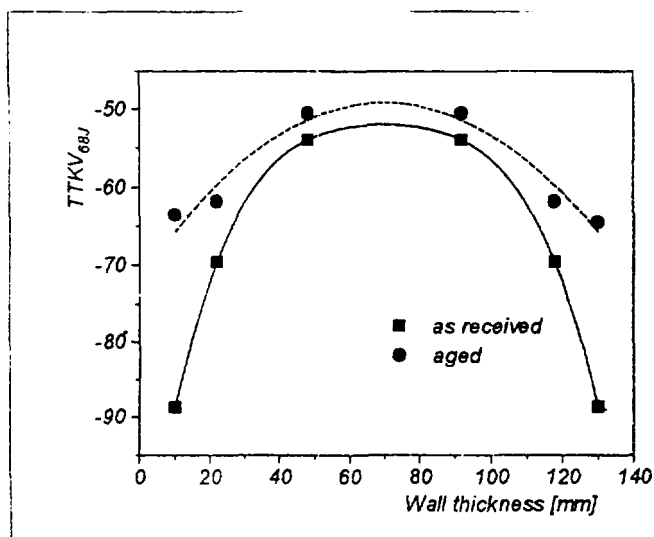


FIG. 8. $TTKV_{41J}$ temperature distribution in as received and aged 15H2MFA steel.

At the same time the near surface layer of the material has higher toughness even in aged condition than the middle zone. This gives a considerable reserve at evaluation of stability of near surface cracks, like cladding cracks, or undercladding short cracks which are typical in certain generation of the RPV-s.

6. CONCLUSION

There are several practical tasks which can only be completed by using large databases. Three different examples have been shown, where material data for PTS analysis have been obtained from databases, or even where database analysis resulted new knowledge on material behavior. These knowledge is very important for PTS or lifetime calculations.

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Session 7

7 MAY, WEDNESDAY

Material degradation, PTS modelling

Nahm, S. H. Kim, A. Yu, K. M. Kim, S. C.	Korea	Toughness Degradation Evaluation of Low Alloyed Steels by Electrical Resistivity
Martins, G. P. Terra, J. L. Lorenzo, R. Pirfo, S. Mansur, T. R.	Brazil	Elaboration and Verification of a PTS Model
Roos, E. Eisele, U. Stumpfrock, L.	Germany	Transferability of Results of PTS Experiments to the Integrity Assessment of Reactor Pressure Vessels



Toughness Degradation Evaluation of Low Alloyed Steels by Electrical Resistivity

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Abstract Remaining life of turbine rotors with a crack can be assessed by the fracture toughness of the aged rotors at service temperature. DC potential drop measurement system was constructed in order to evaluate material toughness nondestructively. Test material was 1Cr-1Mo-0.25V steel used widely for turbine rotor material. Seven kinds of specimen with different degradation levels were prepared according to isothermal aging heat treatment at 630°C. Electrical resistivity of test material was measured at room temperature. It was observed that material toughness and electrical resistivity decreased with the increase of degradation. The relationship between fracture toughness and electrical resistivity was investigated. Fracture toughness of a test material may be determined nondestructively by electrical resistivity.

Key Words : Fracture Toughness, 1Cr-1Mo-0.25V steel, Degradation, electrical Resistivity

1. Introduction

During the service at elevated temperature, the power plant equipment materials suffer aging, temper embrittlement, radiation embrittlement so on. Such embrittlements take places in turbine rotor steel in service, which cause the decrease of toughness and strength. Therefore, the evaluation of the remaining life of turbine rotor must be based on the proper estimation of fracture toughness of its degraded material.

However, it is almost impossible to sample the specimen for the fracture toughness test without damage to the rotor. Consequently, the toughness evaluation of turbine rotor steel using means of destructive method is impractical. Thus the nondestructive evaluation technique for the material toughness need to be devised. Although various nondestructive methods have been studied, the development of nondestructive technique to estimate the degradation of toughness

quantitatively has not been completed yet[1-4].

Electrical resistance can be used as a nondestructive measurement method. The electrical resistance is changed mainly due to microstructural change such as precipitation of carbide, decrease of solid element within matrix, the change of potential density, and the size and distribution of macroscopic defects such as void or crack[5]. Although the electrical resistivity variation of the steel is relatively small to be less than a few micro ohm-cm, it can be discerned by a high resolution measuring equipment. Particularly, this method would be useful at initial aging stage of steel because the level of creep damage is hardly discerned by microscopy[6].

In this study, 1Cr-1Mo-0.25V steel was heat-treated at 630°C for various times which was higher than the service temperature and lower than the tempering temperature. The effect of heat-treatment, which simulates the microstructures of long term served materials, on electrical resistivity was investigated. The applicability of this method to the evaluation of rotor safety was discussed. Finally, the estimation of FATT using a single impact specimen was discussed.

2. Experimental

Test material is 1Cr-1Mo-0.25V steel which is employed as a steam turbine rotor material. Chemical composition and mechanical properties are described in reference 7. Unused materials were heat-treated for 448, 894, 1340, 1832, 3640, 5460 hours respectively at 630°C. This is to simulate the microstructures of long term served materials at elevated temperature because of difficulty to sample the aged materials on site[8]. Thus seven kinds of specimens with different microstructures were prepared.

Electrical resistivity was measured by a direct current four-terminal potential method. Figure 1 is the schematic diagram of experimental setup. A constant current was supplied to specimen by using a DC current source (Fluke 382 A. the maximum capacity 5A) to 0.05% accuracy via standard resistor of 0.01Ω. The potential drop of specimen was measured down to 1nV by a nanovoltmeter(Keithley 181) to 0.1% accuracy.

The sheet type specimen of 55 mm in length, 5 mm in width and 1 mm in thickness was used for measuring electrical resistivity. The chromel thermocouple wire of 0.5 mm diameter was used for the voltage measurement lead and the copper wire of 7 mm diameter for the current input lead. Two current input leads were screwed at the end of the specimen and two voltage measurement leads were attached at the central part by spot-welding. In order to keep uniform potential drop or current density between two voltage measurement leads, it is recommended that the length between two voltage lead terminals might be 1.5 times longer than the circumference of the cross section area of the specimen.

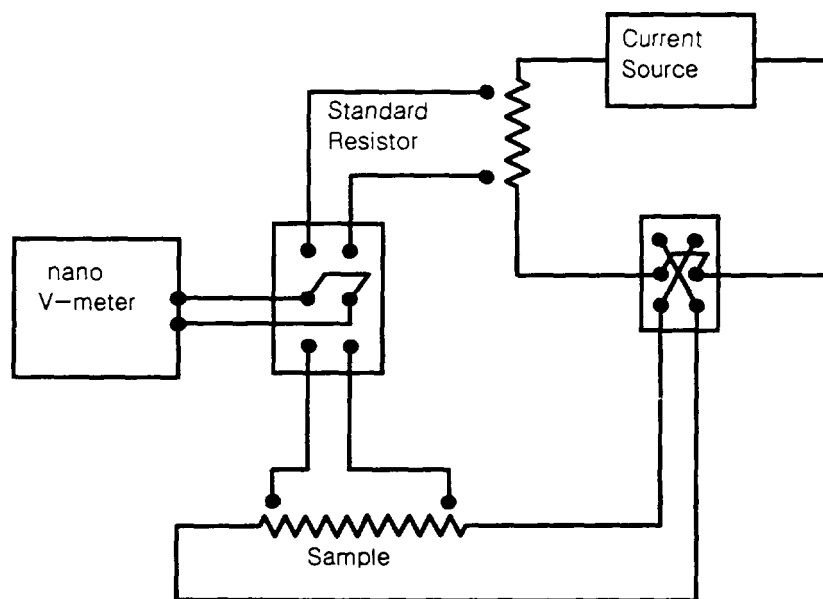


Fig. 1. Schematic diagram of experimental setup.

The change of temperature of the specimen might be a main factor having an effect on electrical resistivity[9]. It is desirable to keep the constant temperature during measuring electrical resistivity, because the change of electrical resistance of 1Cr-1Mo-0.25V steel due to degradation is within a few percent. In this study, electrical resistivity was measured under the test temperature whose range of fluctuation is within 0.5% to 24.0°C in a constant temperature thermostat.

Among various kinds of factors affecting the measurement of electrical resistivity, the specimen size and the voltage output lead location might be considerable. Thus a micrometer was used to measure the accurate size of specimen and a traveling microscope was used to locate accurate positions of voltage output leads in this study. The current direction for the voltage measurement of each specimen was altered (i. e. forward and backward) to reduce the effect of thermoelectromotive force due to the change of temperature of contact point giving rise to quite big error. The average value of voltage gained from forward and backward direction was used. The same grounding was also used to minimize the error of noise signal caused by complementary signal.

3. Results

3.1 Variation of electrical resistivity on aging time

Figure 2 shows the change of electrical resistivity depending on aging time. Electrical resistivity measured for unaged material is $24.8 \sim 27.2 \mu\Omega\text{cm}$, and that measured for 300,000 hours simulated aging time material is $22.5 \sim 25.1 \mu\Omega\text{cm}$. The average value of electrical resistivity changes from $23.8 \mu\Omega\text{cm}$ to $26.0 \mu\Omega\text{cm}$. Even though electrical resistivity is measured under the same testing condition, its scatter is roughly $2.0 \sim 2.5 \mu\Omega\text{cm}$. It is not clear what makes the scatter. However it can be considered that the variation of test temperature and the measurement error of the distance between measurement leads have effect on that.

The distance between two voltage measurement leads should be measured carefully. The measurement error of 0.05 mm about the distance between two voltage measurement leads gives rise to the error of $0.13 \mu\Omega\text{cm}$. As the aging time increases, the value of electrical resistivity decreases significantly in the short aging time. However the change became smaller in long aging time. Finally, the change of electrical resistivity couldn't be discerned after the aging time is longer than about 50,000 hours.

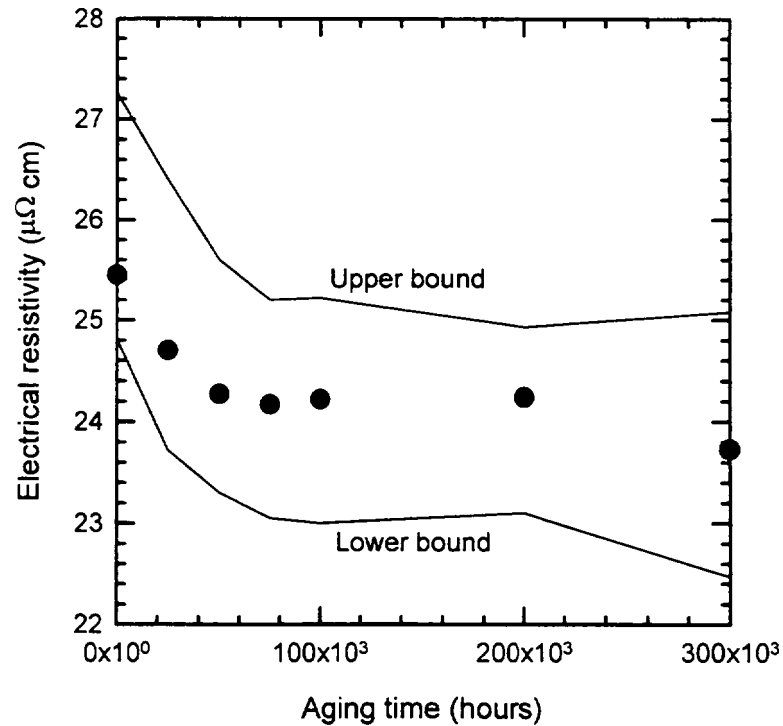


Fig. 2. Dependence of electrical resistivity on ageing heat treatment time according to various testing method.

3.2 Decrease of mechanical properties by degradation

Figure 3 shows the dependence of Vickers hardness and FATT on aging time. The trend of hardness depending on aging time looks similar to that of electrical resistivity. However, the value of hardness decreases more rapidly in the short aging time and the change becomes slower in the long aging time.

○ symbol and □ symbol in Fig. 3 represent hardness which is obtained by different testing machines and operators. In spite of the same specimen, the measured hardness is dependent on testing machines and operators. Therefore, to estimate the degradation of the material using hardness requires careful consideration of hardness measurement.

Figure 3 shows the change of FATT due to the change of aging time. FATT is dependent on aging time. Maximum FATT is obtained in the specimen heat treated for 894 hours at 630°C equivalent to 50,000 hours service time at 538°C. If the measurement error is taken into account, it is presumed that FATT is not changed over 50,000 hours. According to previous results[1], the degree of degradation is maximized at 40,000 hours in case of turbine rotor steel. Consequently, FATT doesn't change after that. these results agree with the previous results.

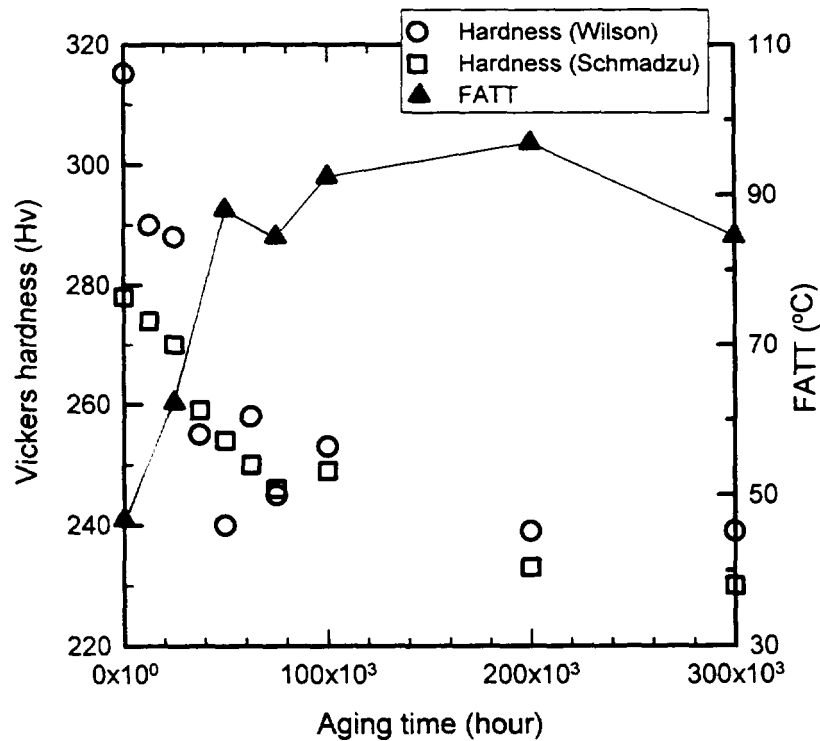


Fig. 3. Dependence of Vickers hardness on aging heat treatment time.

4. Discussion

4.1 Relation between electrical resistivity and Vickers hardness

Figure 4 shows the relationship between electrical resistivity and value of Vickers hardness. As shown in Fig. 2 and Fig. 3, as the aging time gets longer at initial stage, the hardness and electrical resistivity become smaller. If the aging time become over 50,000 hours, the electrical resistivity doesn't change in spite of the change of hardness. Thus, it is presumed that hardness method is superior to electrical potential method to evaluate the life of the equipments served for a long time.

The relationship between electrical resistivity and hardness could be expressed as equation.

$$H_v = A_1(\rho)^2 + B_1(\rho) + C_1 \quad (1)$$

where $A_1 = -8.79$, $B_1 = 474.87$ and $C_1 = -6.13 \times 10^3$. It is possible to estimate Vickers hardness out of electrical resistivity by using equation (1). Therefore, even if the only electrical resistivity is available, the remaining life can be still estimated from the existing relation of hardness and life estimation parameter such as G[10].

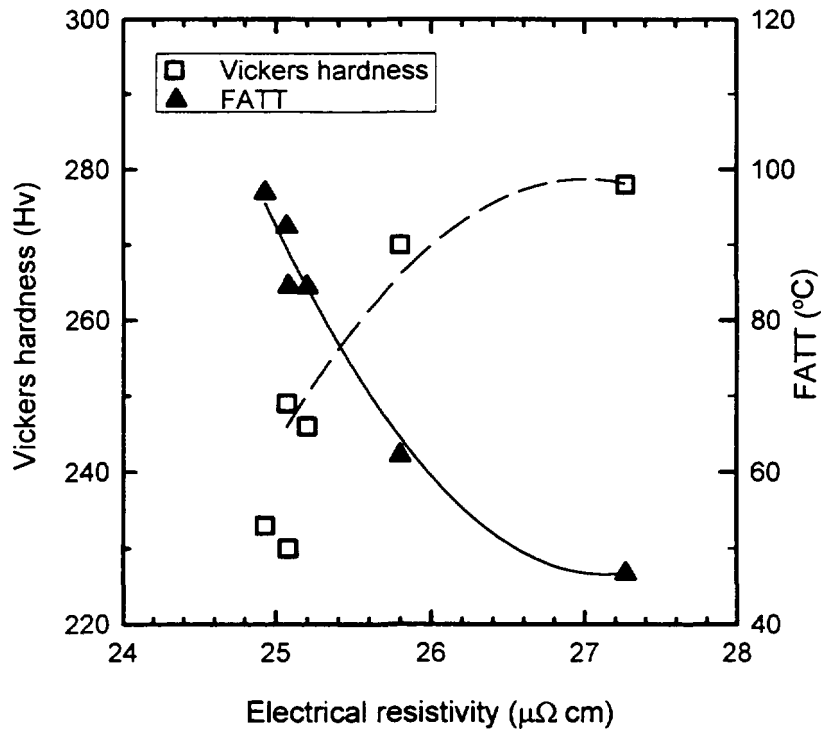


Fig. 4. Relation between electrical resistivity and Vickers hardness.

4.2 Relation between electrical resistivity and FATT

Figure 4 shows the relationship between electrical resistivity and FATT. Electrical resistivity decreases considerably while FATT increases considerably at initial stage. This relation can be used to estimate FATT out of electrical resistivity. It is difficult to evaluate the remaining life of material served for a long time using the relation of electrical resistivity and FATT when the aging time is over 50,000 hours because FATT and electrical resistivity doesn't nearly change. Thus Electrical resistivity is related to FATT. The relation of electrical resistivity and FATT obtained in this study is as follows:

$$FATT = A_2(\rho)^2 + B_2(\rho) + C_2 \quad (2)$$

where, $A_2=10.02$, $B_2=-574.75$ and $C_2=7.48 \times 10^3$

4.3 Relation between electrical resistivity and fracture toughness

Fracture toughness, K_{IC} , can be deduced easily from the measured electrical resistivity value, if the existing correlation[11-13] of FATT and fracture toughness, K_{IC} is utilized. In this study, fracture toughness was deduced by using Jones' equation[13] as follows.

$$K_{IC} = 10,800 / [108 - (T - FATT)] \quad (3)$$

where the unit of K_{IC} is ksi in^{1/2}, the unit of T and FATT is °F.

Figure 5 represents the dependence of K_{IC} on the excess temperature (test temperature - FATT). The scatterband of the data is referred to reference 11. These data can be used to evaluate toughness because the results obtained in this study is within the scatterband presented by other researchers. In order to evaluate the toughness of turbine rotor in service by the nondestructive method, the electrical resistivity must be measured at first and then FATT can be estimated from the correlation of electrical resistivity and FATT (as shown in Fig. 4). K_{IC} can be estimated by using the correlation of excess temperature and K_{IC} .

Figure 6 represents the relationship between electrical resistivity and K_{IC} calculated using equation (3). As the aging time gets longer, the variation of electrical resistivity and fracture toughness is considerably large until 50,000 hours simulated aging time. However electrical resistivity and FATT don't change after the time. So the values of K_{IC} calculated from FATT are almost constant. Thus electrical resistivity has a good relation with material toughness and the relation is expressed by equation (4).

$$K_{IC} = A_3(\rho)^2 + B_3(\rho) + C_3 \quad (4)$$

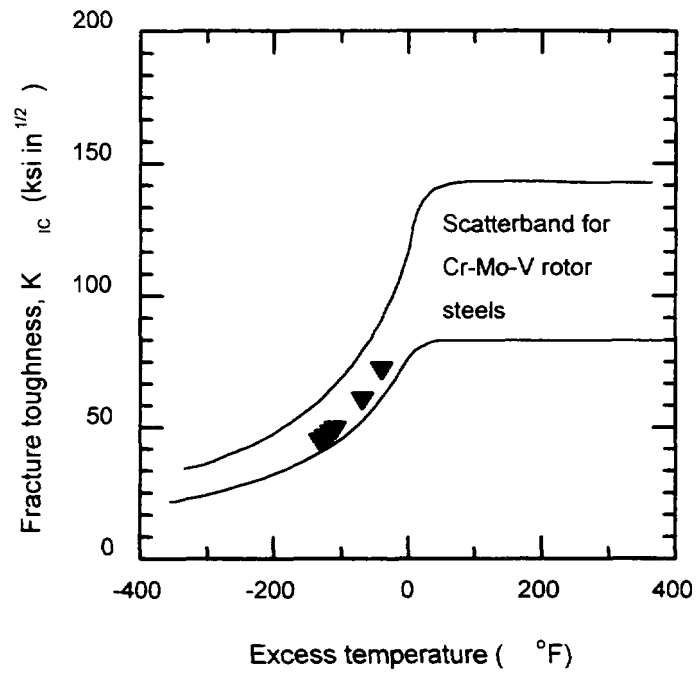


Fig. 5. Relation between excess temperature and fracture toughness.

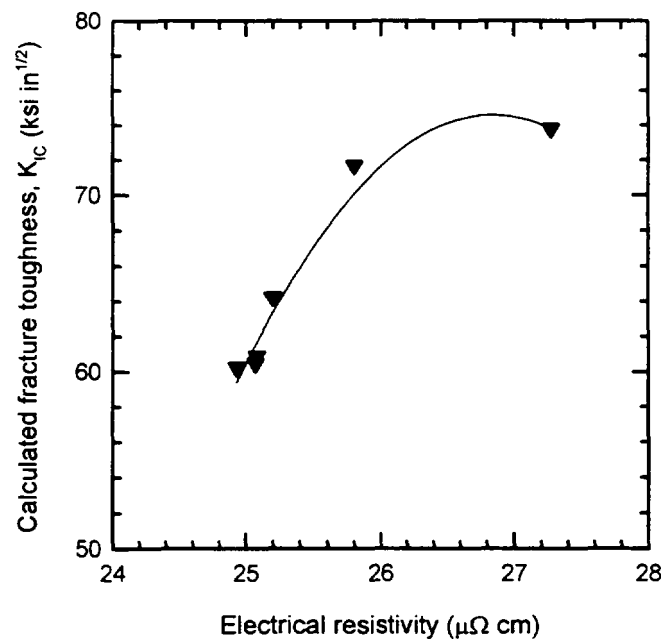


Fig. 6. Relation between electrical resistivity and calculated fracture toughness.

where $A_3=10.09$, $B_3=-547.75$ and $C_3=7.48 \times 10^3$

4.4 Estimation of FATT by a single impact specimen

Figure 7 shows the relationship between excess temperature and absorbed impact energy. Excess temperature can be obtained from FATT. The data can be represented by a specific function because most data are distributed within a small scatterband. Therefore, the absorbed impact energy can be deduced from the FATT value estimated by the electrical resistivity. In addition, if Fig. 7 is used, FATT can be estimated from the impact test result using a single specimen.

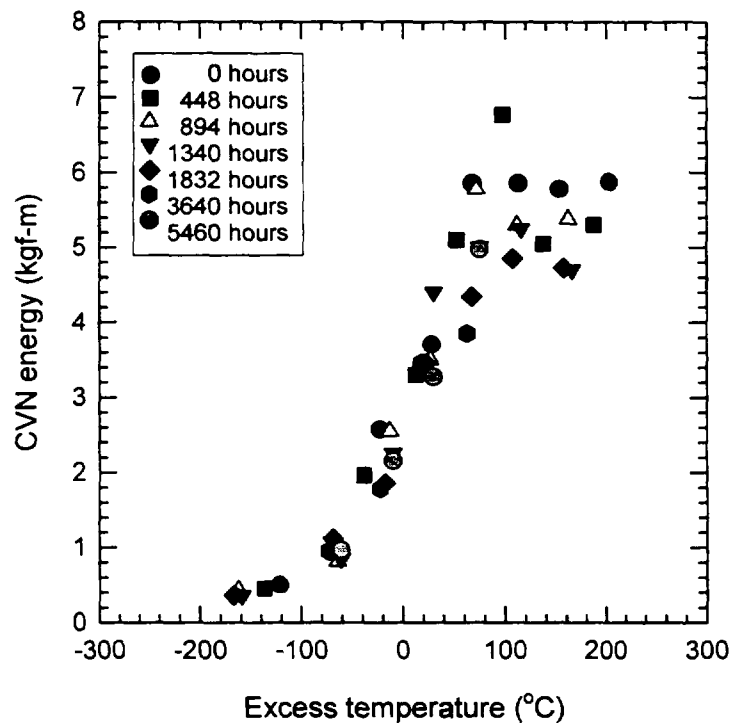


Fig. 7. Relation between excess temperature and Charpy impact energy.

Conclusion

Nondestructive method for evaluating fracture toughness of turbine rotor steel was studied using the characteristics of electrical resistivity of materials. Even though the data are not enough, the applicability of nondestructive method to the evaluation of material toughness can be concluded as the following:

- (1) Electrical resistivity decreases with the increase of aging time at early stage. However its decrease doesn't appear over 50,000 hours simulated aging time.
- (2) Hardness roughly decreases with the increase of aging time. The tendency of decrease of hardness still exists over 50,000 hours simulated aging time. Thus it is estimated that hardness method is more appropriate to evaluate the remaining life of the equipment in long service than electrical potential method.
- (3) FATT decreases with the increase of aging time at early stage. However, it doesn't increase over 50,000 hours simulated aging time. So the electrical resistivity is related closely to the material toughness of 1Cr-1Mo-0.25V steel
- (4) FATT can be estimated using a single impact specimen and thus material toughness can be evaluated from the relation of FATT to K_{IC} .

Acknowledgement

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ELABORATION AND VERIFICATION OF A PTS MODEL

by



XA9744643

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Abstract

This work concerns to pressurises thermal shock (PTS) phenomena, it is rapid cooldown of the primary system by low temperature water injection from the emergency core cooling system (ECCS). To assess the effect of a PTS event calculations and experimental work are equally necessary. The purpose of the present was to prepare the PTS analysis of Angra II RPV located near to Rio de Janeiro. This paper presents the method and the results of the calculations used to design an optimal RPV model.

Keywords: PTS modelling, stress and temperature distribution, RPV

Introduction.

The primary reactor circuit is important key on the nuclear power plant safety. It is submitted to different thermal and mechanical loading during normal operation. During normal operation the cooling water temperature and the PWR wall temperature differs only a few degree Celsius. The design stress analysis calculates the safety factors during different operational modes, and fracture mechanics is used to evaluate the stability of the real and hypothetic flaws. In case of the operation of ECCS the difference between the vessel wall and the coolant temperature can be as high as 270°C. The stress, strain and temperature distribution -as well as the material toughness- change dynamically. The most extreme example of this process is the PTS event, which is the highest load an RPV must survive without losing its integrity even at the end of the lifetime, when the core zone toughness is degraded by thermal and irradiation effects. This dynamic stress and strain changes can be analysed only by using sophisticated codes. These codes must be verified by model experiments.

A test facility for PTS study is under development at CDTN (Nuclear Technology Developing Centre).

It will model the ANGRA II RPV. The main features of it:

Table 1 - Angra II PV

Dimensions	Values on mm
Internal diameter	5000
Thickness	250
Length	9748
Material	22NiCrMo37

The design steps of the vessel model is presented in this work.

The test facility

The test facility to be constructed will be similar than the facility used in Finland Experiment constructed at Prometey Institute/Russia[1,2]. The vessel model will be heated electrically e.g. by resistor shielding, the model will be placed inside a pool upon its one face. The heating system is removed and cooling water flow from storage tank will cool down the model from outside. The cooling water storage tanks will be in elevated position, the water will flow by its potential energy. During heating and cooling periods the model temperature and stress distribution will be measured by thermocouples and high temperature strain gauges.

Design of the model vessel

Basic design requirements

The model was designed to satisfy the following requirements:

- maximum available similarity with the RPV of ANGRA II Station (dimensions are given in table 1.
- use the world-wide collected experience on PTS modelling
- to produce easily measurable temperature and stress transients (High rate of stress changes needs expensive data acquisition systems)
- no plastic deformation should occur in the model during the tests. In case of plastic, or even non linear elastic deformation (out of the validity of the Hooke's law) the strain gauge measurements gives false results. Therefore the highest stresses should be limited to yield point
- keep the construction and operational costs low
- using steel available at the Brazilian foundry
- the size of the model should be beyond the machining capabilities of CDTN

Calculation of the optimal model geometry.

On the base of the above mentioned requirements a structural steel SAE 8620 was selected as model material. The properties of this steel are given in table 2.

The model maximum temperature were selected according to the operational temperature of Angra II, while the cooling water temperature follows the average weather of Belo Horizonte, where the facility will be located.:

- the model vessel max. (heated) temperature: $T_h = 300^\circ\text{C}$
- cooling water injection temperature: $T_c = 30^\circ\text{C}$

Table 2 - Material properties

Yield stress (MPa)	400
Thermal Conductivity Coefficient (W/m °C) K	36 *
Young Modulus E (GPa) *	200

* High temperature value

The requirements and other boundary conditions limited the model diameter max.: 500 mm and the length max.: 1000 mm. The only free dimension during the model design became the wall thickness.

To find the optimum value of the wall thickness 7 different calculation was performed. The minimum calculated wall thickness was 21 and the maximum was 126 mm[3].

Calculation of thermal stresses..

The ACIB-RPV *Analytical Calculation of Integrity of Beltline of Reactor Pressure Vessel* code was used to calculate the temperature and stress distribution at Atomic Energy Research Institute - AEKI - at Budapest/Hungary. This is an analytical code using similar equations then the US NRC code VISA[4].

The program inputs are: Young Modulus E , heating T_h and cooling temperature T_c , thermal conductivity coefficient K , heat transfer coefficient in the function of the temperature, pressure, wall thickness and cladding parameter. Since the model will not be pressurised, the pressure value was 0 during all calculations. The model vessel is not cladde therefore the cladding parameters are the same than base steel and thickness near zero value was used. (For mathematical reasons 0 value is not allowable in the code)

The dimensionless Biot-Savat number was calculated for Angra II vessel, and this value became used for the model calculations to ensure the conservatism during the model design..

Two heat transfer coefficients were used. When the model surface temperature is over 100 C boiling is supposed resulting a increased value.

Table 3- Heat transfer coefficients used

Temperature (°C)	0 to 100°C	100 up to 300°C
Surface Heat Transfer Coefficient - h (W/ m ² . °C)	5.000	30.000

The electrical furnace uniformly heat up the model, and the cooling water is cover rapidly the outer surface too. This aloud to use a simple one dimensional calculation, neglecting the temperature differences along the model height.

The temperature distributions during the model cooling were calculated periodically. From the results cooling diagrams was elaborated at the outer surface and at the location 12 mm distance from the inner surface . Three of these calculated cooling diagrams are shown on Fig. 1-3.

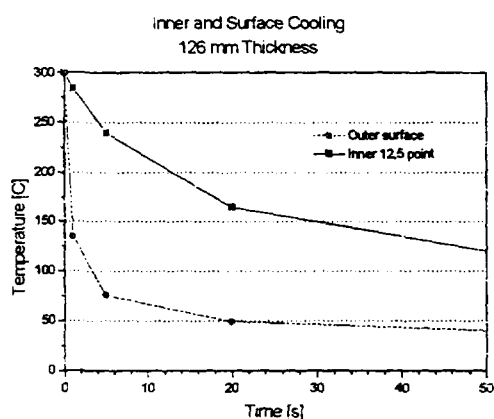


Figure 1 - Cooling diagram of the model with 126 mm wall thickness. Locations:
a.) Outer surface
b.) 12 mm from the inner surface

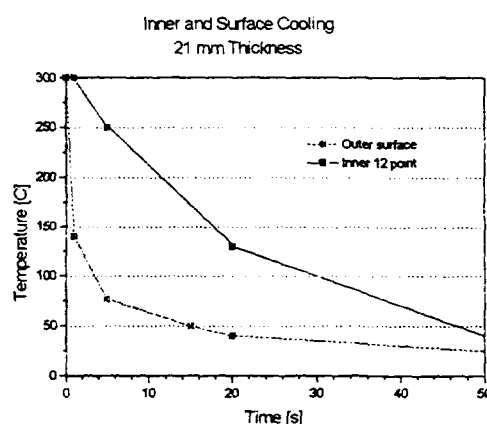


Figure 2 - Cooling diagram of the model with 21 mm wall thickness. Locations:
a.) Outer surface
b.) 12 mm from the inner surface)

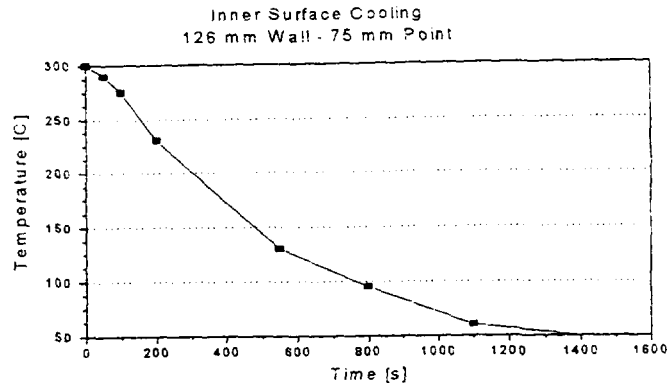


Figure 3 - Cooling diagram of the model with 75 mm wall thickness. Locations:

During the first 10-15 seconds the temperature distribution is independent from the model wall thickness in the range considered. After this initial period the different models shows different temperature gradient, and as consequence the stress distribution is also become different

Table 4-6 give some examples how the wall thickness affect the cooling rate of the outer surface.

Table 4- Model surface temperature after 50 seconds cooling

At 50 seconds		
Thickness (mm)	21	126
Temperature (°C)	24	38

Table 5- Time to reach 40 C surface temperature

to 40 °C temperature		
Thickness (mm)	21	126
Time (sec.)	25	40

Table 6- Necessary time interval to reach uniform temperature distribution along the wall thickness

To uniform temperature along wall thickness		
Thickness (mm)	21	126
Tempo (sec.)	60	1700

The stress analysis has shown that the difference between the maximum values of the compression and tension stresses are increasing with the model thickness. Optimum wall thickness can be chosen according to the surface stress level required for good measurement.

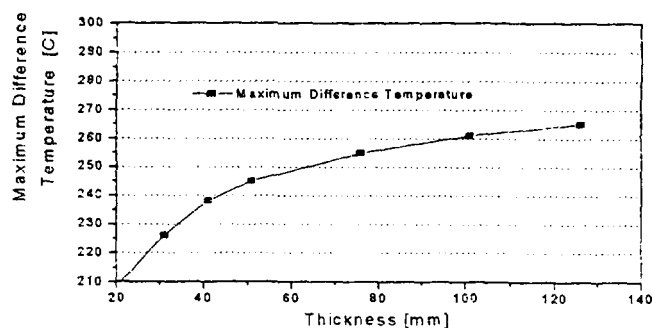


Figure 4 - Maximum difference between outer and inner surface temperature.

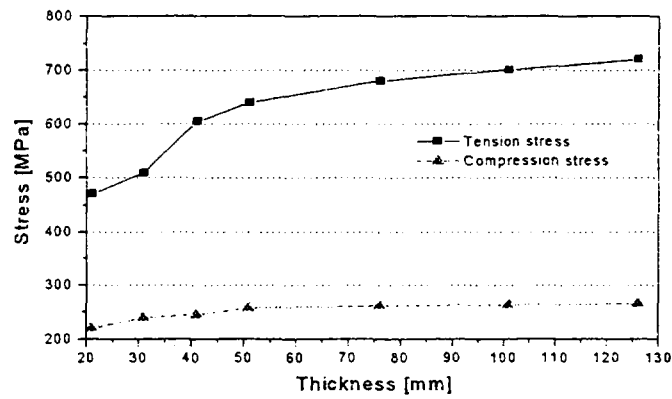


Figure 5 - Maximum stress values calculated on differently thick models. The max tension stress occurs at the outer surface and maximum compression stress occurs at the inner surface.

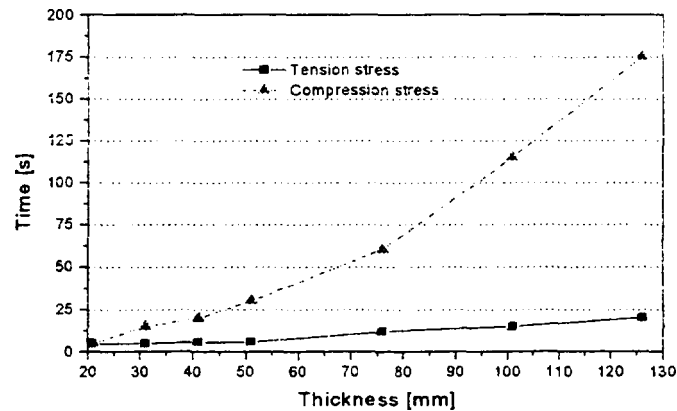


Figure 6 - Time to the peak stress values in case of different models

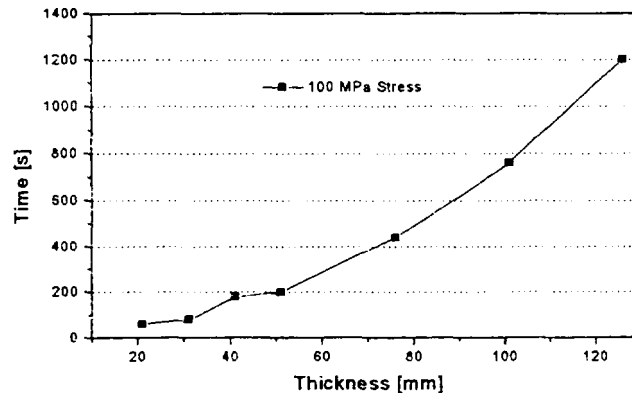


Figure 9 - Reaching time to 100 MPa stress in the function of the model wall thickness

Figs 6 and 7 shown, that the peak stress values are increasing rapidly with the thickness up to 50 mm. Increasing thickness over 50 mm has smaller effect, that is the unlimited increase of the model diameter has no practical advantage.

Conclusion

a) The optimised model dimensions will be: 1000 mm length, 500 mm external diameter, 330 mm inner diameter (85mm wall thickness). The surface stress transients easily can be measured by strain gauge at this thickness value.

b) SAE 8620 steel needs to be thermally treated to achieve high yield strength value on the surface to avoid plastic deformation.

The suggested heat treatment: heating up to 850- 950 C, following water cooling to ambient temperature. The resulted yield point will be approximately 800 MPa which is appropriate to the tests]

c) Model can be used for crack initiation and arrest studies by repeating the test cycles

d) Finally thermal fatigue ageing can be evaluated by mechanical testing of the model material.

d) The model have been designed to test at 300°C initial vessel temperature and 30°C cooler temperature. The maximum stress in this case will be 680 MPa. The model can be operated without previous thermal treatment of the steel by decreasing the initial temperature or increasing water temperature.

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Transferability of Results of PTS Experiments to the Integrity Assessment of Reactor Pressure Vessels

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Abstract

The integrity assessment of the reactor pressure vessel (RPV) is based on the fracture mechanics concept as provided in the code. However, this concept covers only the linear-elastic fracture mechanics regime on the basis of the reference temperature RT_{NDT} as derived from charpy impact and drop-weight test.

The conservatism of this concept was demonstrated for a variety of different materials covering optimized and lower bound material states with regard to unirradiated and irradiated conditions.

For the elastic-plastic regime, methodologies have been developed to describe ductile crack initiation and stable crack growth. The transferability of both, the linear-elastic and elastic-plastic fracture mechanics concept was investigated with the help of large scale specimens focusing on complex loading situations as they result from postulated thermal shock events for the RPV.

A series of pressurized thermal shock (PTS) experiments were performed in which the applicability of the fracture mechanics parameters derived from small scale specimen testing could be demonstrated. This includes brittle (static and dynamic) crack initiation and crack arrest in the low charpy energy regime as well as stable crack initiation, stable crack growth and crack arrest in the upper shelf toughness regime. The paper provides the basic material data, the load paths, representative for large complex components as well as experimental and theoretical results of PTS experiments. From these data it can be concluded that the available fracture mechanics concepts can be used to describe the component behavior under transient loading conditions.

1 Introduction

In Germany the procedure for the integrity assessment of the reactor pressure vessel (RPV), the material requirements and the loading conditions to be regarded are fixed e.g. in KTA [1].

In the safety assessment of the RPV the incredibility for initiation of a brittle fracture has to be demonstrated throughout the entire life-time for all service loading conditions including postulated accidents. Basis for the safety analysis is a semi-elliptical flaw in the cylindrical shell of the RPV. Since this assumption is most challenging, the fracture mechanics understanding of material behaviour and the necessary concepts and tools will be discussed in this paper with respect to PTS application. Besides sufficient material ductility which has to be proved by Charpy-V-notch impact tests, the analysis may be based on fracture

toughness on the basis of the fracture mechanics concept of the Code or on fracture mechanics parameters experimentally determined with material of the component.

2 Technical aim

According to the Code [1,2] the exclusion of brittle fracture has to be assured for the reactor pressure vessel of a nuclear power plant under all service and accident conditions. For the analysis a crack has to be postulated that is perpendicular to the maximum principle stress. For conditions A and B acc. to KTA the postulated flaw is assumed to be a surface flaw with a depth of $0.25 \times$ wall thickness and an effective length of $1.5 \times$ wall thickness. For conditions C and D the flaw dimension to be assumed is twice the size that can be detected by advanced non destructive examination.

The loading of the crack is characterised by the stress intensity factor K_I . Failure by brittle fracture is avoided if K_I is lower than the corresponding material parameter K_{Ic} . The appropriate material parameter can be selected from the K_{Ic} curve. In case that crack initiation cannot be excluded, crack arrest has to be proved before the crack penetrates 75 % of the wall thickness. For characterising crack arrest a large number of linear-elastically determined dynamic fracture mechanics data (K_{Id}) and crack arrest data (K_{Ia}) were evaluated and presented in [3] that lead to the K_{Ia} curve which is the basis of the reference curve K_{IR} of the ASME and the KTA Code. The tests were performed with materials typical for nuclear application. The individual material properties are implemented by normalising the fracture toughness data to the nil ductility transition temperature T_{NDT} as recommended by Pellini 1969 [4] and an early edition of [2]. In later editions of the ASME Code and in the KTA Code the reference temperature RT_{NDT} was chosen as normalising temperature instead of T_{NDT} . The assessment of safety against brittle failure in case of flaws detected in service is performed in a similar way, considering first the case of crack initiation.

3 Fracture Mechanics Parameters

The procedure of determining fracture mechanics parameters experimentally is described in detail in the common Standards. There exist Standards according to the material behaviour for the linear-elastic and for the elastic-plastic regime as well.

3.1 Linear-Elastic Fracture Mechanics Parameters

The linear-elastic fracture mechanics parameter is the fracture toughness K_{Ic} . The K_{Ic} value is characteristic for the initiation of a brittle fracture. The procedure to determine K_{Ic} values is given in the U.S. Test Standard ASTM E 399 [5], in the British Standard BS 5447 [6] and in the ISO/DSI Draft 12 737 [7]. All three Standards are essentially identical. The Standards contain criteria to assure linear-elastic behaviour up to fracture and to meet the requirements for plane-strain conditions. The K_{Ic} values determined by that procedure are supposed to be size independent.

3.2 Elastic-Plastic Fracture Mechanics Parameters

In the region of elastic-plastic material behaviour the crack tip opening displacement (CTOD) and the J-Integral can be used as fracture mechanics parameter, as well. With these parameters it is possible to determine the crack resistance of a material experimentally. Plotting the J-Integral vs the amount of stable crack extension leads to the "crack resistance curve" of the material. From the crack resistance curve crack initiation values can be derived according to the following Standards and drafts:

- ASTM E 813 (J_{Ic}) [8]
- European Structural Integrity Society: ESIS P1/P2 (J_i , $J_{0.2}$, $J_{0.2bl}$) [9]
- Deutscher Verband für Materialprüfung: DVM ($J_{0.2bl}$, J_i) [10]

At MPA Stuttgart, since several years a procedure is applied additionally to the procedures mentioned above that is based on J_i values derived from the crack resistance curve and the

"stretched zone" [11 - 15], since the crack resistance curve (J_R -curve) itself is not a material law. This procedure has been implemented also in the European recommendation ESIS P2 for the determination of fracture mechanics parameters.

As shown in [13, 14, 15] the J_{Ic} , $J_{0.15}$, $J_{0.2}$ and $J_{0.2/bl}$ values may be used as technological parameters which are suitable for a qualitative comparison of different materials. For characterising the physical process of crack initiation, however, only the effective crack initiation value can be used since this parameter is independent of specimen size and geometry and is not influenced by the stress state in the structure.

The crack initiation value J_i [14] is based on the determination of the "stretched zone" Δa_i . This is the region at the crack tip of large plastic deformation ("blunting") before the event of stable crack extension. The physical background of this procedure is the fact that after the process of blunting, the stretched zone has reached its final size which is sustained during the complete process of stable crack growth. Tests with specimens of different amount of stable crack extension confirmed this assumption [16, 17]. After the test, the stretched zone can be determined in a perpendicular projection of the crack plane with the help of a scanning electron microscope.

For the quantitative determination of the crack initiation value J_i , that point of the J_R -curve (J - Δa -curve) is selected for which Δa equals Δa_i . To obtain a reliable J_i value high performance of the measuring technique is required so that sufficient reliable data are provided in the region of small Δa values that have to be fitted by an adequate polynomial. The dense covering of the J_R -curve with data pairs in the region of small Δa values is the main feature of the procedure developed at MPA Stuttgart in comparison with other procedures. Fig 1 shows the variety of different initiation values as they were determined by various procedures. It can clearly be seen that only J_i is not effected by significant amount of stable crack growth.

3.3 Crack arrest toughness

The procedure to determine crack arrest toughness values for ferritic materials is described in ASTM E 1221 [18]. The crack is initiated under a given amount of energy and while the crack propagates, the stress intensity decreases. The K_{Ia} -value is the stress intensity at the crack tip after the crack has arrested. It is a substitute parameter but represents a lower limit that covers the dynamic crack arrest parameter K_{IA} conservatively [19]. To obtain valid K_{Ia} -values linear-elastic material behaviour and plane strain conditions are required as for static crack initiation values.

The unstable crack extension which is the required starting point to achieve crack arrest data is realised by initiating the crack in a high strength (brittle) weld bead. The determination of crack arrest data in the elastic-plastic regime is limited by the ductility of the material. Usually K_{Ia} values cannot be determined at temperatures $T > T_{NDT} + 60 \text{ K}$ because sufficient unstable crack extension before arrest cannot be assured by applying present test procedures.

4 Transferability of Fracture Mechanics Concepts and Material Parameters to Components

The transferability of fracture mechanics concepts and material parameters is verified when data derived from small scale specimen testing can describe the material behaviour of large complex structures and stress states. This means in particular that the concepts must reliably enable a prediction of crack initiation, regardless if brittle or ductile failure occurs, on the basis of K_{Ic} or J_i -values, respectively, and in the elastic-plastic regime the prediction of the amount of stable crack growth based on the J -integral.

4.1 Transferability to Mechanically Loaded Structures

In the frame of the research programme FKS crack resistance curves were determined on large scale specimens [12, 13]. The crack initiation values were determined according to the procedure applied at small scale specimens as described in chapter 3.

The K_{IJ} -values derived from J_i of specimens with different size and geometry (CT specimens with thickness $B \geq 100$ mm, single edge (SECT), double edge (DECT) and center cracked (CCT) tension specimens as well as three point bend (TPB) specimens with $100 \text{ mm} \leq B \leq 600 \text{ mm}$ and $2W = 200 \text{ mm}$) are plotted together with the scatter band of initiation values obtained for small scale specimens. For the three materials with different upper shelf Charpy energy (170, 90 and 40 J) it shows that the effective crack initiation value J_i is not depending on specimen size and geometry. This proves that J_i and the corresponding K_{IJ} -value can be applied to all structures, [fig. 2](#).

The general application of fracture mechanics parameters, however, is limited to the initiation value J_i . The crack resistance curves itself does not represent an universal material law but is characteristic for an individual specimen or component [20, 21,22]. [Fig. 3](#) shows crack resistance curves determined with the modified 22 NiMoCr 3 7 base material and specimens of different geometry and size.

It has to be assumed that specimens with little stable crack extension incorporate a distinct three dimensional stress state with the consequence of limited possibility for plastic deformation. Under extreme conditions, plastic deformation may be suppressed completely so that initiation is immediately followed by brittle fracture.

For the quantification of the three dimensional stress state in the ligament of a structure the

coefficient of multiaxiality $q = \frac{\tau_r}{\sigma_m}$

can be used [21, 23]. In this equation τ_r is the reduced stress acc. to Hencky and σ_m the mean stress.

Using the invariants, q can be expressed in the form of

$$q = \left(\frac{-9 J_2'}{J_1^2} \right)^{1/2}$$

with J_2' as the second invariant of the deviator and J_1 as the first invariant of the stress tensor. From a mechanistic point of view q describes the interaction of slip mechanisms characterised by J_2' and fission mechanisms characterised by J_1 . The highest degree of multiaxiality occurs in case of the hydrostatic stress state and leads to the value of $q = 0$.

The quantification of the stable crack growth is also possible by analysing the coefficient of multiaxiality across the ligament of a specimen or a component.

4.2 Transferability Thermally Transient Loaded Structures

The transferability of fracture mechanics concepts and material parameters to structures which are subjected to thermally transient load, was proved at MPA Stuttgart by means of thick walled hollow cylinders under thermal shock loading [24, 25, 26]. The results were compared with the predictions according to the Code.

The basic component and material behaviour will be demonstrated with the help of three experiments referred to as NKS 4-1, NKS 6 and NT3 out of a series of 8 experiments. Two specimens contained a circumferential crack the third one two partial circumferential cracks. Geometric and material parameters of the specimens are given in [table 1](#), (see also [fig. 4](#)).

Specimen	NKS 4-1	NKS 6 compound specimen	NT 3
material	22 NiMoCr 3 7 "modified"	MoV steel "special heat"	MoV steel „special heat“
Charpy upper shelf energy			
- base material (b.m.)	60 J	30 J	70 J
- weld metal	-	220 J	-
yield strength (b.m.)	506 MPa	1092 MPa	692 MPa
ultimate strength (b.m.)	789 MPa	1165 MPa	824 MPa
T_{ref} (not corresp. to RT_{NDT})	120°C	250°C	140°C
inner radius R_i			
- base material	200 mm	202 mm	200 mm
- weld metal	-	296 mm	-
outer radius R_a	400 mm	402.5 mm	395 mm
crack length	2 x 52°	360°	360°C
crack depth	31.2 / 59.5 mm	34 mm	20 mm
a_0/W	0.15	0.17	0.10
crack growth	1.5 / 0.9 mm	61 mm	48.5 mm

Table 1: Geometry and material of selected PTS specimens

Two materials have a Charpy energy of less than 68 J, therefore, the reference temperature RT_{NDT} could not be determined acc. to the Code. Even the nil ductility transition temperature could not be determined for all materials since at the necessary high temperatures unstable crack initiation could not be realised. For this reason a transition temperature was derived from the instrumented Charpy impact test taking crack arrest load $P_4 = 4$ kN and portion of ductile fracture FATT 50 into account. The transition temperature was selected as the lower temperatures from both of the above mentioned criteria and referred to as T_{ref} .

The result of the NKS 4-1 experiment is plotted in [fig 5](#) and shows the stress intensity factor K_I - calculated for the deepest point of the crack on the basis of the J-Integral - for this transient loading on the one hand and on the other hand the initiation values K_{Ij} derived from J_i of the material vs temperature.

The load path intersects the crack initiation curve at high temperature in the ductile regime which leads to stable crack initiation. Since the stress intensity factor further increases with decreasing temperature, stable crack growth occurs up to the point when the maximum in the load path is reached. After that point no further crack growth was detected not even when the K_{Ic} -curve of the material was intersected. This can be explained firstly by the fact that the K_{Ic} -curve does not represent the mean material behaviour but obviously an upper bound, secondly that the K_{Ic} -curve is limited in the K-axis by the material specific curve for ductile crack initiation derived from J_i and thirdly that the load path already starts to decrease while intersecting the K_{Ic} -curve which excludes crack initiation in principle.

Nevertheless of the low Charpy upper shelf energy of 60 J at 200°C crack initiation occurred in a ductile mode and the process can be described by the J-Integral. The stable crack growth at the deepest point amounts for both cracks to 1.5 and 0.9 mm, respectively. The crack did not extend in circumferential direction.

To assess the amount of stable crack growth the J_R -curves for temperature in the range of 200 to 280°C, fig 6, right part, are compared with the load path, fig 6, left part. For the J_{max} -value of the load path an amount of stable crack growth of 1.5 mm can be determined from the J_R -curve. This good agreement with the value obtained from the experiment can be referred to the agreement in stress state which is essentially the same in both the CT specimens used for determination of the J_R -curves and the thermal shock specimen NKS 4-1, fig 7.

Different from the test specimen NKS 4-1 the specimen NKS 6 contained a circumferential crack. The compound specimen was composed of the MoV steel with low USE at the inner part and a weld metal (S 3 NiMo 1) with high USE at the outer part applied by the shape welding technique. After plating, the specimen was post weld heat treated for 10 hrs at 590°C.

The linear-elastic and elastic-plastic fracture toughness data are plotted in fig 8 and compared with the K_{Ic} -curve of the Code, however, normalised to T_{ref} and not to RT_{NDT} . The K_{Ic} -data are enveloped by the K_{Ic} -curve and the upper part of the K_{Ic} -curve is cut by the K_{IJ} -values in the ductile regime.

The load path K_I for the specimen NKS6 during the cooling phase is shown in fig 9 together with the fracture toughness curves K_{Ic} and K_{IR} (normalised to $T_{ref} = 250^\circ\text{C}$ of the base material) and the K_{IJ} -data of the weld metal. The first crack initiation occurred predominantly in a ductile mode which is in accordance with the experimentally established K_{IJ} -curve and at the first intersection of the load path with this curve. Subsequent a spontaneous crack jump of 20 mm occurred. The crack was then arrested in the base material with low toughness. The fracture surface of this crack jump shows 100 % fission fracture. After further cooling a second crack initiation occurred. In this case both the region of crack initiation and the region where the crack extended showed ductile appearance. This second crack jump of 41 mm ended at the interface between base material and high tough weld metal. Acoustic emission signals showed clearly the spontaneous event of the first crack growth and signals over a period of time indicating ductile crack growth. The initiation values of the ductile weld metal which are also shown in fig 9, are much higher than those of the base material. Therefore conditions were given for crack arrest in the upper shelf. According to the load path a further ductile crack initiation with little crack growth might have occurred. It was not observed, however, probably because of the material scatter and the reduced driving force in the range of the maximum of the load path. The experiment shows that also for the NKS 6 test the initiation and arrest behaviour could be described. Initiation occurred in the range of the K_{Ic} -curve normalised to T_{ref} and this confirms essentially the fracture mechanics concept of the Code. Crack arrest occurred at lower temperature as predicted from the K_{Ic} -curve, e.g. the K_{Ia} -curve describes the arrest behaviour conservatively.

The load path K_I for the specimen NT3 during the cooling phase is shown in fig. 10. The first initiation of the crack occurred when reaching the scatterband of small scale initiation values. The crack was arrested after some unstable crack growth due to decreasing crack tip loading and increasing temperature. After further cooling crack growth and subsequent crack arrest took place again in 8 events. After passing the maximum of the load path no further crack growth occurred. The crack path predicted with analytical methods is shown in fig. 11 in the critical crack depth diagramme. Based on experimentally determined initiation and arrest values a greater crack growth of ($a_{max}/W = 0.57$) is predicted than reached in the experiment ($a_{max}/W = 0.35$). Based on the fracture toughness curves of the code a crack growth of $a_{max}/W = 0.81$ would be predicted. So these curves describe the behaviour of the specimen NT3 conservatively.

The experiment NT3 showed 9 crack initiation - crack arrest events, which may be clearly seen by means of acoustic emission measurements. Each event had strong signals in a short time interval during crack propagation, fig. 12.

5 Summary

The safety analysis of a reactor pressure vessel (RPV) is based on the fracture mechanics concept which is implemented in the Code. The present Code, however, covers only the linear-elastic regime. The individual properties of a material are described by the reference temperature that results from Charpy impact and drop-weight test. The conservatism of this concept was proved for a variety of different materials which represent optimised quality as well as materials at the bounds of the specification (lower bound) and specification exceeding materials (worst case).

For the elastic-plastic regime methods were developed, in the past, to describe stable crack initiation and crack growth quantitatively on the basis of the J-Integral. The nuclear Codes have not yet adapted these concepts. With the help of large specimen tests, particularly considering complex loading situations, the transferability of both the linear-elastic and the elastic-plastic concepts could be confirmed.

A series of thermal shock experiments was performed to demonstrate the applicability of fracture mechanics parameters derived from small scale specimens to large structures with complex stress states. The experiments were aimed at static crack initiation and crack arrest in the low shelf region of Charpy impact energy on the one hand and on the other hand to crack initiation and stable crack growth in the Charpy upper shelf energy region. The thermal shock experiments were performed with large scale model structures with a wall thickness realistic with regard to the conditions of the cylindrical shell of a RPV. Crack initiation and crack growth could be described analytically. These experiments demonstrated the applicability of fracture mechanics concepts and material parameters to describe failure behaviour of large structures under transient conditions.

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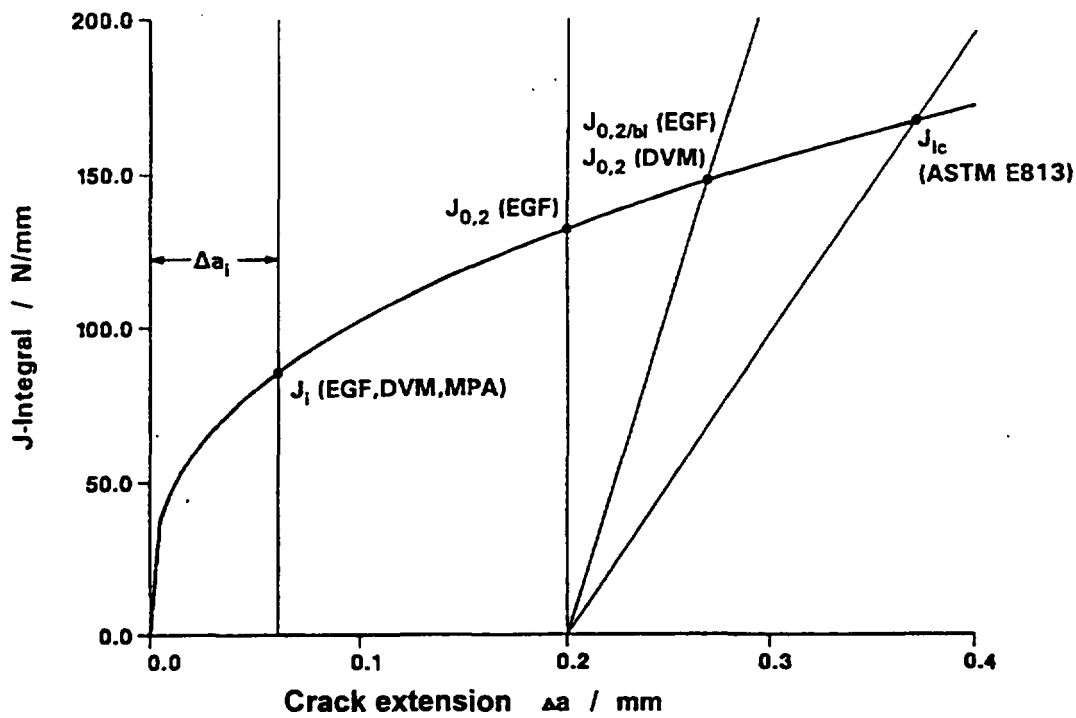


Fig. 1: Comparison of crack initiation values and pseudo initiation parameters according to different test standards

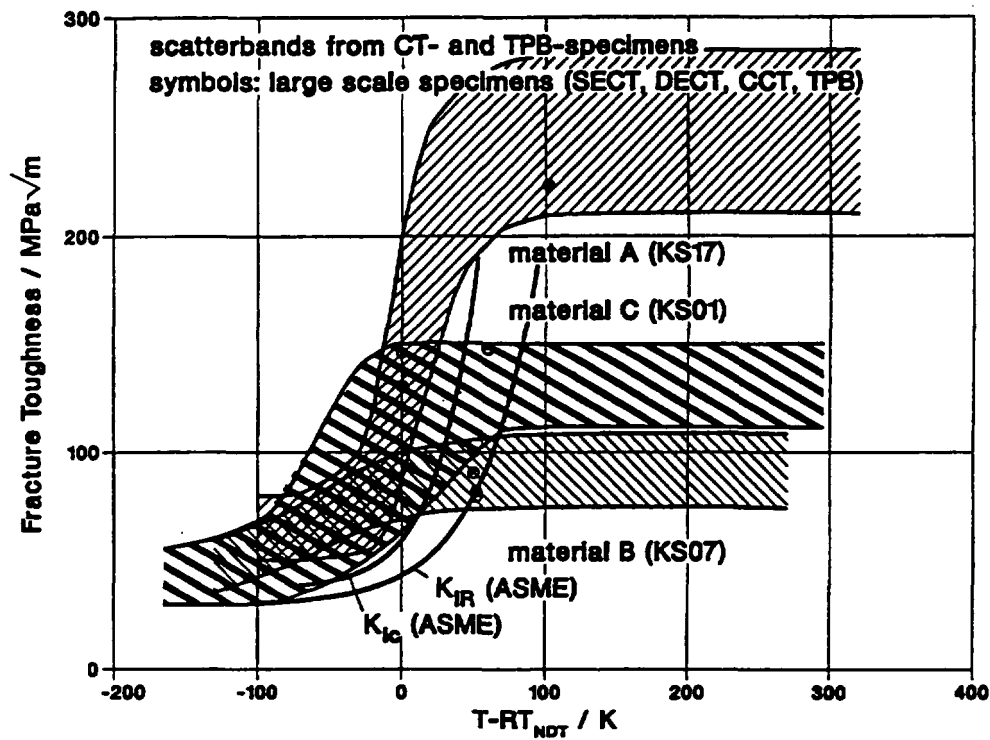


Fig. 2: Crack initiation values determined with large scale specimens in comparison with the scatter band derived from small scale specimen testing

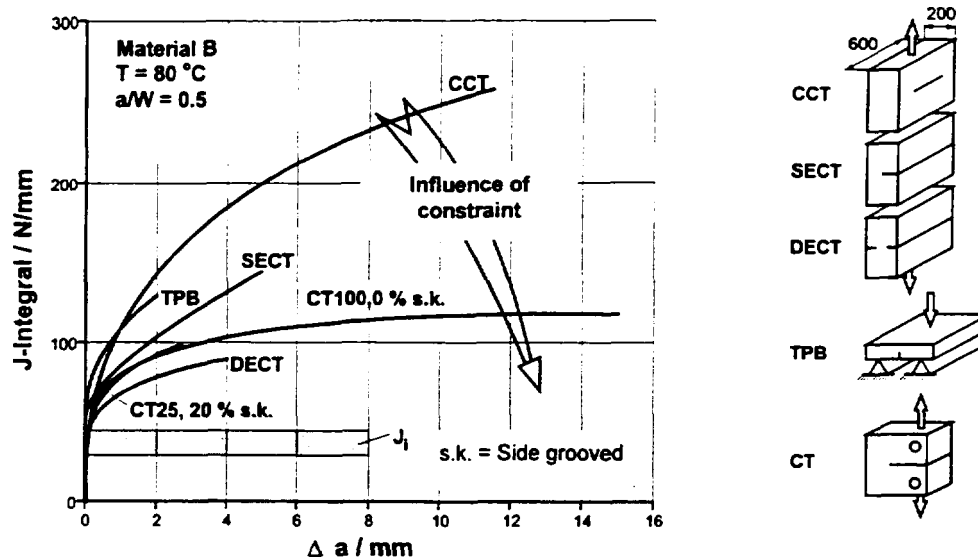


Fig. 3: Crack resistance curves of large scale specimens of different geometry with fatigue crack

Specimen	NKS4	NKS6	NT3
Material	22 NiMoCr 3 7	17 MoV 8 4 S3 NiMo 1	17 MoV 8 4
Dimensions	$R_i = 200 \text{ mm}$ $R_a = 400 \text{ mm}$	$R_i = 202 \text{ mm}$ $R_{Mat} = 296 \text{ mm}$ $R_a = 398 \text{ mm}$	$R_i = 200 \text{ mm}$ $R_a = 395 \text{ mm}$
Geometry of crack	Circum. crack $\alpha = 52^\circ$	Circum. crack $\alpha = 360^\circ$	Circum. crack $\alpha = 360^\circ$
Crack depth	$a_0 = 31,2/29,5 \text{ mm}$ $a_0/W = 0,15$	$a_0 = 34 \text{ mm}$ $a_0/W = 0,17$	$a_0 = 20 \text{ mm}$ $a_0/W = 0,1$

Fig. 4: Selected MPA thermal shock experiments

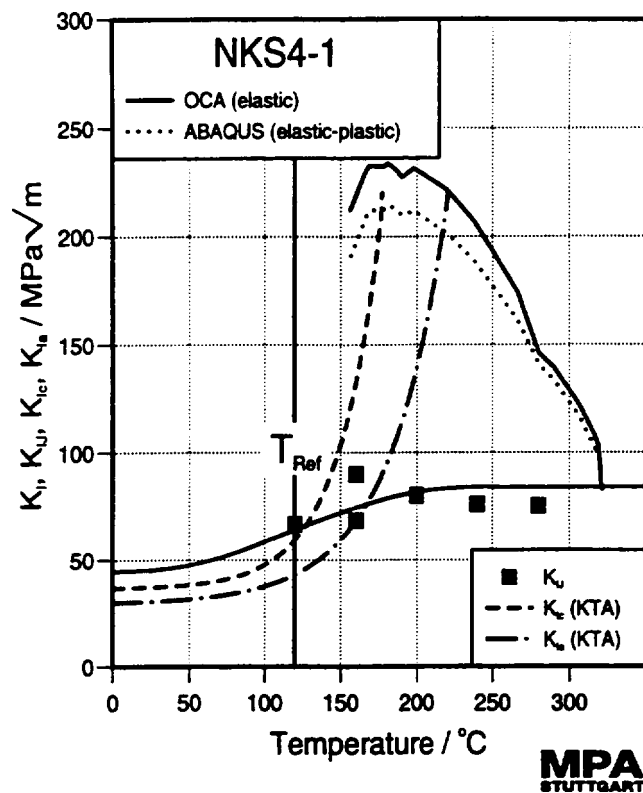


Fig. 5: Load path and fracture mechanics material parameters of thermal shock experiment NKS 4-1

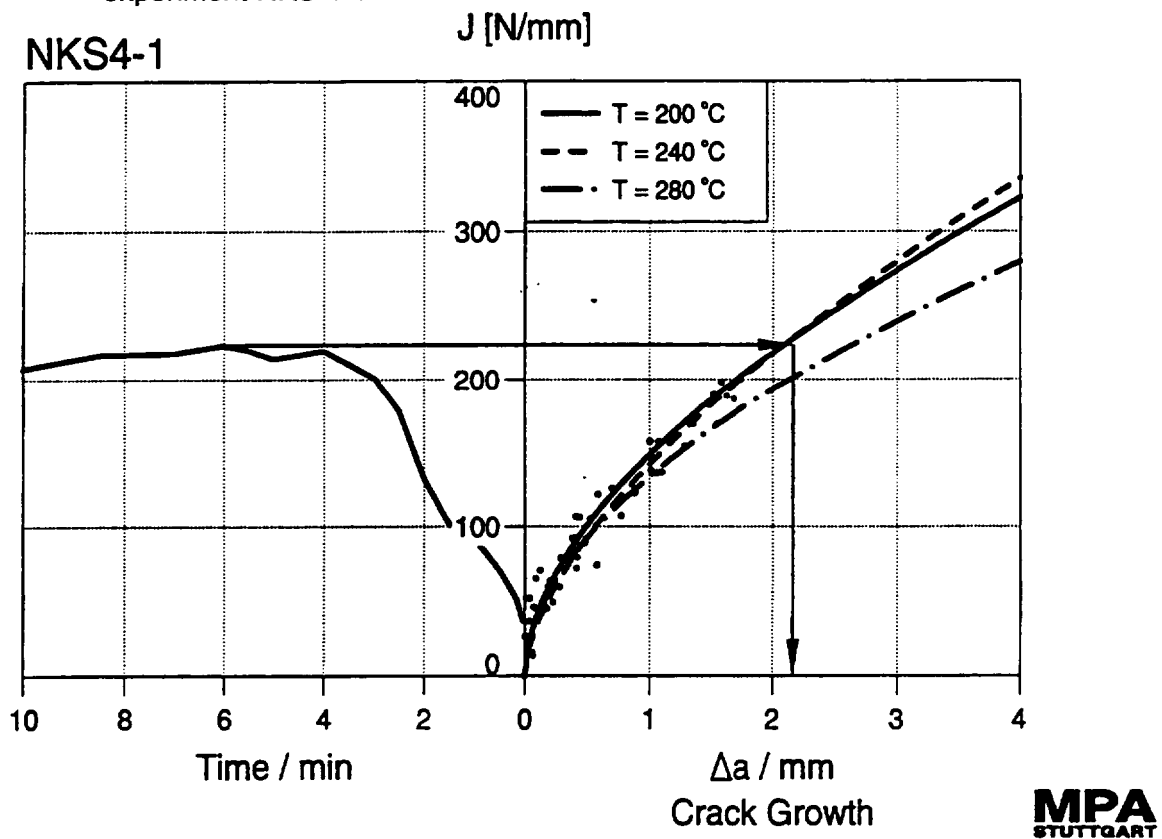
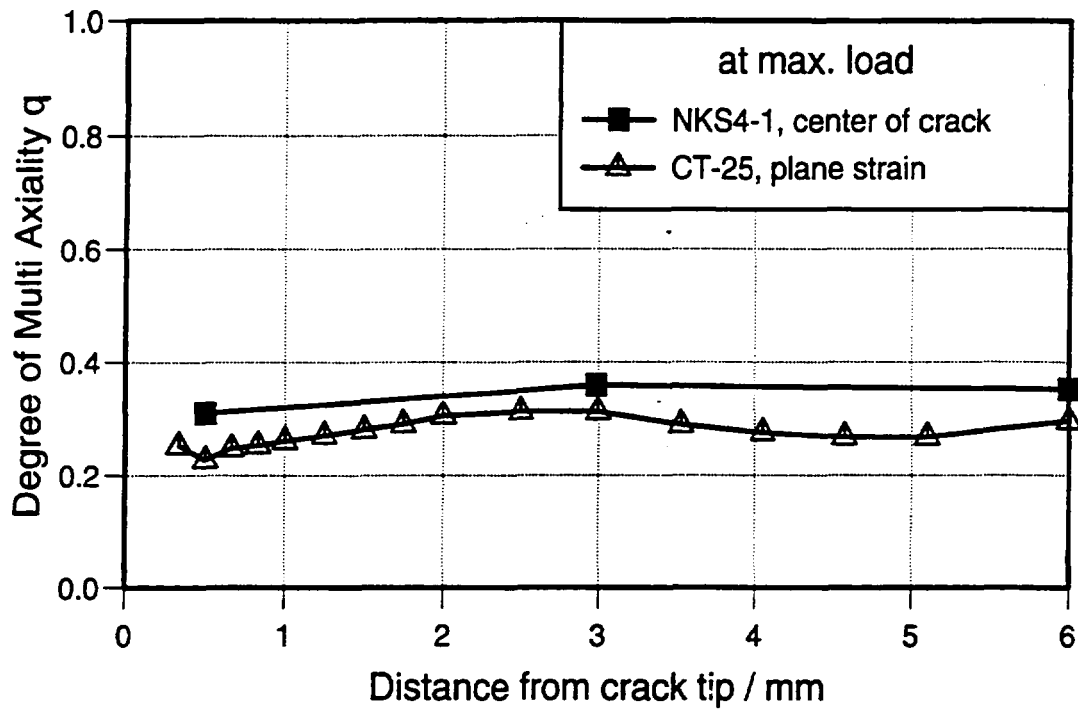


Fig. 6: Procedure to evaluate amount of stable crack extension from load path and crack resistance curve of thermal shock experiment NKS 4-1



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Fig. 7: Degree of multi-axiality q ahead of the crack tip of the thermal shock specimen NKS 4-1

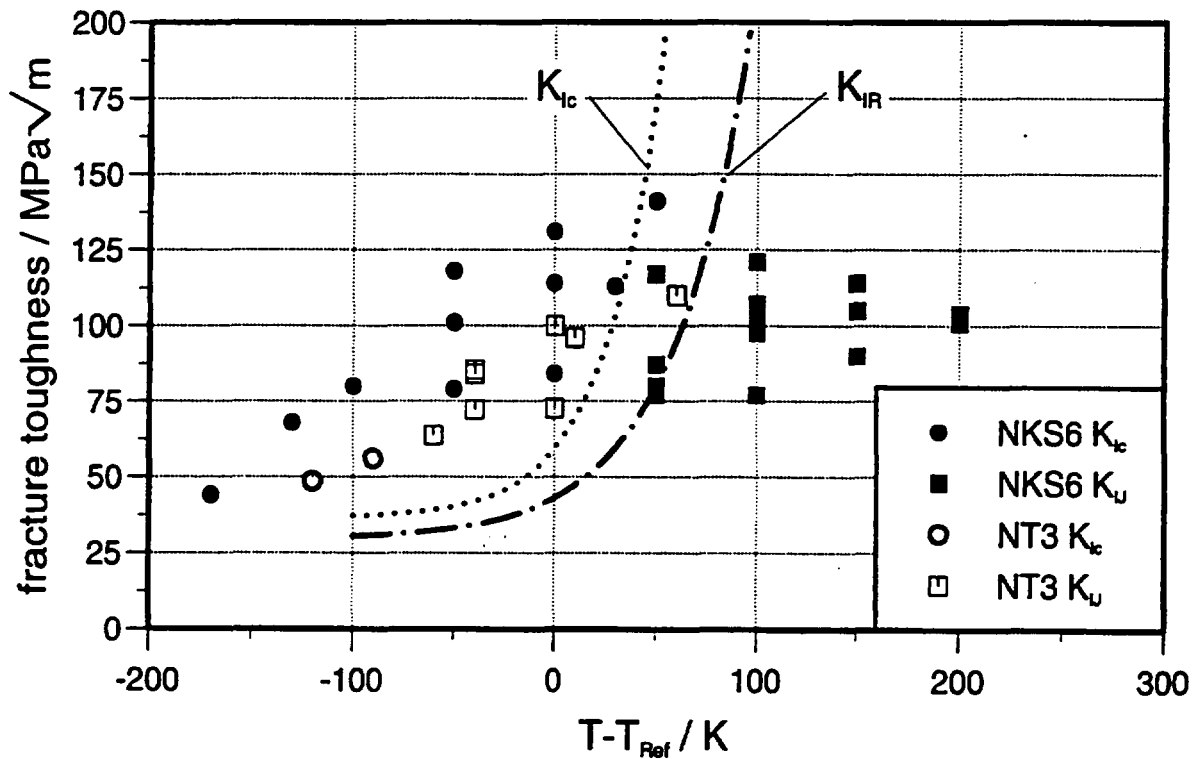


Fig. 8: Fracture toughness data K_{IC} and K_{IJ} normalised to the transition temperature T_{ref} of the MoV steel of the thermal shock specimens NKS 6 and NT 3

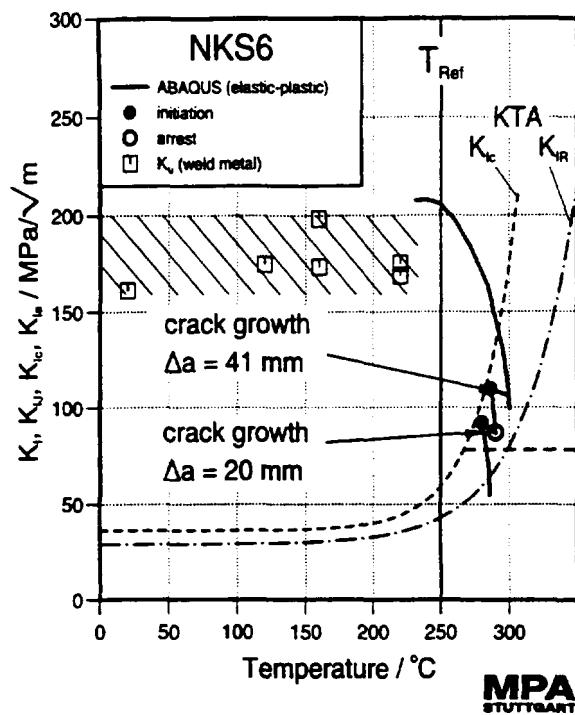


Fig. 9: Load path and fracture mechanics material parameters of thermal shock experiment NKS 6

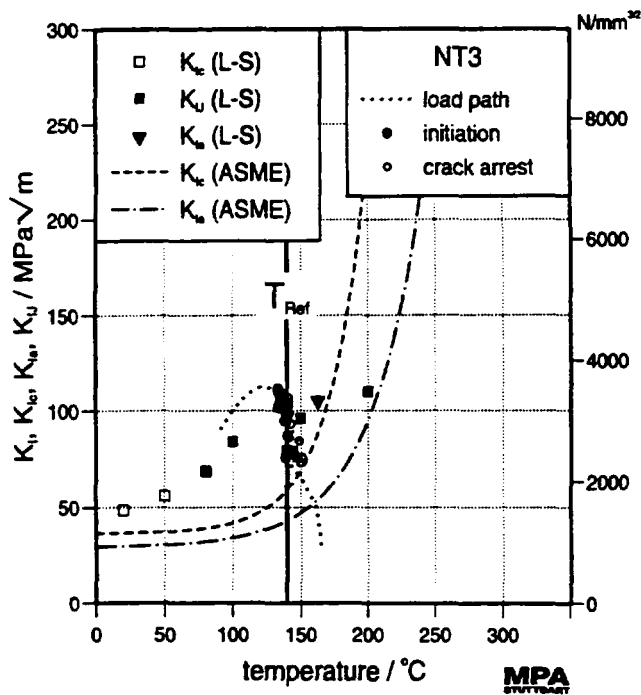


Fig. 10: Load path and fracture mechanics material parameters of thermal shock experiment NT 3

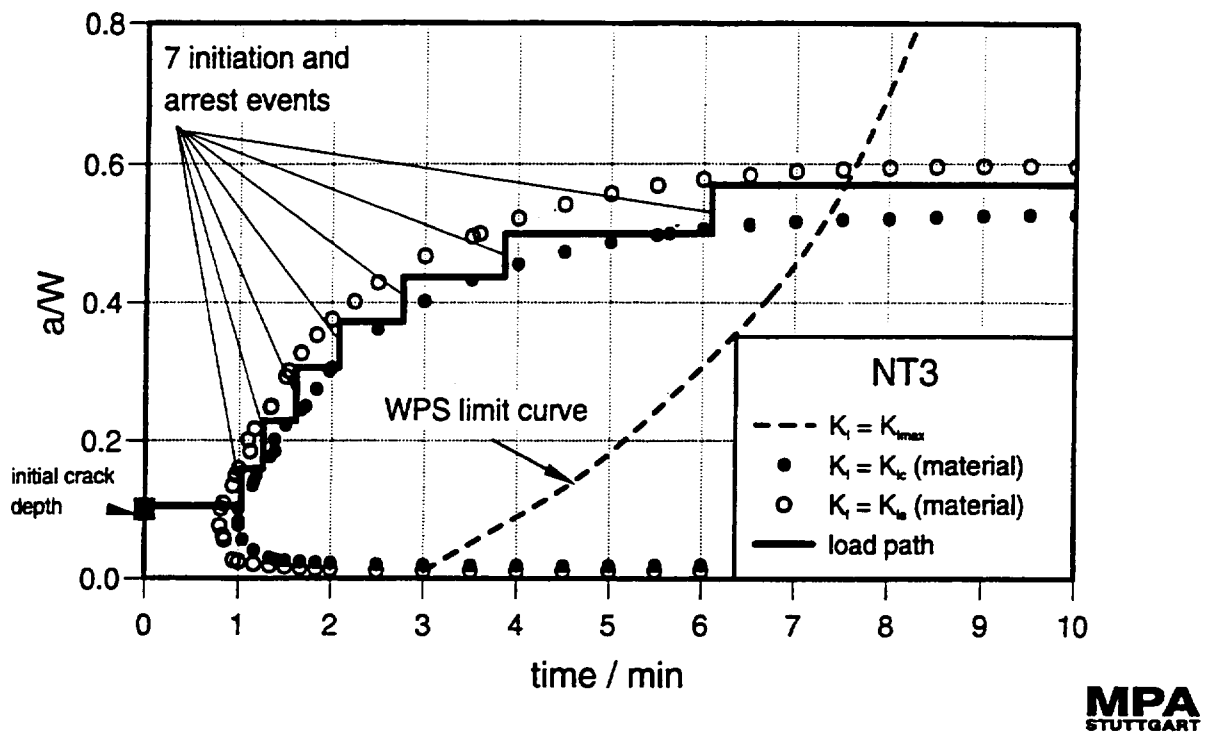


Fig. 11: Critical crack depth diagram of thermal shock experiment NT 3

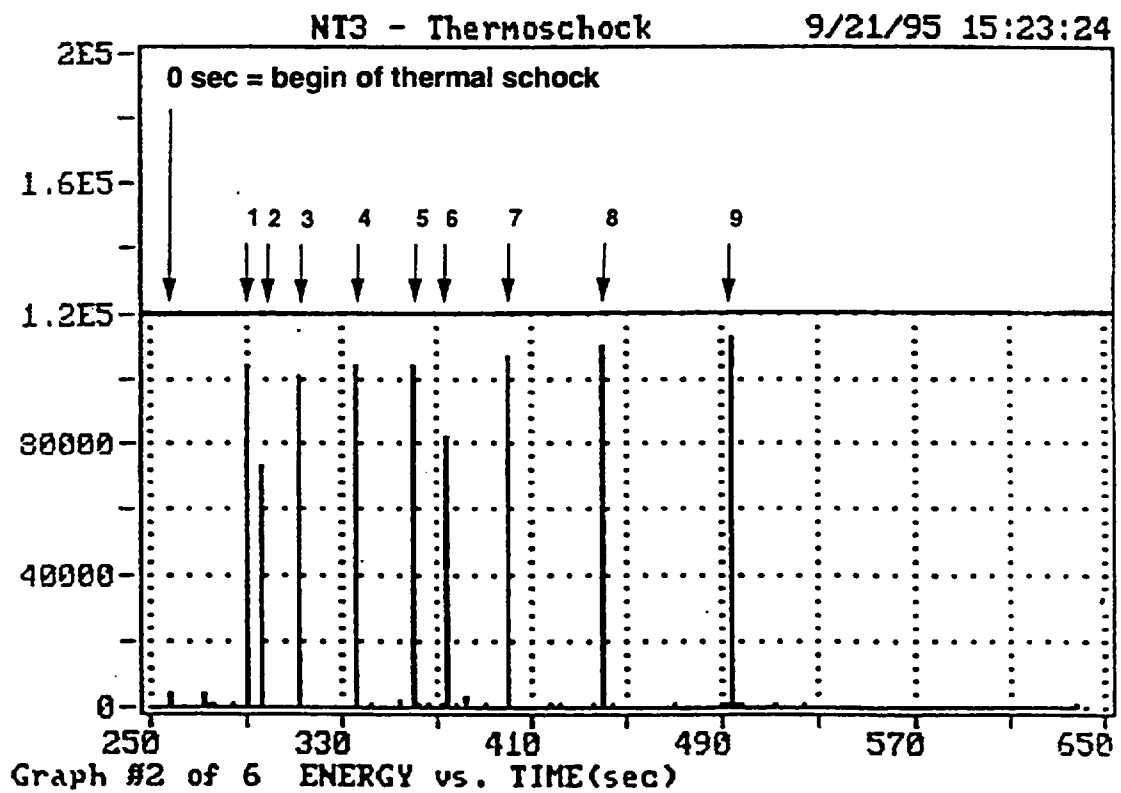


Fig. 12: Acoustic emission signals during thermal shock experiment NT 3

Session 8

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WWER reactor analysis 1.

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Fracture Analyses of WWER Reactor Pressure Vessels

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IAEA Specialists Meeting on "Methodology for Pressurized Thermal Shock (PTS)",

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Abstract

Since several years GRS supports the authorities in Russia and Eastern European countries concerning safety assessments of nuclear power plants with WWER reactors. One main topic is the integrity assessment of reactor pressure vessels (RPV), which is an interdisciplinary task because the loading of postulated cracks under thermal mechanical transient loading has to be determined and assessed with regard to the material properties. The consideration of the effect that crack initiation cannot occur if the crack loading is decreasing during the transient (warm prestressing) can show additional safety margins which have not yet been used.

In the paper first the methodology of fracture assessment based on finite element (FE) calculations is described and compared with simplified methods. The FE based methodology was verified by analyses of large scale thermal shock experiments in the framework of the international comparative study FALSIRE (Fracture Analyses of Large Scale Experiments) organized by GRS and ORNL.

Furthermore, selected results from fracture analyses of different WWER type RPVs with postulated cracks under different loading transients are presented. For a vessel without cladding the loading of postulated cracks due to a primary leak transient was determined by a 3d FE calculation and by simplified methods. The agreement was very good because the effects of plastification were negligible. For a cladded vessel the results of simplified methods show large deviations to the FE results, because the simplified methods do not consider the cladding adequately. Important influence factors on safety relevant results like the maximum allowable brittle fracture transition temperature were studied in the framework of parametric studies. The type of the transients, thermal hydraulic boundary conditions describing the asymmetry of the loading, the geometry of the postulated cracks, the consideration of plasticity effects and the effects of sampling for material tests were investigated in detail.

1 Introduction

Several years ago GRS has been asked to reinforce the authorities of Russia and Eastern Europe in matters of safety evaluation of nuclear power plants with WWER reactors. A focal point of the work was to provide professional assistance in evaluating the integrity of the pressure-retaining enclosure. Since the beginning of the work, the reactor pressure vessels (RPV), especially of the type WWER-440/V-230, were at the middle of the group effort, as the brittle-fracture transition temperature of the near core circumferential welding markedly increases during operation, i.e. more than that anticipated by virtue of the design. Just before expiration of the planned lifetime, a state can be reached for which results of the proof-of-integrity lie on the border. Therefore, to evaluate the integrity of pressure vessels, it is necessary to test how conservative the applied method of analysis actually is.

RPV-integrity must be guaranteed at all levels of the safety concept. This is determined by the material quality and stresses during regular and abnormal operation, and the presupposed level of damage in the pressure vessel wall. This paper is concentrated on parametric fracture analyses to determine the behaviour of assumed cracks, under transient thermomechanical loading due to emergency core cooling. The investigation was aimed at obtaining comparisons between the methods of analysis employed by the Russians and those of the West for brittle-fracture-safety-assessments, as well as to identify important influence parameters. Hence a high precision method of analysis based on the finite element technique (FE) was employed, as well as simplified methods, among others, those also used by the Russians. With FE methods, a high level of verification was reached by performing numerous analyses of large-scale thermal shock experiments in the Federal Minister Education frame of the FALSIRE project /SIE 94, SIE 96/.

In what follows, the methodology and then selected results of structural parameter analyses are presented within a framework for evaluating the integrity of the set of problems investigated.

2 Methodology for RPV Integrity Assessment

The RPV region close to the core of a RPV (see e.g. Fig. 1) becomes more brittle because of neutron radiation during operation throughout the lifetime of the vessel; i.e. depending on the content of particular chemical elements present, the toughness of the material region close to the core is larger at the beginning than at the end of the projected operational lifetime. This is revealed by a shift in the brittle-fracture transition temperature to substantially higher values. To determine RPV integrity the loading of postulated cracks are calculated and evaluated for the presupposed thermomechanical load transients within the framework of design basis accidents. Russian regulations require that the presupposed semi-elliptic surface cracks with depth up to 1/4 of the wall thickness do not initiate. In the event that this demand cannot be fulfilled, the proof is limited to crack configurations which can be reliably detected by means of non destructive testing. Hence, a factor of safety of 1.4 for the fracture-toughness curve must be observed according to the Russian rules. This corresponds approximately to a factor of 2 for the crack depth, common among regulations of western countries. In the PTS-Guide for WWER RPVs /IAEA 96/ prepared by experts from Russia, Eastern Europe and Western countries, sponsored by the IAEA, the methodology is described in more detail including thermal hydraulic aspects.

Fig. 2 contrasts qualitatively the loading of a postulated crack (load-path curve) and the behaviour of the material (fracture-toughness curves). The above-mentioned embrittling process is evidenced, among other things, by an increasing brittle-fracture transition temperature (T_k) and, hence, by a shift in the fracture-toughness curve (K_{IC} as a function of temperature) to higher temperatures. The load-path curve shows the local crack loading as a function of local crack frontal temperature consequent to accident load. As long as the load-path curve lies below the fracture-toughness curve, crack initiation will not be of concern. In the rising portion of the load-path curve $K_I(T)$, running from right to left, an intersection with the fracture-toughness curve K_{IC} means that crack initiation is possible, and hence, safety against fracturing the RPV can not be assured. The shape of the load-path curve largely depends upon circumstances attributed to the accident, the ensuing stresses thus determined, and the crack postulate, respectively (see Fig. 3). With the advance of the embrittling process the danger of a crack getting started increases. According to Russian regulations, the maximum permissible brittle-fracture transition temperature comes at a point of contact of the

fracture-toughness curve with the region of the load-path curves for the presumed cracks and accidents.

In quantifying brittle-fracture safety, the warm pre-stress effect (WPS) is meaningful. Thus, the effect is characterised by experimental evidence showing that cracks do not initiate on the decreasing region of the load-path curve (WPS region, Fig. 3). Therefore, an increased toughness against fracture, vis-a-vis the regulatory curve, exists in the WPS region. This state of affairs is explained by local plastic processes at the tip of the crack. Small scale tests carried out world-wide (e.g. /SHU 93/, /POK94/) and large thermal shock experiments (e.g. /SIE 94, SIE 96/) clearly demonstrate the WPS-effect. Aspects of crack growth and arrest will not be gone into here. In the Russian brittle-fracture proof-of-integrity, the WPS-effect has as yet not been considered. The shortest distance between the fracture-toughness curve of the regulations and the load-path curve is called upon for the evaluation. Depending on the transient, the WPS-effect can more or less strongly increase the safe distance. The methodology described here has a high verification status - i.e. crack behaviour can be well explained ordinarily by large thermal shock experiments.

To guarantee integrity, largely independent particular aspects of the behaviour of materials, the load and the crack postulate must stay within mutually determined bounds, i.e. given the toughness of the material and the assumed magnitude of uncertainty, the crack loading may not exceed predetermined values. By incorporating damage prevention measures (e.g. raising the injection temperature of the emergency cooling water, installing fast closing fittings on the secondary side, annealing the RPV wall area close to the core, qualifying and trying out non destructive testing techniques) the RPV-integrity is assured for all possible load conditions during normal operation and abnormal operation as well.

For some vessels of the WWER-440/W-230 type, the expected brittle-fracture transition temperatures T_k of the near core weld stand clearly above 150°C - because of the high content of radiation-sensitive elements, e.g. phosphorous and copper, at the time of the expected end of operational life (EOL: end of life). Hence, possible load-path curves intersecting with emergency cooling transients cannot be ruled out.

Embrittlement of RPV which can be more than that predetermined by the design is also to be expected in WWER-1000 type facilities, according to recent information on

welding material, due to a slight excess of nickel above specification. Work on estimating the effects on RPV-integrity under cooling transients has begun.

3 Parametric Structural Analysis of RPV-Integrity

Among the estimating procedures, finite element techniques (FE) are very much used in safety-technical examinations of the integrity of components of the leading pressure enclosure. FE-analyses conducted by GRS were carried out using a developed chain of analyses based on the ADINA code /ADN 92/ as well as suitable pre- and post-processors. The crack loading was determined via the J-integral, by the method of virtual crack expansion /LOR 85/. For analyses by means of simplified procedures, the GRS code SIGMAKT was used.

In the following description of the parametric analyses, clad and unclad reactor vessels were examined whereby the influence of parameters such as the method of analysis, transient form, thermohydraulic boundary conditions, crack geometry, plasticity and grinding consequent upon sample-taking for investigation of materials, were investigated.

3.1 Comparison of Fracture Mechanical Analysis Methods (Analyses of WWER-440/V-213 type of RPV with cladding)

In the framework of a BMBF-project for comparing analysis methodology, finite element models for a clad 6-loop RPV (type WWER-440) were developed, qualified by simplified assumptions concerning load. Within the scope of the work summarized here, a study was made of the load upon an RPV of the WWER-440/V-213 type with associated partly circumferential through-clad crack (15 mm deep, 50 mm long) in the near core circumferential weld (FE-Modell, Fig. 4) and due to a flow-mixing transient with time-varying coolant temperature in the axial and radial directions (Fig. 5). This study was carried out by IVO Intern.¹, VTT² and GRS using different FE-Programmes and Models, as well as by GRS utilising several simplified methods. The data used by GRS for base metal, cladding and welding material are given in Table 1.

¹ IVO = Imatran Voima Oy, Finland

² VTT = Technical Research Centre of Finland

With the transient analyzed by IVO Intern., /KOK 87/, a primary side leak due to a pressurizer valve being left open was assumed, and also a delayed closing during a pressure drop in the "hot standby" operation. The pressure increases again at low temperatures because of insertion of high pressure injection. An operating pressure (13.7 MPa) of the pressure vessel safety valve was assigned for the duration of the transient calculated here. The downcomer temperature (T_m in Fig. 5) is based on the thermohydraulic calculations of IVO Intern. with RELAP5. After 1550 s of rotationally symmetric cooling it is assumed that circulation of the coolant stagnates in the primary loop, and a cold stream from the high pressure injection with axially varying temperature ($T_1 - T_5$ in Fig.5) cools the RPV wall. The temperature distribution within the coolant for the time after 1550 s was calculated with the REMIX code. The temperature distribution thus set within the structure in the region of the near core weld (middle cooling line) is depicted at different times in Fig. 6.

Evaluation of the fracture mechanical behaviour of presupposed cracks is based on the stress-intensity factor K_I determined by the J-integral and the regulatory curves or, insofar as it is available, on measured fracture-toughness curves (K_{Ic}) and crack-resistance (J_R) got from small scale specimens. Thus, to be considered are possible differences in the stress triaxiality between the small scale fracture specimens and components, by comparison with a suitable constraint parameter /SIE 95a/.

Fig. 7 shows the crack loading as a function of crack tip temperature at the deepest point of the assumed partly circumferential crack (elastoplastic 3D-FE-Analyses by IVO-Intern., VTT and GRS via different FE-Programmes and Models). There appears to be a very good agreement (~10% deviation) between IVO-Intern. and GRS. The 15 to 20% higher crack load of the VTT-analysis is traceable to differences in the material data used and assumptions made about the crack geometry.

Shown In Fig. 8, opposite the FE results, are results of several simplified fracture mechanical methods, among others also the Russian method, which are available in the GRS code SIGMAKT. The spread of results based on the simplified methods relative to FE results lies between + 30% and - 20%, and is not acceptable for a precise evaluation. This reverts mostly to procedures in the simplified methods, in which non linear stress gradients in the cladding and welding materials, including jumps at places of material transitions are characterised by membrane- and bending stresses. Taking the

cladding into account with simplified methods of analysis is especially imprecise. Detailed notes are given in /SIE 95b/.

3.2 Controlling Parameters: Methods of Analysis, Transient Forms, Grinding (Analyses of an RPV of the WWER-440/V-230 type without cladding)

Continuing, analyses of an uncladded RPV of the WWER 440/230 type under transient loads, owing to an assumed primary side leak of approximate size 3 cm², DN 20 equivalent, as well as to a postulated steam line break (SLB), were carried out without consideration of residual core heat. In this process, the load assumptions were based upon the thermohydraulic calculations of Gidropress. For the load condition of a DN 20 leak, Fig. 9 shows good agreement among the load-path curves calculated with different fracture mechanical methods, in contrast to the earlier described nominal differences. The latter do not appear with the uncladded vessel examined here.

Moreover, the influence of typical spherical grinding (radius of 270 mm, 8 mm) on the inner side of the RPV wall, how they bear upon the characterisation of materials in connection with sample-taking, was investigated. Fig. 10 shows that the maximum crack loading of shallow cracks can increase by about 20%, whereas the effect is nearly negligible for deeper cracks. The load-path curves for a SLB (Fig.11) in comparison to a DN 20 leak are characterised by a much steeper fall off after reaching maximum crack force. This is reached after about 7 minutes in the case of a SLB, and in about 11 minutes for a DN 20. The maximum allowable brittle-fracture transition temperature for the system-technical boundary conditions put forth here is about 165°C for a SLB.

3.3 Controlling Parameters: Crack Form, Thermohydraulic Boundary Conditions, Plasticity (Analyses of a RPV of the WWER-1000 type)

A 3D-FE Modell of the cylindrical part of a cladded RPV (WWER-1000 type) with an associated partly circumferential crack was generated, and parameter analyses of an emergency cooling transient were carried out. Load assumptions were prescribed for the structural mechanics modell of the reactor pressure vessel, based on a thermal hydraulic accident analysis "station blackout and simultaneous DN 50 leak in the cooling line" calculated with ATHLET /BUR 89/. Hence, transient responses for the

internal pressure and fluid temperature in the downcomer were incurred, and, additionally, a cooled inner wall region (beginning with a 400mm wide band) with a 55°C fluid-temperature (emergency core cooling injection temperature) was assumed. Mixing- and heating processes are not accounted for, hence this assumption of load is very conservative. Fig. 12 shows the crack loading of an assumed cladding-penetrating, partly circumferential crack positioned in the middle of the cooling plume (Crack 1: 15mm deep and 50 mm long) and, respectively, an underclad crack (Crack 2: 6 mm deep and 50 mm long) at two different points on the crack front as a function of the corresponding crack-tip temperature. The deepest point of both cracks studied (S1) specifically shows the highest crack loading - reached after about 17 minutes at a crack-tip temperature of about 105°C. These load-path curves are compared with the material characterised crack-toughness curves, determined by the brittle-fracture transition temperature T_k in accordance with the Russian guidelines. The fracture mechanical evaluation shows that for Crack 2 at T_k -values up to 150°C, crack initiation can be ruled out, whereas for Crack 1 at around 100°C crack initiations cannot be closed out.

The influence of plasticity was examined within the frame of a parameter study where frequently a linear elastic behaviour of materials is taken as a basis, especially when estimating crack loading via simplified methods. For the cladding-penetrating crack, the elastic FE-calculation shows a slightly increased maximum value at the deepest point (S1). At frontal crack nodes on the inner surface, the elastic calculation greatly overestimates (approx. 70%) the maximum stress intensity because the cladding is becoming plastic. With an underclad crack, the plastification of the cladding enhances the crack opening and in this way contributes to an increase in stress intensity, hence the elastic calculation underestimates the elastoplastic results (in the investigated case approx. 10% at the deepest point of the crack).

The size of the cooled region of the wall was varied in a further parameter study in which a cooling plume of various sizes was assumed (160, 400, 950, 2000 mm and total extent 12865 mm) and respectively, two, four and six cooling plumes 400 mm wide. The cold side of the connecting ring of the RPV has four main coolant nozzles (NW 850) and two emergency cooling injection nozzles (NW 265). The load-path curves for Crack 1 (Fig. 13) show that a cooling plume width of 400 - 2000 mm is structural-mechanically more critical to evaluate than ca. rotationally symmetric cooling or a smaller cooling plume width. For Crack 1 studied under plume cooling, the initiation of

cracking can be excluded only for $T_k < \sim 100^\circ\text{C}$, while rotationally symmetric cooling of the crack becomes critical beginning with $T_k > \sim 135^\circ\text{C}$ because the load-path maximum is reached approximately 10 minutes earlier and at a correspondingly higher crack tip temperature.

More details about the parametric studies are given in /LIU 97/. Results documented here are to be evaluated as samplings, as they are dependent upon the associated crack geometry, the characteristics of the material and the loading transients which in this case were very conservatively selected.

4 Summary

The goal of the work was to compare the results of different methods of analysis for quantitatively determining component integrity, especially of the reactor pressure vessel, and to evaluate their predictions about safety.

- For uncladded RPV under transient loading with no significant level of plasticity, results from simplified methods, including those used in the Russian brittle-fracture proof-of-integrity, agree very acceptably well with those of the more precise 3D elasto-plastic finite element methods.
- For cladded RPV, results from simplified methods in part differ very much from those of the 3D finite element methods.

The differences are to be explained in that the simplified brittle-fracture methods studied here can only very imprecisely take into account the influence of the cladding becoming plastic as well as jumps in stresses at places of transition between cladding and base/weld material. The analyses carried out show that brittle-fracture proof-of-integrity based on simplified methods is problematic, especially for cladded vessels, particularly when the distance between the material resistance- and load-path curves is small, i.e. when a crossover occurs. To quantify the margin of safety in such cases, employment of the more precise elasto-plastic FE methods becomes necessary.

Futhermore, the parametrical investigations show the effects of intrinsic controlling parameters on the safety-technical relevant results of the RPV integrity assessment,

such as e.g. the maximum permissible brittle-fracture transition temperature. In particular, the transient form, the assumed crack geometry, the thermohydraulic conditions, and the plasticity as well as the consequences of grinding for taking samples for studies of material properties as a controlling parameter, were investigated.

Taking the warm pre-stress effects (WPS) into consideration can reveal previously unused safety margins, since, according to the results of experiments on test bodies in regions of decreasing stress intensity, crack initiation can be closed out.

5 Acknowledgements

We wish to thank the Federal Minister for the Environment, Nature Conservation and Nuclear Safety (BMU) and the Federal Minister for Education, Science, Research and Technology (BMBF) for supporting our work, as well as IVO Intern., the Technical Research Centre of Finland (VTT) and Gidropress for preparing the results of analysis and data on materials.

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7 Tables and Figures

Table 1: WWER440, type 213 data on basic material (BM), welding material (WM) and Cladding (CL)

Material	BM/WM	CL	BM/WM	CL
Temperature [° C]	20	20	325	325
λ [N/(sK)]	40.2	15.1	37.9	18.8
α_{20} [10 ⁻⁶ /K]	11.3	16.3	13.1	17.4
$\rho \cdot c$ [N/(mm ² K)]	3.92	3.6	3.92	4.18
E [GPa]	210	205	195	180
ν	0.3	0.3	0.3	0.3
$R_{p0.2}$ [MPa]	625	426	555	326
E_T [MPa]	8 682	876	8 682	676

with : λ heat conductivity

α_{20} thermal extension coefficient

$\rho \cdot c$ specific heat capacity per unit volume

E elastic modulus

ν Poisson's ratio

$\sigma_{0.2}=R_{p0.2}$ yield stress

E_T tangent modulus

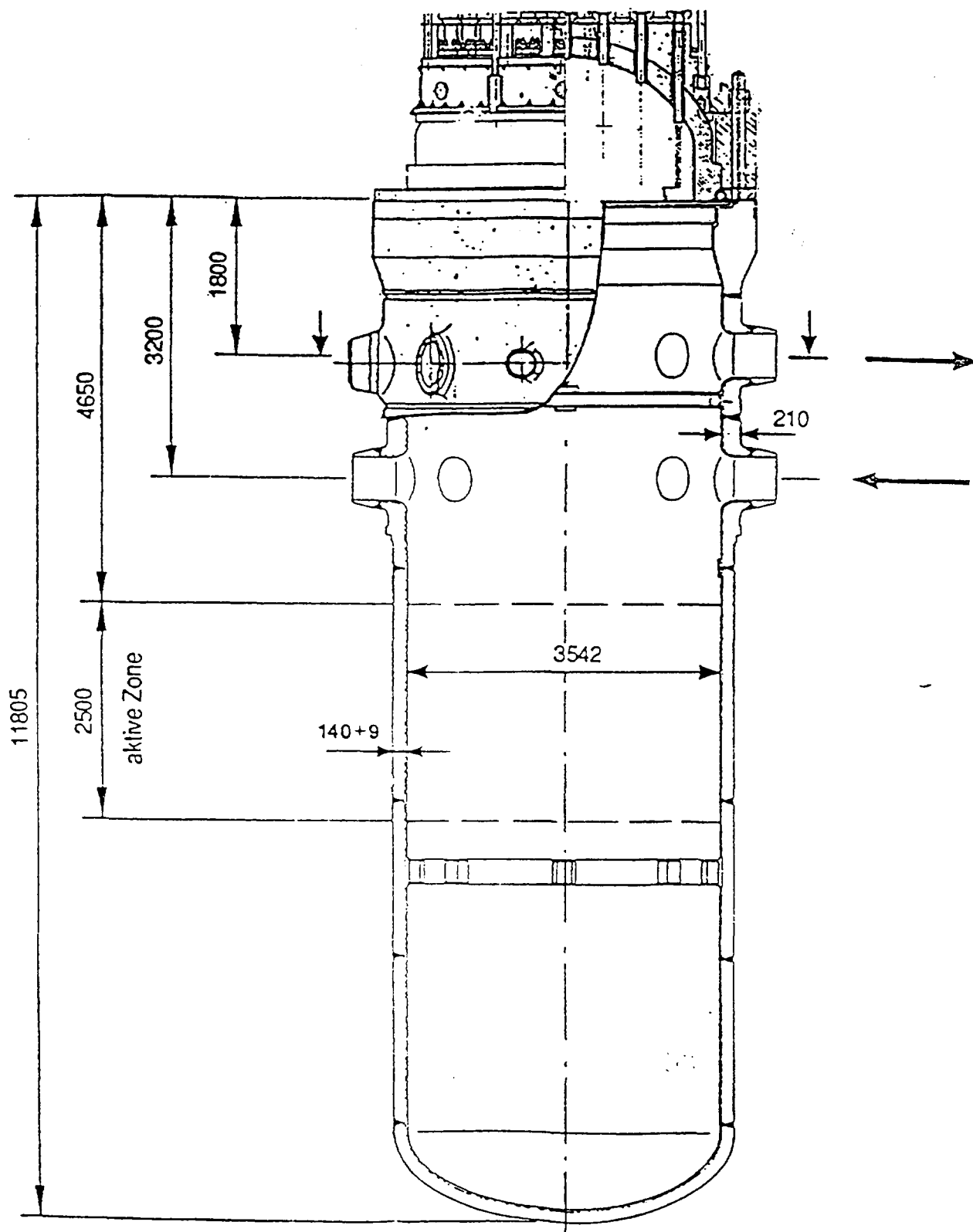


Figure 1 RPV of type WWER-440/V-213

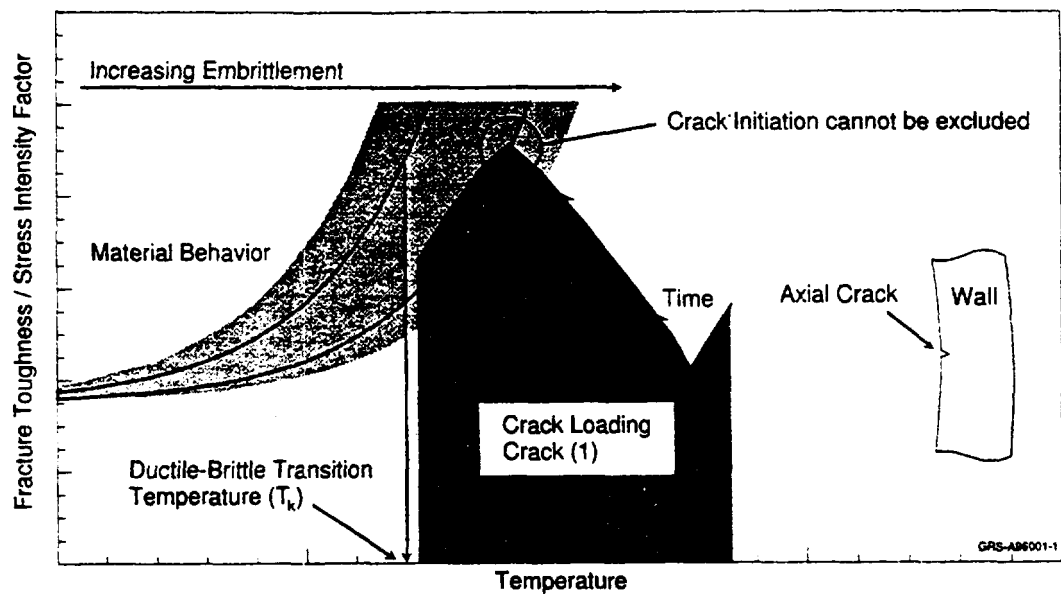


Figure 2 Integrity Assessment of Reactor Pressure Vessel under Accident Condition (1)

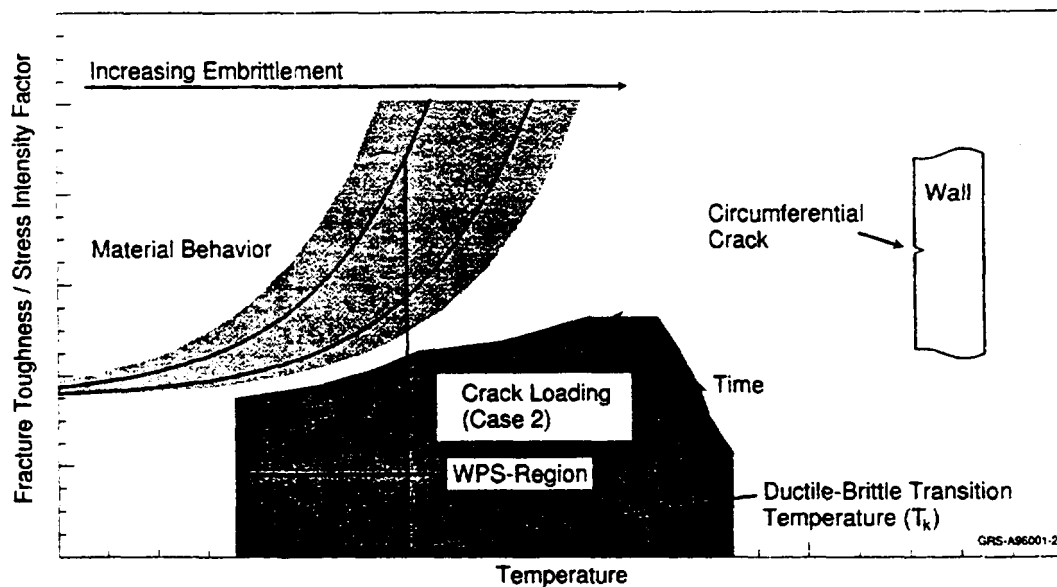


Figure 3 Integrity Assessment of Reactor Pressure Vessel under Accident Condition (2)

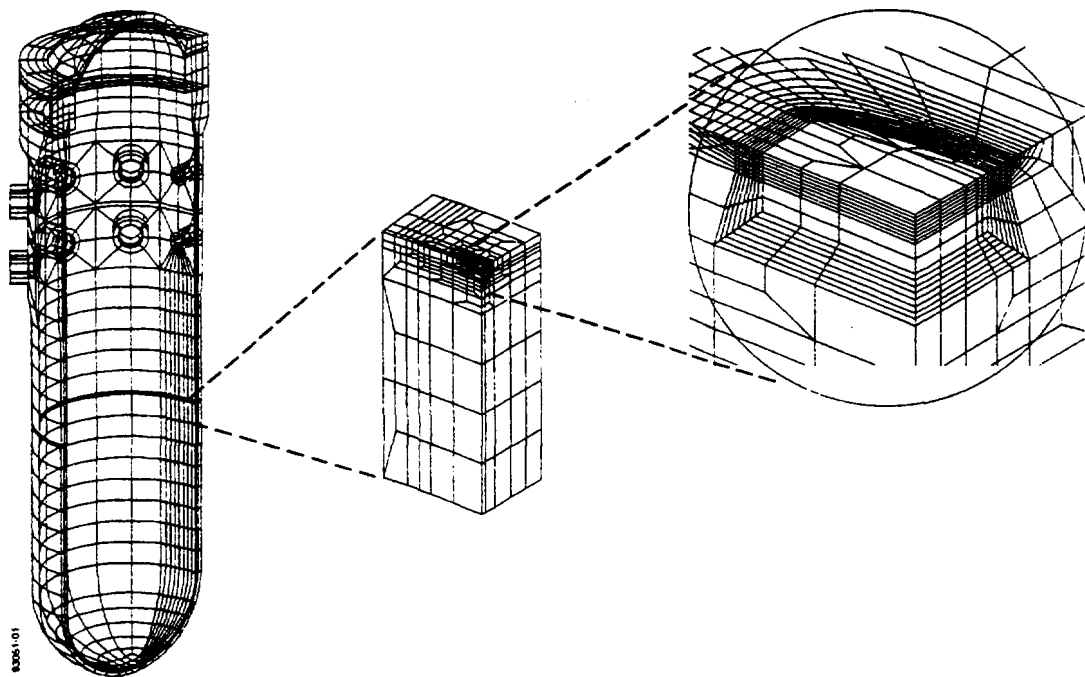


Figure 4 RPV-2 (Type WWER-440/W-213): 3D-180°-FE-Model with one cooling strip

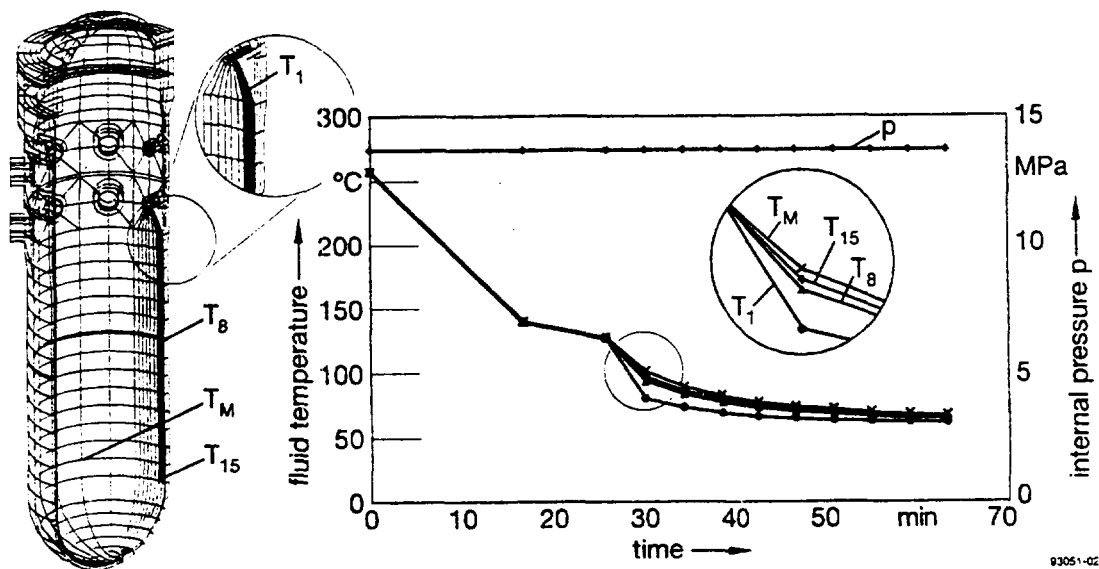


Figure 5 RPV-2 (Type WWER-440/W-213): Loading assumption for loss of coolant accident

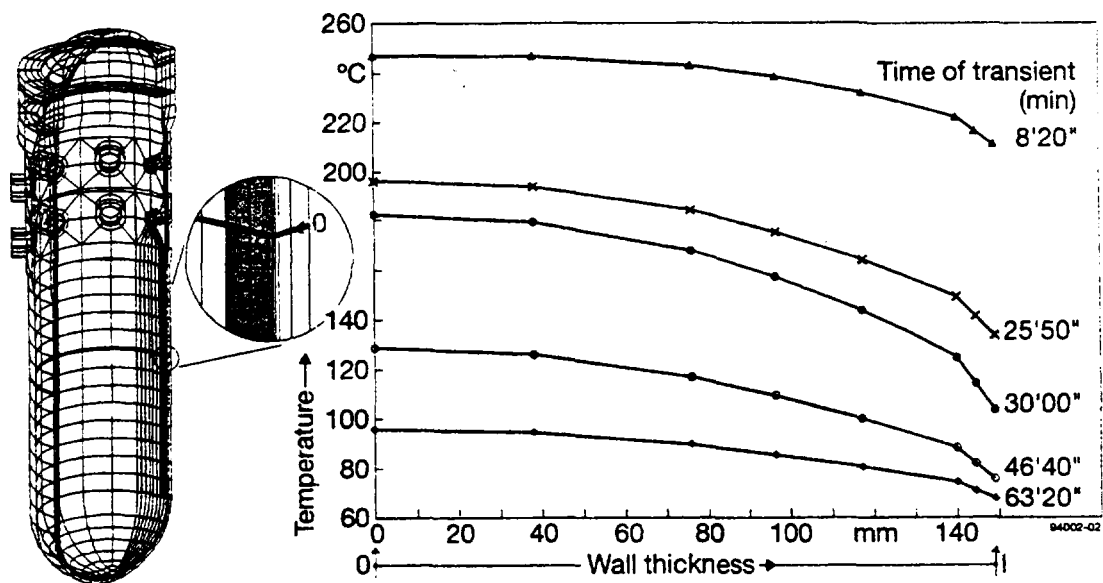


Figure 6 RPV-2 (Type WWER-440/W-213): Wall temperatures for loss of coolant accident

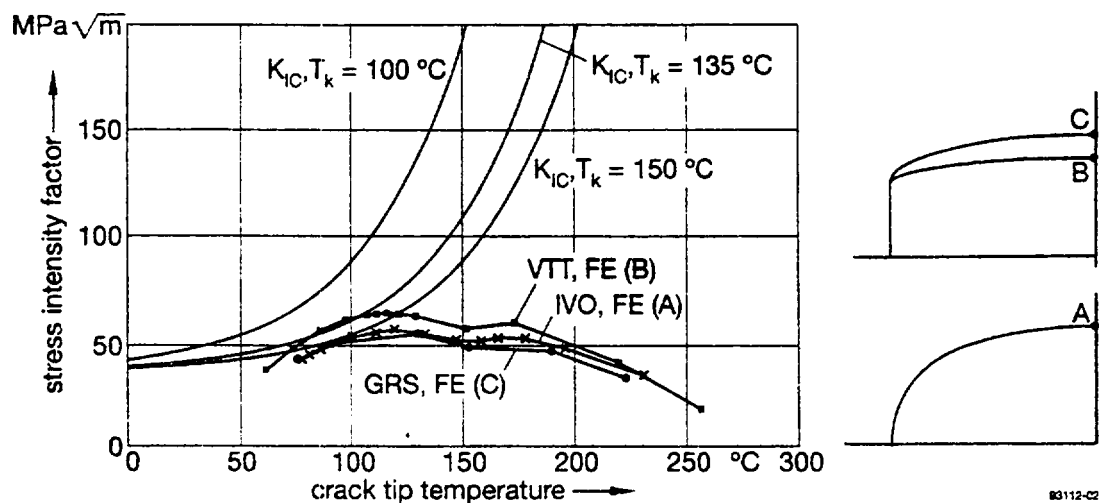


Figure 7 RPV-2 (Type WWER-440/W-213): Loss of coolant transient, crack loading of partly circumferential crack

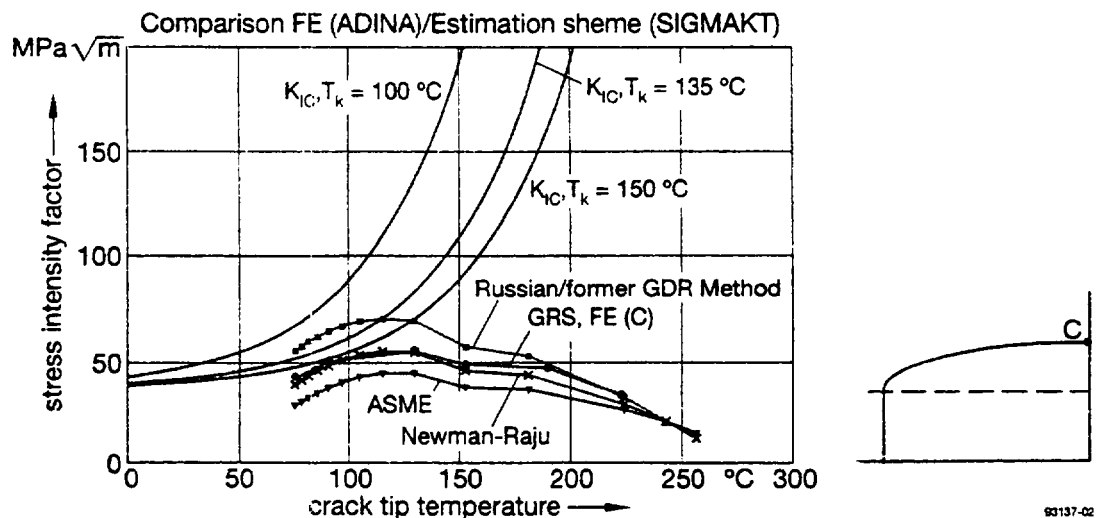


Figure 8 RPV-2 (Type WWER-440/W-213): Loss of coolant transient, crack loading of partly circumferential crack

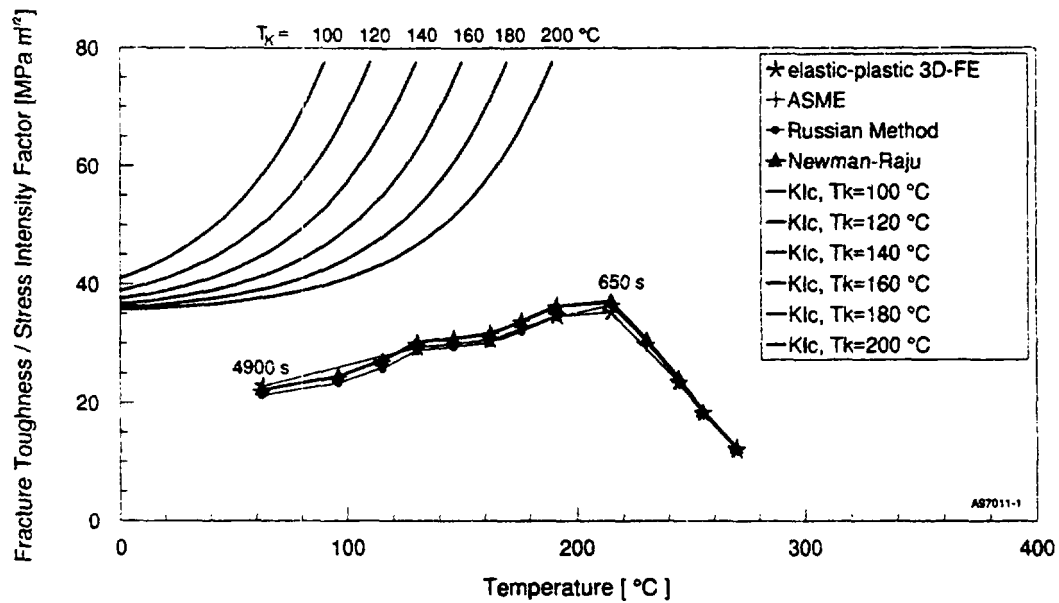


Figure 9 RPV-5 (Type WWER 440/230): Primary leak DN20, without residual heat, partly circumferential crack 10x25mm in weld No.4, deepest point

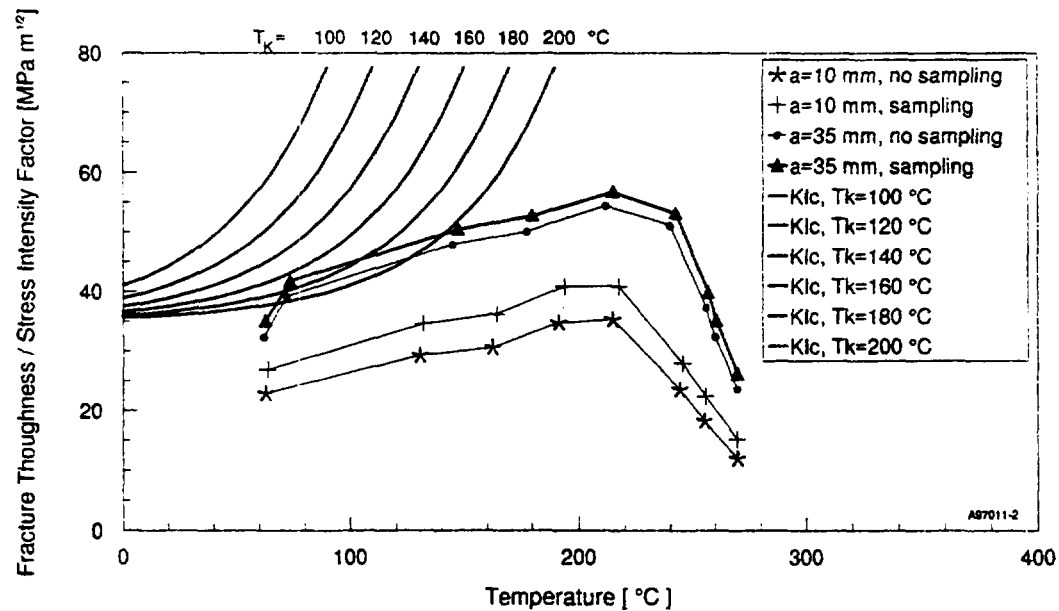


Figure 10 RPV-5 (Type WWER 440/230): Primary leak DN20, without residual heat, partly circumferential crack 10x25mm in weld No.4, deepest point

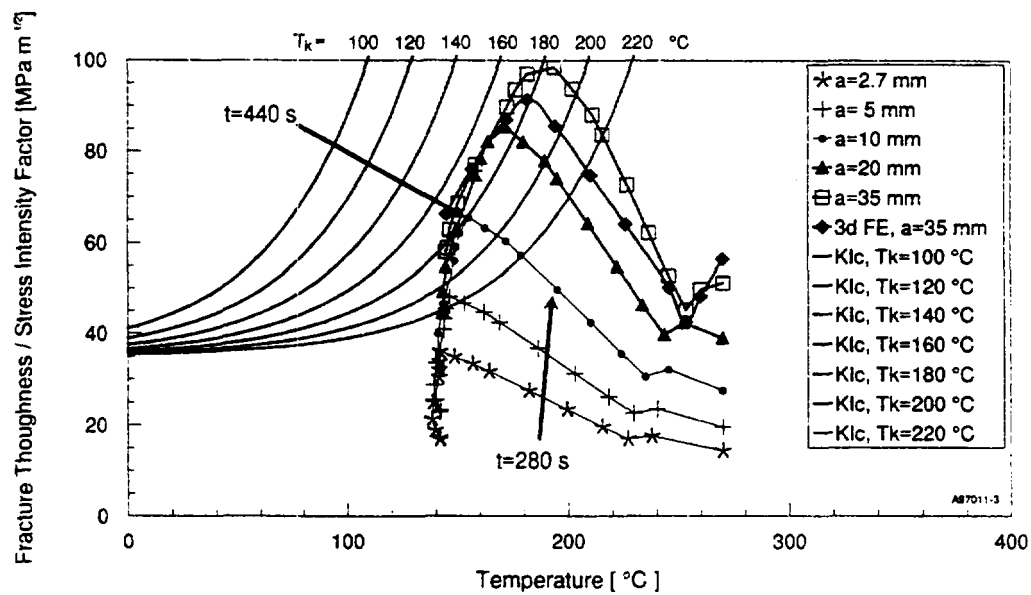


Figure 11 RPV-5 (Type WWER 440/230): Steam line break, 10% nominal thermal power, without residual heat, Russian Method, axial crack, deepest point

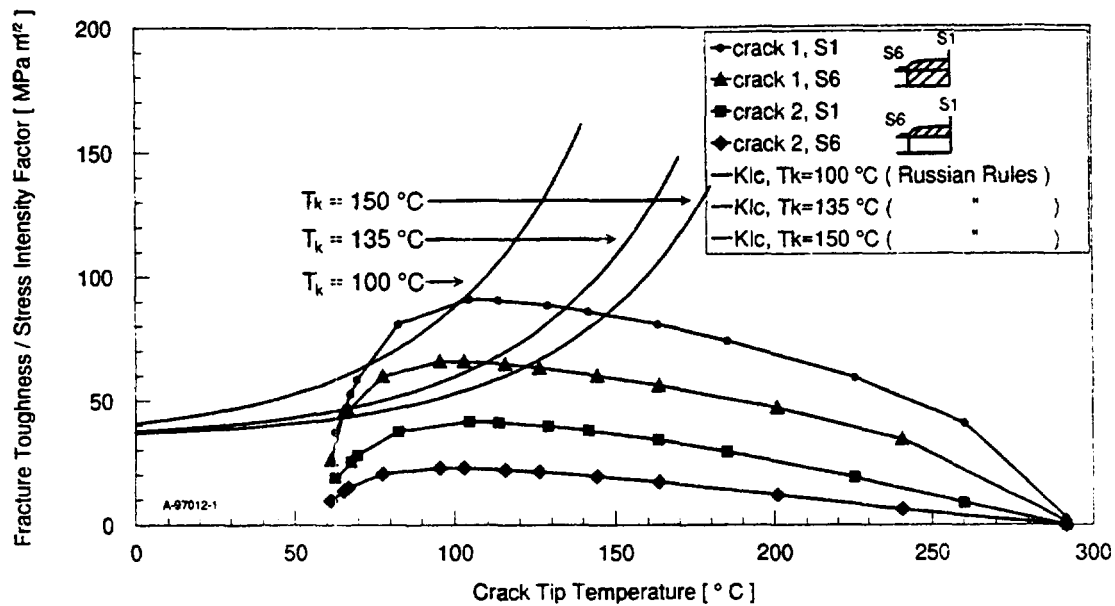


Figure 12 RPV-4 (Type WWER 1000): Emergency cooling transient "station blackout and leak DN50" one cooling strip (width =400mm, temperature 55°C), partly circumferential crack 15x50 mm (crack 1), under-clad crack 6x50 mm (crack 2)

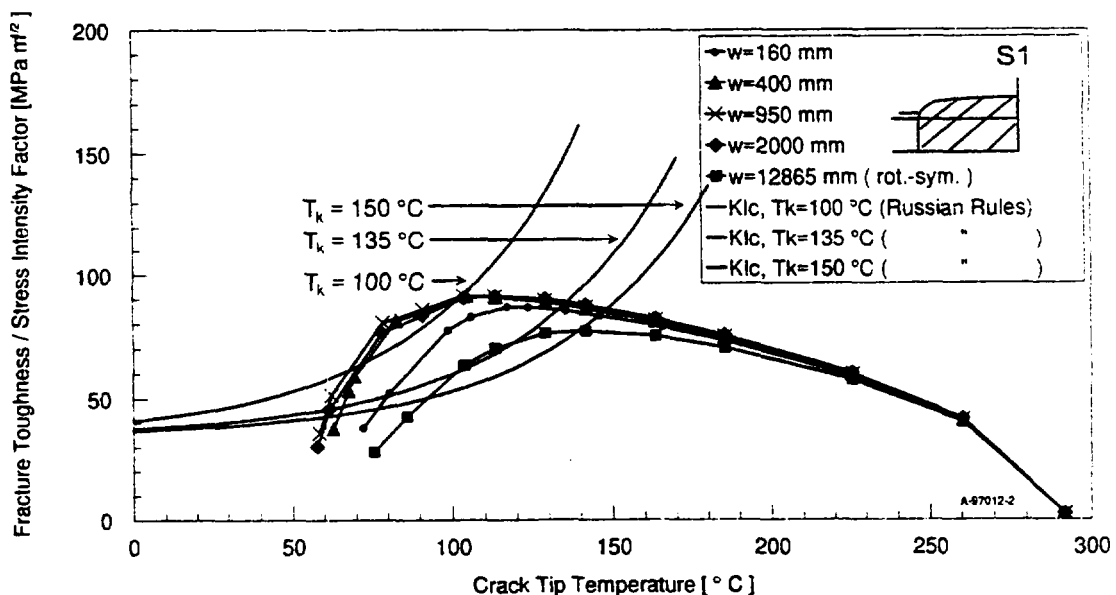


Figure 13 RPV-4 (Type WWER 1000): Emergency cooling transient "station blackout and leak DN50", one cooling strip (temperature 55°C), Variation of cooling width w, partly circumferential crack 15x50 mm, deepest point (S1)

PTS ASSESSMENT -THE BASIS OF LIFE TIME EVALUATION AT NPP PAKS.

by

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Abstract

Plant specific PTS analysis at NPP Paks was performed in the frame of the AGNES (Advanced General New Evaluation of Safety) project.

NPP Paks belongs to the second generation of the WWER-440/213 NPP-s. To verify the safety during transient events and predict the lifetime of the RPV-s several transient cases have been analysed.

The paper summarizes:

- - *the general scheme elaborated for the assessment,*
- - *the safety philosophy used,*
- - *the applied and available codes and methods,*
- - *the ongoing and planned developments*

Keywords: *PTS, life time evaluation, fracture mechanics*

1. INTRODUCTION

The very high tensile stress caused by pressurised thermal shock [PTS] at the vessel inside wall could cause the initiation of the propagation of a pre-existing flaw of small dimensions. It would stop when it reached a zone of higher temperature, where the material toughness is high enough to arrest the running crack. This crack could restart propagating later on during the transient if the pressure is increased, or due to the progressive cooling of the vessel wall. This could ultimately lead to vessel failure after a sequence of arrest re-initiation events.

The WWER-440 V-213 type design was developed in the seventies, without PTS assessments. Since then several calculations were performed by GIDROPRESS (USSR) - main designer of the WWER units - and other institutes, but they did not take into account the plant specific conditions.

In the frame of the AGNES project PTS study was performed on unit 3 of NPP Paks. The specific design and material characteristics of the WWER-440-s (and NPP Paks) made it impossible to refer directly to the results of the generic US studies in case of the PTS assessment of the pressure vessel integrity, but the methodology of the PTS evaluation, the safety factors and the acceptance limit met the requirements of the ASME code [1], and the 10 CFR 50 code [2].

During the elaboration of the methodology for updated evaluation of PTS

integrity of the PAKS vessels the experience of the PTS assessment at Loviisa (Finland) [3], RPV Stade (Germany) [4], RPV-s DOEL (Belgium) [5]; US practice [6], and IAEA documentation were used [7].

This paper is giving an overview of the full analysis, including the already completed and planned development of the methodology.

2. OVERVIEW OF PTS METHODOLOGY USED AT NPP PAKS

The PTS assessment includes several separated actions as shown in FIG. 1.

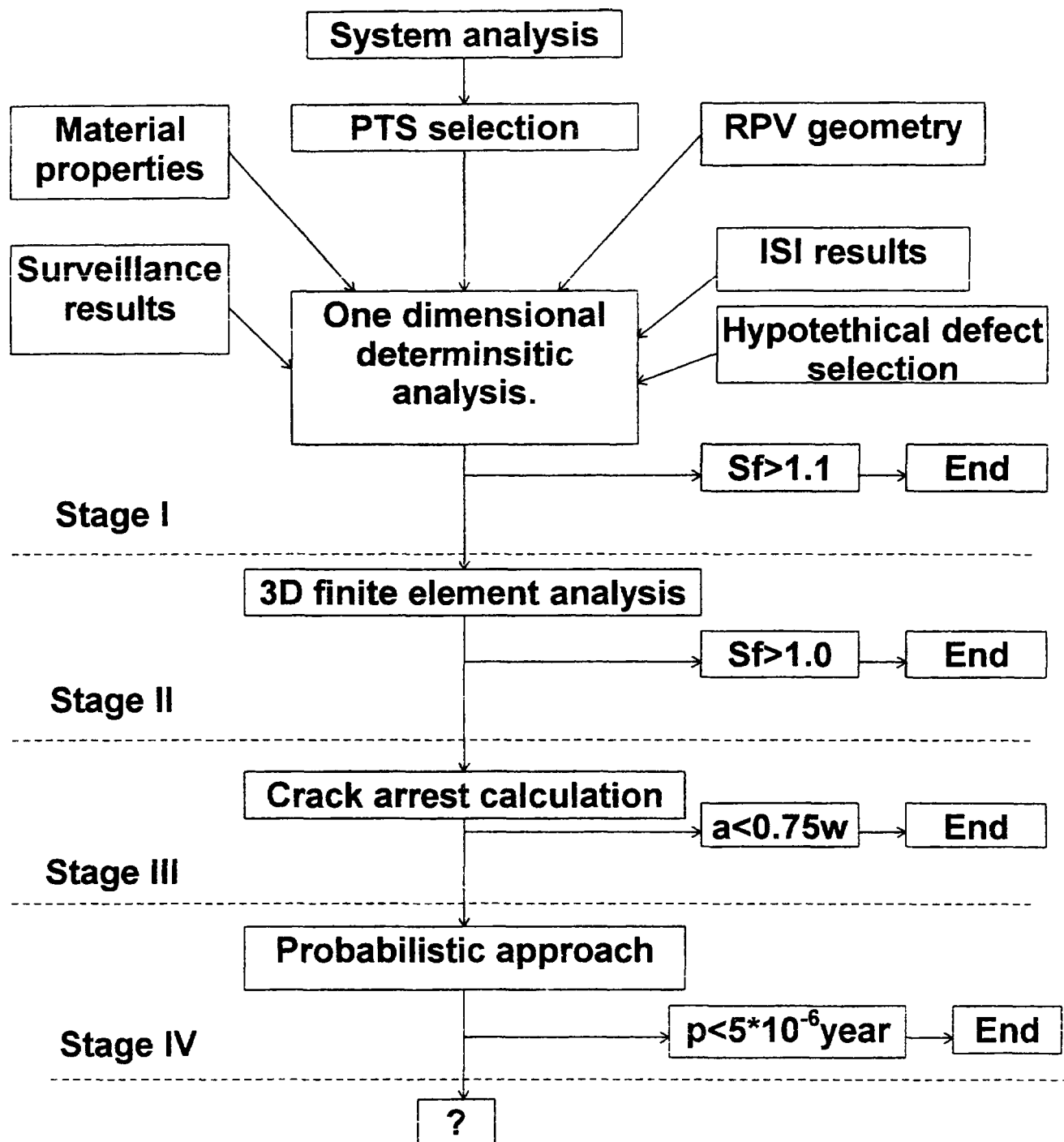


FIG. 1. The flowchart of the PTS assessment in the frame of the AGNES project.

These actions were performed by different groups of engineers and scientists, and were coordinated by the manager of the AGNES project. The main actions are further discussed below:

2.1. Transients selection

The selection of the transients to be analysed is the most difficult part of the analysis. The selection needs good knowledge of the reactor system, and analysis of the plant specific behavior of it. Many transients are plant specific, that is they can occur at one plant, but are not typical for another similar plant operated in a different way, or located at a site, where the weather conditions are different. Generally the so called similar plants only mean plant specific equipment, and during operation the operational mode and maintenance history are very often different even in case of the units of the same plant. In the frame of the AGNES project the PTS transient pressure, temperature data are always based on thermohydraulic transient simulation, which incorporates the unit specific characteristics. The calculations contain not only the effect of the overcooling-depressurization event, repressurisation-reheating situations are also considered. Altogether 7 transients with 18 cases were analyzed. Case means here different operator actions after the same transient event started or variations of the transient according to random behavior of the damaged equipment. (E.g. after the inadvertent opening of the safety valve it automatically closes again after a certain time.)

The selected transients are given in table 1.

TABLE 1. The tested transients.

Transient name	Cases
Inadvertent Opening of the Pressuriser Safety Valve	4
Opening of Steam Generator Cover.	3
Line break Ø 233	1
Line break Ø 73	3
Cold leg large break LOCA	3
Inadvertent Operation of the Emergency Core Cooling System	2
Steam line break	2
Total: 7 transients	18

2.2 RPV geometry

Paks units 1-4 are WWER-440 V-213 pressure vessels. The main characteristics of the V-213 vessels are:

- the vessel is welded from forged rings, and has an inside cladding made by submerged arc strip welding. Inside diameter: 3542 mm
- the base material is 140 mm thick against the core zone, and 190 mm at the nozzle zone. The material is 15H2MFA. The cladding is 9 mm thick welded 18/8 type stainless steel.: 08H18N12B.

- The most critical parts of the pressure vessels (because of the highest neutron flux) are the welds 5/6, 3/5 and the forged ring against the core. Optionally the nozzle zone may be considered for analysis.

2.3 Thermohydraulic calculations

Most of the thermohydraulic analyses were performed by RELAP 5 code. The code has been modified to model the 6 loop WWER-440 design. The water mixing in the downcomer has also been considered in several cases using REMIX code.

2.4 Material database

All four units of NPP Paks have complete manufacturer documentation including material properties, production technology and quality assurance. These data were validated by the NPP owner during installation. Surveillance testing of the vessel material including surveillance of radiation embrittlement and thermal ageing were performed parallel with ISI testing. The operational parameters are also monitored. These databases and the knowledge of 15H2MFA steel properties gained from international cooperation and from the research performed in Hungary make a reliable integrity assessment during any possible PTS.

During the different stages of the fracture mechanical integrity analysis of the RPV different material characteristics are used. For the analysis of crack stability (no initiation) the K_{Ic} reference curve is used, for calculation of arrest of a propagating crack the crack arrest reference (K_{Ia} or K_{IR}) curve is needed, for probabilistic analysis a mean K_{Ic} curve together with the scatter of the data.

K_{Ic} reference curve

In case of the Paks units both the forged material and the welding satisfies the updated requirements of 15H2MFA steel, and this verifies the use of the new reference curves for PTS events given in the Russian Normative Documents [8]:

$$K_{Ic} = 35 + 45 \cdot \exp(.02 \cdot (T - T_K)) \text{ [Mpa}^{0.5}] \text{ 15H2MFA forging}$$

and

$$K_{Ic} = 35 + 53 \cdot \exp(.0217 \cdot (T - T_K)) \text{ [MPa}^{0.5}] \text{ 15H2MFA weld.}$$

where T_K is the transition temperature measured by Charpy impact testing belonging to unirradiated (T_{K0}) or irradiated (T_{KI}) values. The T_{K0} values are given by the producer, and the T_{KI} values are calculated from the unit surveillance results. The reference curves were compared with the results obtained on Charpy size TBP specimens of the surveillance program as it shown on FIG.2.

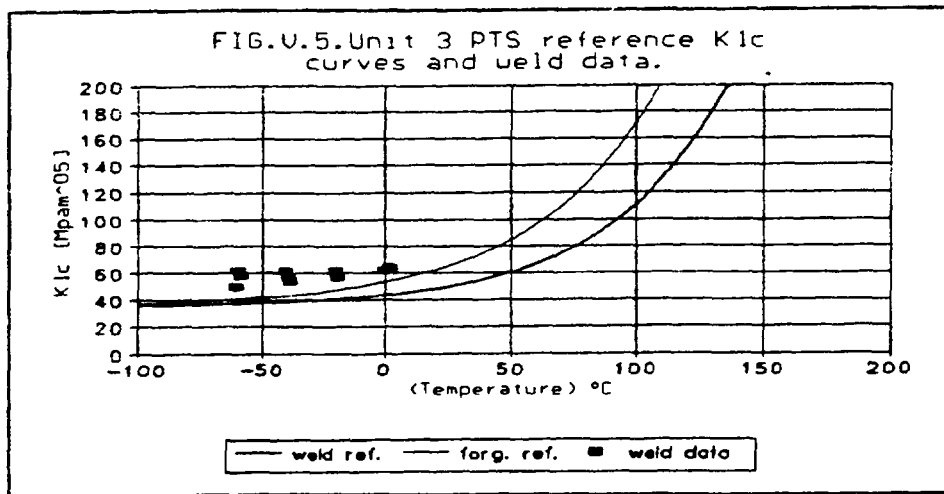


FIG. 2. Comparison of the K_{1c} reference curves and surveillance results obtained on weldment samples.

Crack arrest (K_{Ia}) reference curve

For the 15H2MFA steel and weldment no K_{Ia} reference curves are available in the Rules or in the Russian Normative Documents. Instead of it the ASME K_{IR} (Reference Fracture Toughness) curve was used. The K_{IR} curve represents the lower bound critical stress intensity factors determined from static, dynamic and crack arrest curves. According to the ASME Code the K_{IR} is a function of temperature and RT_{NDT} (Nil-ductility temperature obtained by drop-weight test). The RT_{NDT} values can also be calculated from the surveillance impact testing results. The RT_{NDT0} value (belonging to as received material) is $T_{K0} - 33$ °C where T_{K0} is determined using 68 J criteria. The RT_{NDTi} (belonging to irradiated material) value is $RT_{NDT0} + \Delta TTKV_{41}$, where $\Delta TTKV_{41}$ is the irradiation caused temperature shift measured with 41J criteria.

$$K_{IR} = 29.4 + 13.44 \cdot \exp\{0.0261[T - (RT_{NDTi} - 88.89)]\} \text{ [MPa} \cdot \text{m}^{0.5}]$$

To verify the use of the ASME K_{IR} curve the instrumented impact diagrams measured during the surveillance testing of NPP Paks on 15H2MFA steel were analyzed and compared with the results obtained. K_{Ia} values on 15H2MFA forging were measured in the frame of the OMFB (National Committee on R&D) financed research project "Radiation damage of 15H2MFA steel" (91-97-42-0339).

Mean K_{Ic} curve for probabilistic analysis

Mean K_{Ic} curves are necessary for probabilistic analysis. Such curves do not exist in the Russian Code, therefore the US data used in VISA-II were accepted. These curves have been compared to the real measured data on 15H2MFA and weldment partly taken from the literature, partly obtained during the Hungarian OKKFT A-11/4 research program and in the frame of the Paks surveillance program. The result of the comparison on weld metal data is shown in FIG.2.

The VISA-II curves are: (please note that the following formulae are in US units) for crack initiation:

$$K_{Ic} = 36.2 + 49.4 \cdot \exp(0.0104(T - RT_{NDT})) \quad T - RT_{NDT} < 50 \text{ } ^\circ\text{F} \text{ and}$$

$$K_{Ic} = 55.1 + 28 \cdot \exp(0.0214(T - RT_{NDT})) \quad T - RT_{NDT} > 50 \text{ } ^\circ\text{F}$$

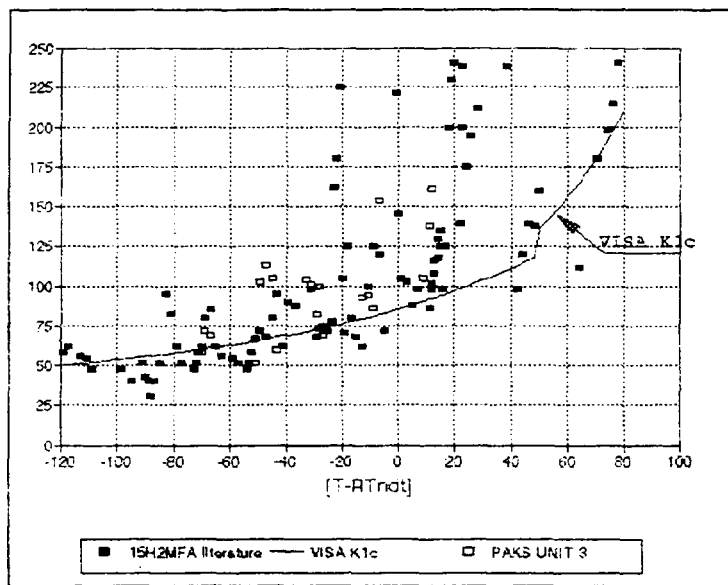


FIG. 3.

Verification of the VISA-II reference curve for 15H2MFA material

Flaw distribution.

The application of probabilistic codes requires a verified flaw distribution. The verification should be based on the ISI (In-service Inspection) results. The ISI testing of the RPV-s is very strict at NPP Paks. The core zone welds and beltline ring are partially checked yearly by automatic UT testing operating outside. During a four year period the outside UT testing covers the whole core zone. Moreover NPP Paks performs an inside UT testing in every four years (based on the good results this period is going to be changed for 8 years or even longer.) The results of the UT tests were compared with the most frequently applied flaw distribution functions. In case of Paks the so-called Marshall distribution gave the best fit, so this has been used.

2.5 Selection of hypothetical defects

According to the Russian Code which was valid at the beginning of the AGNES projekt, and international studies [4,5] the following three models were selected for study.

Model 1.: Axial semielliptical surface crack $a/c=2/3$, depth is $1/4T = 35$ mm. Location: the forging against the middle of the core.

Model 2.: Underclad axial crack in the ferritic welds, 4 mm deep and 50 mm long touching the interface of the clad, which is in complete contact with the vessel wall, and free of defects. Location: weld 5/6; weld 3/5.

Model 3.: Elliptical circumferential surface crack with a depth of 4 mm in the 15H2MFA weldment, and the clad is postulated broken (i.e. Σ depth=13 mm), $a/c=2/3$. Location: weld 5/6; weld 3/5.

3. Organization of the FM (fracture mechanics) analysis

The selected 18 cases multiplied with three crack models result in 54 FM analyses to be performed. Even if the temperature and stress distribution is common in some cases the number of the FM analyses is large. To reduce the calculation time a working process has been elaborated as shown in FIG.1.

In phase 1 of the calculation a one dimensional fast analytical code was used to evaluate the effect of the transient on the vessel integrity. If the results had shown that the safety factor during the transient event can go below 1.1 a more detailed analysis would have followed the first guess. If it was necessary crack arrest calculation or probabilistic analysis would have been used in the continuation.

Phase I. is a deterministic analysis evaluation of whether crack initiation can occur or not during PTS events. The stress and temperature distribution in the function of time is calculated by a fast analytical code ACIB-RPV (Advanced Calculation of Integrity of the Beltline of RPV). The K_{Ic} value belonging to the time scale and crack tip temperature divided by the actual K_I value gives the safety factor. ($S_f = K_{Ic}/K_I$). The safety criterion is that this factor must be higher than 1 plus 0.1 as safety margin for the ACIB-RPV program during the whole transient.

Phase II. If the calculated value in Phase I. had been less than 1.1 the whole calculation would have been repeated by a 3 dimensional finite element code. If the resulting safety factor is higher than 1.0, the assessment is finished, otherwise crack initiation may occur (the calculation is based on very conservative assumptions, and a calculated safety factor below 1.0 means only a low probability of crack initiation, not a vessel failure) and the calculation is continued according to Phase III with an analysis based on crack arrest assumption.

Phase III. If a crack is initiated during a PTS it generally runs into a hotter location where the material toughness is higher and it is arrested. If the crack is arrested before reaching 70% of the wall thickness the vessel is still considered safe. The calculation method is the same as in Phase I. but K_{Ia} (crack arrest toughness) is used instead of K_{Ic} . If the crack is arrested the evaluation continues according to Phase I, because during the remaining time of the PTS event crack initiation may occur again. If the crack becomes stable before reaching 70% of the wall thickness the vessel integrity is not affected by the tested PTS case.

Phase IV. The deterministic evaluation is based on very conservative assumptions. If the results of the assessment in Phase II do not prove the vessel to be safe, probabilistic analyses can be used. The acceptance criterion for probabilistic calculation is that the overall probability of through wall crack penetration (not the brittle fracture of the vessel) must be $<5 \cdot 10^{-6}$ event per reactor year (the probabilistic approach is presently not accepted by the existing codes and rules).

The Hungarian version of US NRC donated VISA-II code was used for this analysis.

4. SUMMARY OF THE RESULTS:

The rather conservative PTS calculations performed in the frame of the AGNES project have shown that NPP Paks units 1-4 can be safely operated at least until the 24th operational year, or more.

To evaluate the real lifetime and to run a life management program further study and research are necessary. Some of them have already been started, some others are still planned.

A short list of the life management actions performed, planned or under consideration at Paks NPP.

1. Use of low leakage core
2. Extension of the surveillance program
3. Heating up the ECCS water to 50 °C
4. Revising the operational regulations
5. Measuring the real K_{1c} and K_{Ia} values of 15H2MFA material and its weldments
6. Study of the thermal ageing effect.
7. Development of the calculation by considering the effects of:
 - The material properties distribution in the RPV wall
 - Cladding effect
 - Low-leakage core
 - Use of the extended surveillance results
 - Following the operational changes

According to preliminary calculations - after the suggested life management actions are done- the recalculated lifetime will reach 40-60 years of safety operation life for all NPP Paks units.

ACKNOWLEDGEMENTS

The authors wish to tank for the support and sponsorship of the PTS analisys methodology development given by the US NRC, NPP Paks, the National Atomic Energy Commission, and the OMFB.

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ASSESSMENT OF THE ZAPORIZHYA NPP UNIT 1 REACTOR PRESSURE VESSEL SAFETY

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1. INTRODUCTION

On April 20, 1995 during planned maintenance there was planned testing performed at unit 1 of IA "Zaporizhya NPP (ZNPP)" with reactor facility (RF) of VVER-1000/V-320 to check pulse protecting device of pressuriser (PPPD). At that time one of its valves failed to close. As the result, a pressure within the 1st circuit began to decrease that had lead to actuation of active and passive emergency core cooling system (ECCS) (cold water supply into the 1st circuit of RF in a purpose to cool core) and, consequently, thermal shock occurred at the reactor pressure vessel. According to the normative documents (ND) in force in Ukraine [1] this mode is being classified as an emergency situation.

This emergency situation had occurred at the ZNPP unit 1 while its being under "hot shutdown" in a natural coolant circulation mode. The main difference between emergency situation and mode with improper setting of PPPD described in the "Technical Safety Substantiation (TSS) is that this mode is being considered in the TSS under rated power of reactor with main circulation pumps (MCP) under operation. This difference is a substantial one. For this reason a necessity appeared to assess an integrity of referred reactor pressure vessel (RPV) under given emergency situation to judge whether results obtained meet the ND requirements [2] (safety assessment).

Under operation such RPV elements are being mostly affected as upper cowling, lower cowling, weld No. 3 and weld No. 4 situated in front of core. These elements material ageing process is the most intense one.

Thus, this work was aimed at investigation of structure material behaviour and RPV integrity assessment under thermal shock conditions while PPPD improper setting. At that time the most attention was drawn to above mentioned upper and lower cowlings along with welds No. 3 and 4 (hereinafter "elements").

To reach named goal the calculation was performed on RPV elements resistance to brittle fracture.

Due to lack of trustworthy information from NPP as to progressing of emergency situation under PPPD improper setting, two of its possible options were distinguished by readings of appliances to measure temperature and pressure within cold and hot legs of the 1st circuit and within RPV (as regards coolant flow rate and temperature values) by which calculation was performed as to RPV elements under consideration metal resistance to brittle fracture.

This calculation was performed by three methods in parallel as follows: by PNAE G-7-002-86 ("Standards for strength analysis of NPP equipment and pipelines"), by computer code VISA-II and by finite elements method (FEM). At that time permissible coefficients of stress intensity $[K_I]$ equalling to critical ones K_{IC} were determined in compliance with the requirements of [2], while calculated coefficients of stress intensity K_I - with the use of the named methods.

Utilisation of the three methods to calculate stresses and K_I was stipulated by the following reasons.

In 1994 SSTC NRS obtained from US NRC as a kind of assistance computer code VISA-II in assessment of RPV destruction probability under thermal shock consisting of two calculating blocks: deterministic and probabilistic ones. We did adapt it as regards VVER-type RPVs. It was followed by its verification necessity. In this purpose we did solve some simple testing tasks with the use of VISA-

II and method [2]. The testing calculation results allowed to detect the differences between the methodology of both approaches as regards determination of stresses and coefficients of stress intensity. Then the decision was made to address a real situation with a thermal shock with the use of [2], VISA-II and FEM to make the final conclusions on boundaries of VISA-II utilisation for VVER-type reactors and, at the same time, to assess IA ZNPP unit 1 RPV integrity under an emergency situation with improper setting of PPPD.

The 3rd valve of PPPD that afterwards failed to close did open at 3:37:36.. At 3:41:34 ECCS did actuate. In this calculation time origin was adopted as 3:42:40 (moment of temperature drop start by data of regular appliances). The transient period section of 10000 s length was under consideration that was subdivided into 10 pieces. After 10000 s temperature and stress gradients within RPV wall were negligible ones.

Since RPV wall cooling intensity was the most intense at the 4th weld area level, then the 4th weld area revealed to be the most loaded among all the elements considered. Loads for the whole RPV, by conservative approach, were adopted as those for the 4th weld area.

Calculation of stress intensity coefficients K_I under improper setting of PPPD was performed by the two calculating schemes as follows:

1) RPV with superficial semi-elliptical crack in the 4th weld area with the parameters as follows: $a/c=2/3$, $a/s=0.25$, where a is a crack depth, c is a crack half length, s is a RPV weld thickness;

2) RPV with semi-elliptical crack in the 4th weld area with the parameters as follows: $a/c=1/3$, $a/s=0.25$.

In the 1st calculating scheme the brittle strength parameters were calculated by PNAE G-7-002-86 method and FEM, and in the 2nd - with the use of VISA-II code.

Selection of the calculating schemes was stipulated by the fact that, in compliance with the requirements of [2] to assess brittle strength it is necessary to address the 1st one that is not envisaged for VISA-II code. Below the descriptions are presented for each method.

2. DESCRIPTION OF METHODS TO CALCULATE RPV METAL RESISTANCE TO BRITTLE FRACTURE UNDER THERMAL SHOCK

2.1 Assumptions made under calculation

When stresses within RPV wall were determined, only radial temperature gradients were taken into account. It simplified the calculational procedure. Residual weld stresses were not taken into account during calculating.

2.2 PNAE G-7-002-86 method

According to the requirements of [2], RPV was considered as two-layered vessel with thick walls and non-uniform physical and mechanical material properties subject to internal pressure and temperature impacts (see Fig.2.1). Elastic modules, coefficients of linear expansion for plating and base metal were addressed as temperature functions.

When the main stress components were determined by pressure and temperature impacts, plating and base metal were addressed separately with their inter-influencing taking into account and without a crack within a wall.

By stress components from pressure and temperature loads integral stresses were calculated at the main areas which then were divided into membrane and bend ones. Then an area size was determined which within a bend stresses component retains its positive value, and there coefficient of stress intensity was calculated by membrane and bend component of stresses for longitudinal superficial semi-elliptical cracks with the parameters as follows: $a/c=2/3$, $a = 0.25s$.

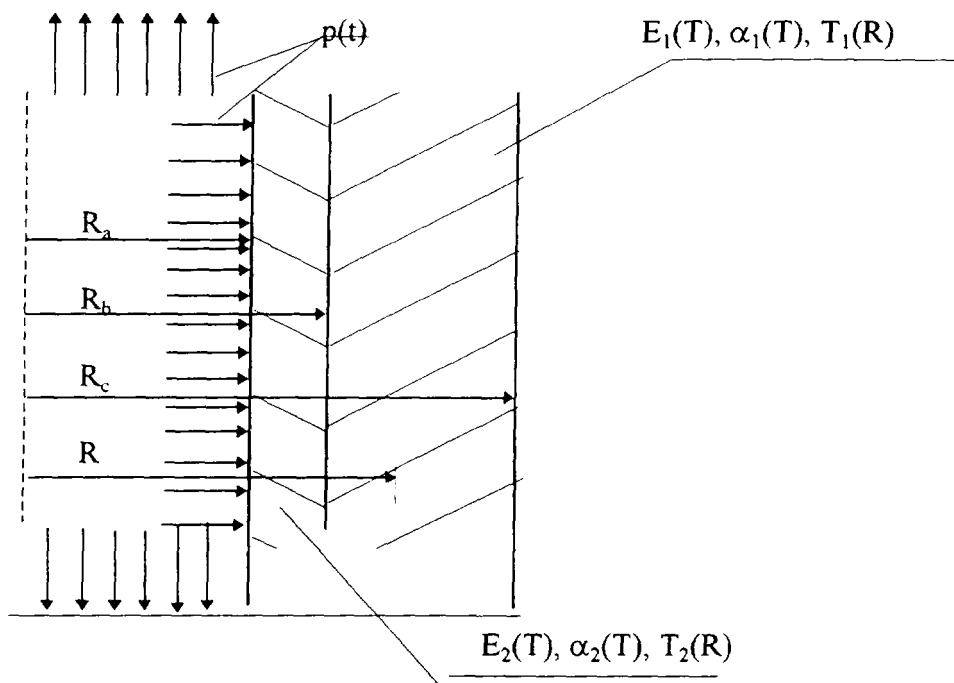


Fig. 2.1

2.2 VISA-II computer code

In VISA-II code stresses from pressure and temperature stresses arising due to temperature gradients within RPV wall were determined by calculating scheme of loading presented in Fig.2.2 where plating material was replaced with the base metal.

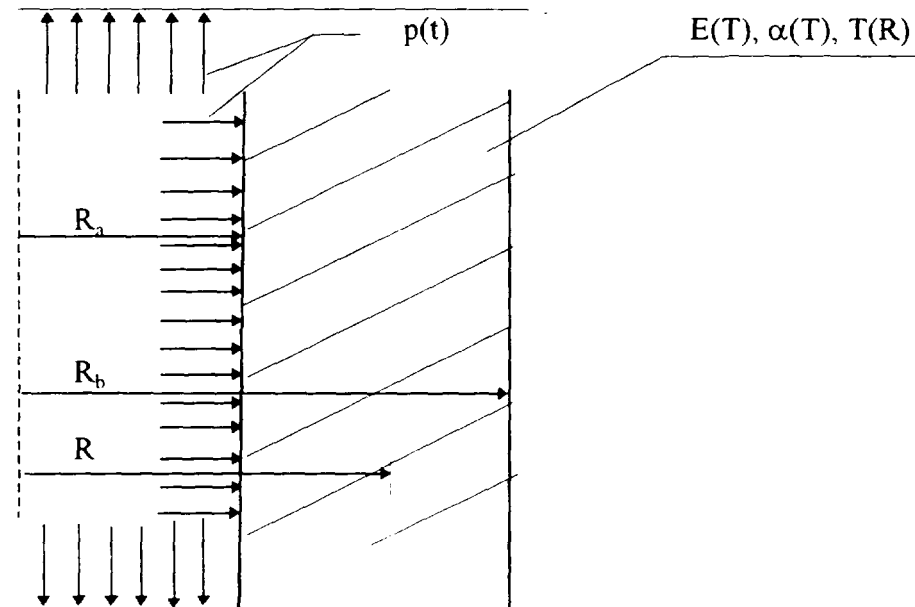


Fig. 2.2

Calculating scheme to determine stresses arising due to different coefficients of linear expansion of plating and base metal is presented in Fig. 2.3

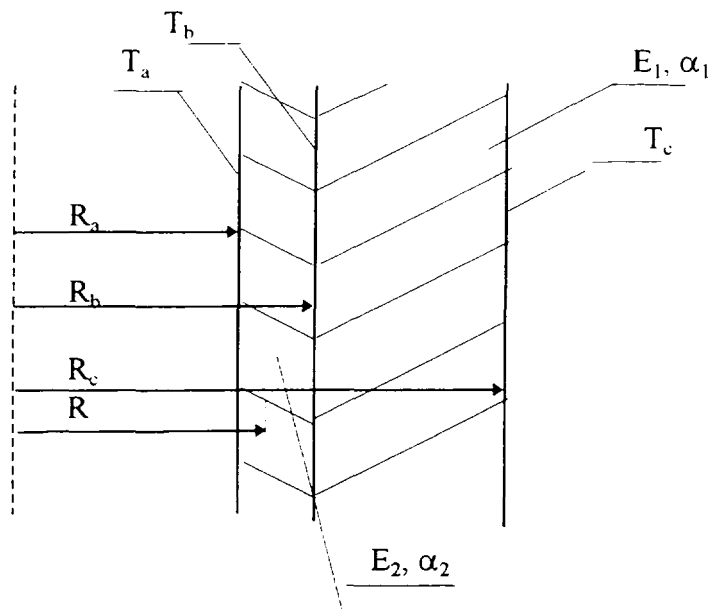


Fig. 2.3

Coefficients of stress intensity were determined for longitudinal superficial semi-elliptical cracks with ratio of $a/c=1/3$, $a=0.25s$ from each stress component separately. Then integral coefficient of stress intensity was determined.

2.3 Finite elements method

This method was used to calculate the stresses at the main areas in compliance with the scheme presented in Fig. 2.1 for a body without a crack. Coefficients of stress intensity were determined with the use of energetic method by stresses calculated for a body with longitudinal superficial semi-elliptical crack with the parameters as follows: $a/c=2/3$, $a=0.25s$. The method description is presented in Annex 1.

2.4 Determination of permissible coefficient of stress intensity $[K_I]$

According to the requirements of PNAE G-7-002-86, permissible coefficient of stress intensity is equal to critical one K_{IC} , being determined by the formula as follows:

$$K_{IC}=35+53e^{0.0217(T-T_k)}$$

where

T is a temperature at the deepest point of a crack at a considered moment;

T_k is a critical temperature of a material brittleness.

$$T_k = T_{k0} + \Delta T_N + \Delta T_F,$$

where

T_{k0} is a critical temperature of a material brittleness at an initial state;

ΔT_N is a shift of critical brittleness temperature due to a cyclic damage ability;

ΔT_F is a shift of critical brittleness temperature due to influence of a neutron irradiation.

Temperature ageing impact was not taken into account (assumption of [2]). Shift of a critical brittleness temperature due to cyclic damage ability was adopted by the end of service life term $\Delta T_N = 20^\circ\text{C}$.

It is well-known that national ND methods [2] does not allow to take into account nickel impact upon RPV materials embrittlement under determination of ΔT_F . Moreover, unloading and testing of referred unit 1 surveillance-specimens by the moment of the works implementation were not performed. That is why while calculating of resistance to brittle fracture additionally results of surveillance-specimens testing were cited from units 2-3 of the same site [3,4] along with formulas for ΔT_F determination in USA (Regulatory Guide-1.99 Revisions 1 and 2) and France (RCC-M Code, RSEM Code) [5].

Shift of a critical brittleness temperature due to impact of neutron irradiation ΔT_F was determined by data of [2], results of ZNPP units 2-3 surveillance-specimens testing [3, 4] and by formulas from USA and France [5] with a real chemical composition of ZNPP unit 1 RPV cowlings and welds along with a spectrum of energy of neutrons and a calculated fluence value at a top of a crack with a depth of $a = 0.25 \cdot s$ (s is a wall thickness) taking into account.

Under development of permissible coefficient of stress intensity $[K_I]$ the maximum values of ΔT_F , were used obtained by all the named methods.

3. INPUT DATA

3.1 There are the results of following thermal-hydraulic parameters within RPV calculation presented in Figs. 3.1-3.5 for two possible options of an emergency situation progressing:

- coolant pressure;
- coolant temperature;
- coefficient of heat transfer at interface water/wall around the 4th weld.

3.2 The results of temperature fields within RPV calculation by item 3.1 data at the 4th weld level are presented in the following way:

- for the 1st option - in Fig. 3.6;
- for the 2nd option - in Fig. 3.7.

3.3 Actual mechanical properties of RPV base metal (BM) and weld metal (WM) were adopted according to the operational data (see Tables 1, 2). BM as well as cladding elasticity modules and coefficients of linear expansion are presented in Tables 3, 4.

Table 1. Actual mechanical properties of BM

RPV element	Material	R_m , kgf/mm ² (under temperatures of 20/350°C)	$R_{p0.2}$, kgf/mm ² (under temperatures of 20/350°C)	Z, % (under temperatures of 20/350°C)
Upper cowling	15Ch2NMFA-A	68.92 / 59.52	61.92 / 53.1	77.35 / 74.45
Lower cowling	15Ch2NMFA-A	74.9 / 61.4	63.77 / 54.25	75.05 / 74.05

Table 2. Actual mechanical properties of WM

Weld No.	R_m , kgf/mm ² (under temperatures of 20/350°C)	$R_{p0.2}$, kgf/mm ² (under temperatures of 20/350°C)	Z, % (under temperatures of 20/350°C)
3	59.65 / 50.5	49.65 / 41.75	74.65 / 75
4	54.5 / 53.12	50.5 / 44	75 / 75.17

Table 3. RPV BM and cladding elasticity modulus E, GPa

Material	Temperature, °C						
	50	100	150	200	250	300	350
15Ch2NMFA-A	207	205	202	200	197	195	190
08Ch18N10T	202	200	195	190	185	180	175

Table 4. RPV BM and cladding temperature coefficient of linear expansion α , mkK⁻¹

Material	Temperature, °C						
	50	100	150	200	250	300	350
15Ch2NMFA	11.5	11.9	12.2	12.5	12.8	13.1	13.4
15Ch2NMFA-A	11.5	11.9	12.2	12.5	12.8	13.1	13.4
08Ch18N10T	16.4	16.6	16.8	17.0	17.2	17.4	17.6

Note:

E- elasticity modulus;

a - coefficient of linear expansion;

$R_{p0.2}$ - minimum value of yield stress;

R_m - minimum value of ultimate resistance;

Z- relative reduction of a specimen cross-section while static destructing under extension.

3.4 Content of copper, phosphorus and nickel as well as temperatures of brittle-plastic transition beginning for elements under consideration are presented in Tables 5, 6.

Table 5. Parameters for unit 1 RPV BM

Cowling	Copper content, weight %	Phosphorus content, weight %	Nickel content, weight %	Initial brittleness temperature, T_{K0} , °C
1	2	3	4	5
RPV upper cowling	0.03	0.008	1.26	-40
RPV lower cowling	0.08	0.007	1.17	-30

Table 6. Parameters for unit 1 RPV WM

Weld No.	Copper content, weight %	Phosphorus content, weight %	Nickel content, weight %	Initial brittleness temperature, T_{K0} , °C
1	2	3	4	5
3	0.03	0.005	1.10	-20
4	0.03	0.006	1.11	-10

3.5 Calculated maximum values of fast neutrons fluence at inner surface of ZNPP unit 1 RPV for 8 fuellings operation period are presented in Table 7.

Table 7. F , 10^{18} neutrons/cm²

Upper cowling	Lower cowling	Weld No. 3	Weld No.4
12.0	10.4	11.8	9.85

3.6 Since heat transfer coefficient at water/wall interface in VISA-II code is a constant which does not depend upon time, then its values were adopted as follows:

- option 1: $\alpha = 175 \text{ W/m}^2\text{°C}$;
- option 2: $\alpha = 380 \text{ W/m}^2\text{°C}$.

Pressure vs time plots and coolant temperature in the area around 4th weld being adopted under calculation by VISA-II code are presented in Figs. 3.8-3.10.

Values of temperature within RPV wall calculated by VISA-II method are presented in Figs. 3.11, 3.12

4. RESULTS OF RESISTANCE TO BRITTLE FRACTURE CALCULATION

4.1 Results of stresses calculation by PNAE G-7-002-86 method and FEM

Maximum stress values under PPPD improper setting were fixed in the following way:

- for option 1: at a time moment of 800 s (see Figs. 4.1, 4.2);
- for option 2: at a time moment of 2400 s (see Figs. 4.3, 4.4).

4.2 Results of stresses calculation by VISA-II code

The values of circumferential stresses calculated with item 3.6 data taking into account are presented in Figs. 4.5, 4.6.

4.3 Results of and coefficients of stress intensity by PNAE G-7-002-86 method, FEM and VISA-II code

Values of calculated stress intensity coefficients for longitudinal superficial semi-elliptical cracks are presented in Figs. 4.7, 4.8.

4.4 Assessment of ZNPP unit 1 RPV brittle strength under PPPD improper setting

4.4.1 According to the requirements of [2], calculation of resistance to brittle fracture is being performed up to the temperature of brittle-plastic transition, when brittle fracture is possible. It is supposed that above this temperature the destruction mechanism transforms itself into quasi-brittle or tough one.

There are time moments of emergency situation for which brittle fracture is possible presented in Table 8.

Table 8

Element	Option	Time moments, s
Upper cowling	1, 2	9000, 10000
Lower cowling	1, 2	8000, 9000, 10000
Weld No. 4	1, 2	10000

The analysis performed did show that under an emergency situation progressing with PPPD improper setting brittle state of metal for weld No. 3 did not come.

4.4.2 There are results of ZNPP unit 1 RPV elements brittle fracture calculation by [2] method, FEM [when a defect is supposed to be a longitudinal superficial semi-elliptic crack ($a=0.25s$, $a/c=2/3$)] and VISA-II code (with a supposed defect in a shape of longitudinal superficial semi-elliptical crack ($a=0.25s$, $a/c=1/3$)) for time moments presented in table 8.

Table 9. Option 1

Element	Time, s	K_I , MPa*m ^{1/2}			$[K_I]$, Mpa*m ^{1/2}
		PNAE G-7-002-86	FEM	VISA-II	
Upper cowling	9000	52.13	19.8	47	163.2
	10000	50.16	19.2	45	141.4
Lower cowling	8000	53.30	20.1	49	156.6
	9000	52.12	19.8	47	132.4
	10000	50.15	19.2	45	118.0
Weld No. 4	10000	50.16	19.2	45	139.7

Table 10. Option 2

Element	Time, s	K_I , MPa*m ^{1/2}			$[K_I]$, Mpa*m ^{1/2}
		PNAE G-7-002-86	FEM	VISA-II	
Upper cowling	9000	52.16	19.1	44	149.0
	10000	48.94	18.4	42	132.5
Lower cowling	8000	55.10	19.8	47	140.9
	9000	52.16	19.1	44	123.2
	10000	48.94	18.4	42	115.2
Weld No. 4	10000	48.94	18.4	42	128.7

CONCLUSIONS

The resistance to brittle fracture is being considered as assured provided at the top of the crack addressed condition $K_I \leq [K_I]$ is met. By the results of calculation (see Tables 9, 10) this condition is observed, and, consequently, growth of calculated cracks for all thermal shock duration at ZNPP unit 1 was detected in no one from the three models adopted.

VISA-II code method concerning stresses determination gives conservative values for plating that leads, consequently, to over-increased values of K_I .

To calculate such emergency situations with a variable coefficient of heat transfer VISA-II code utilisation becomes a problematic one. It is being explained by the difficulties when equivalent (to a real one, changing with a time passing) unchanged with a time coefficient of a heat transfer.

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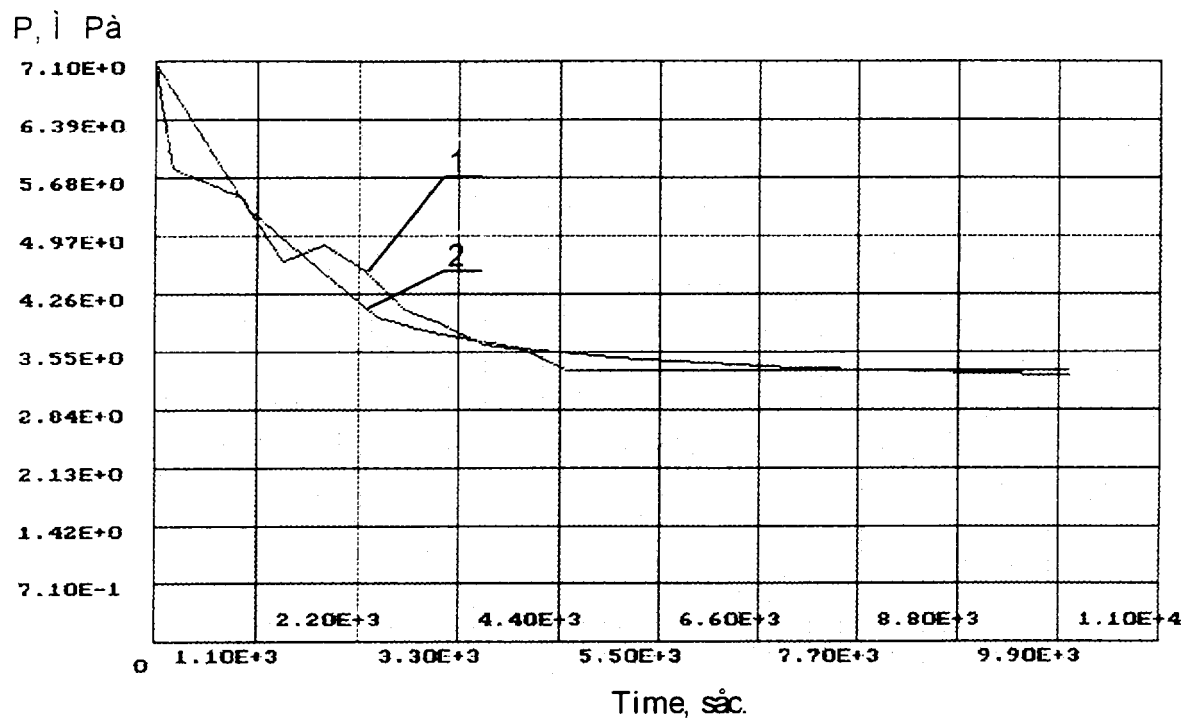


Fig. 3.1 The Pressure at the PWR.

- 1- The testimony sensor (YC30P23);
- 2- The curves which used in the calculation of strength.

T, °C

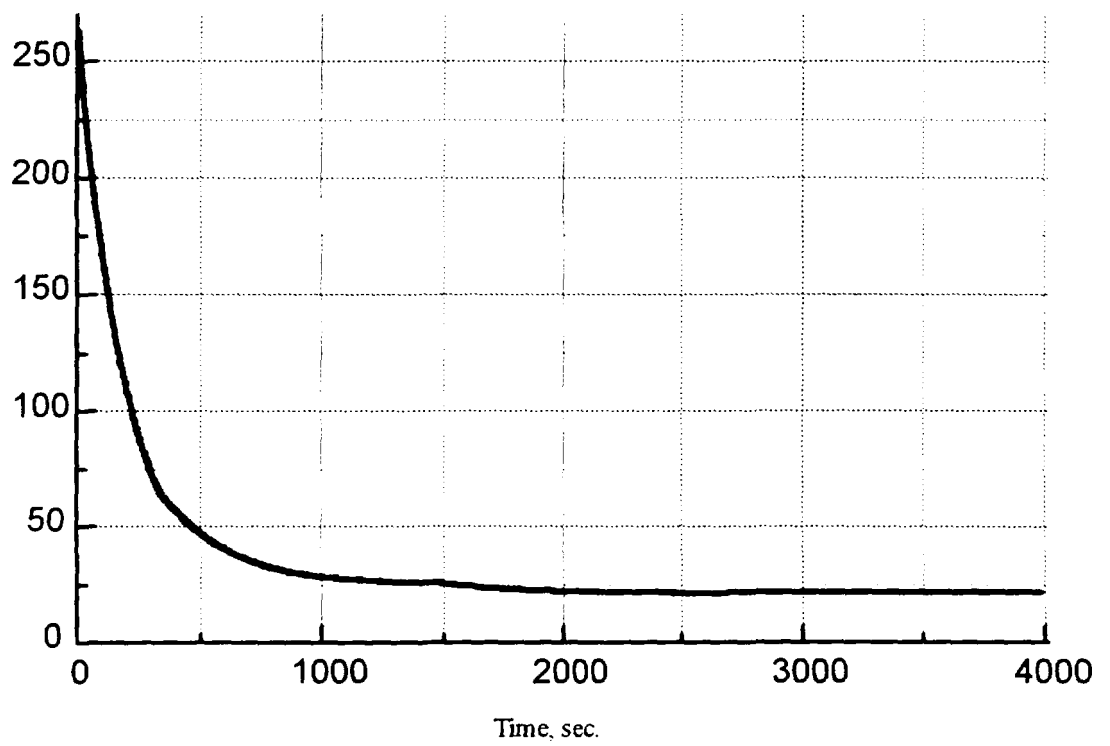
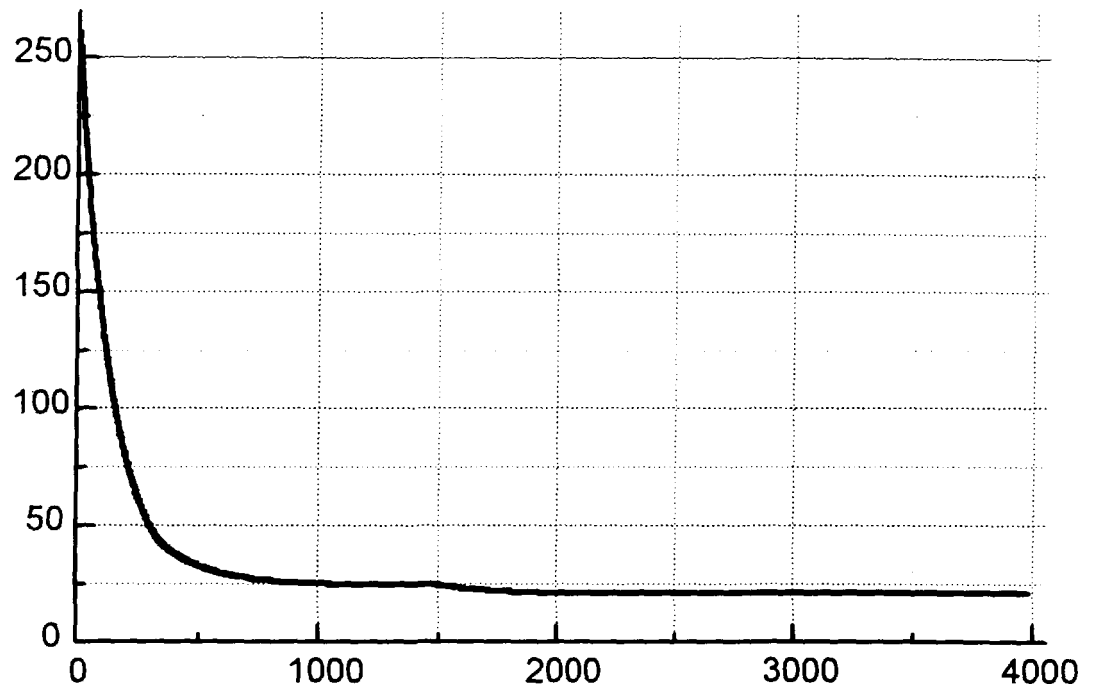


Fig. 3.2 The graphics are water temperature at the entry PWR and near weld 4.
Option 1.

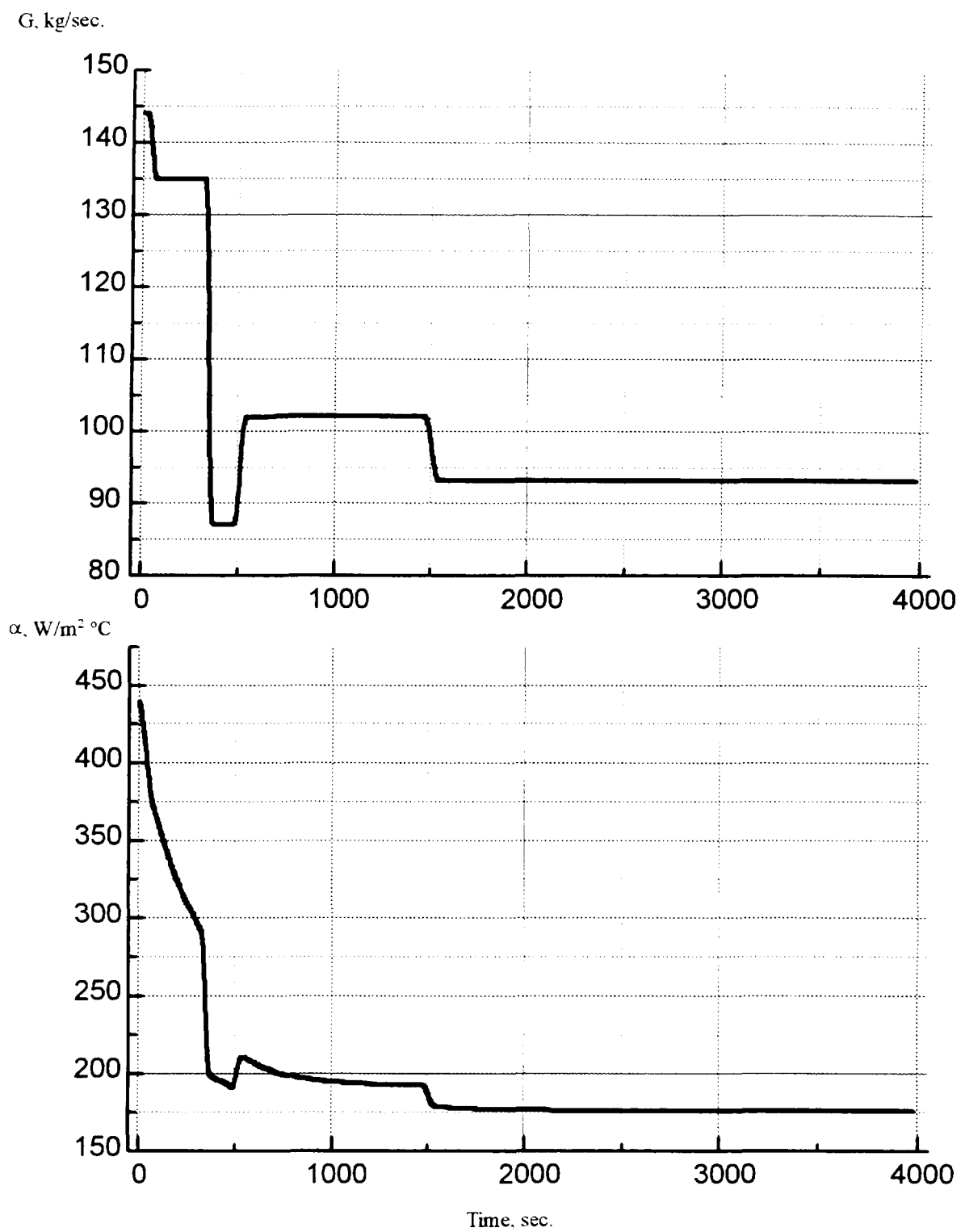


Fig. 3.3 The graphics are coolant discharge and heat transfer coefficient near weld 4.
Option 1.

T, °C

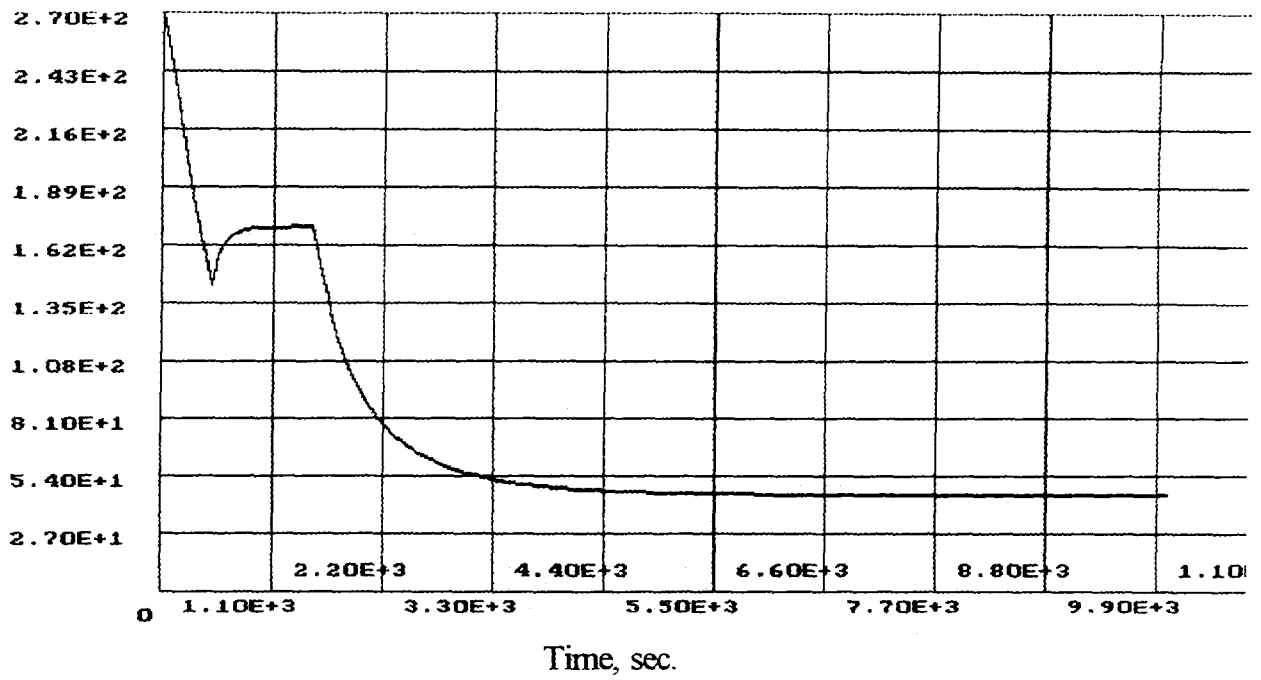


Fig. 3.4 The water temperature near weld 4. Option 2

W/(°C*m²)

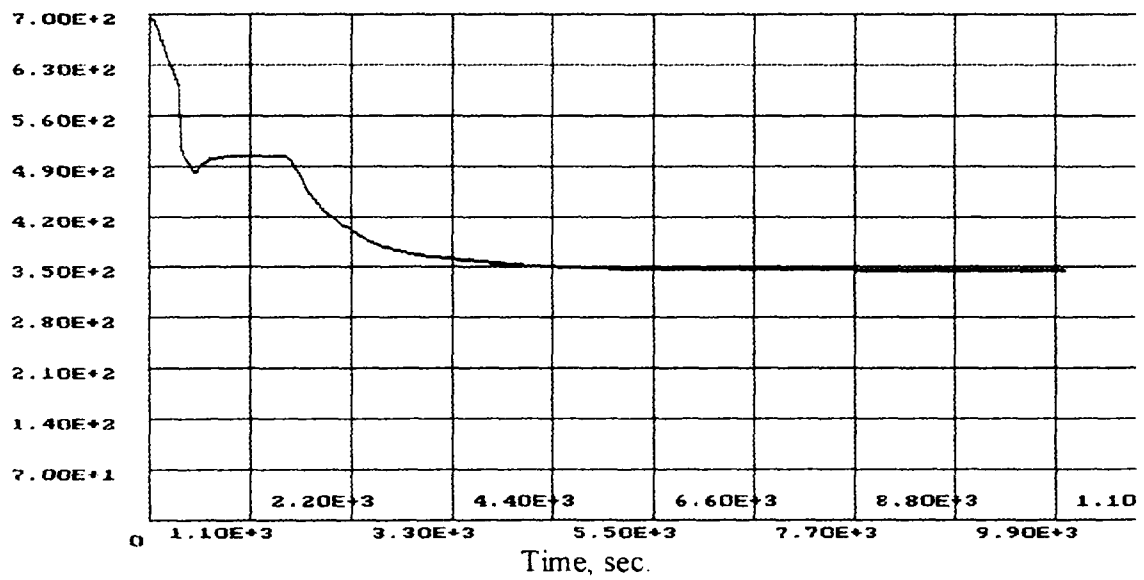


Fig. 3.5 The heat transfer coefficient near weld 4. Option 2

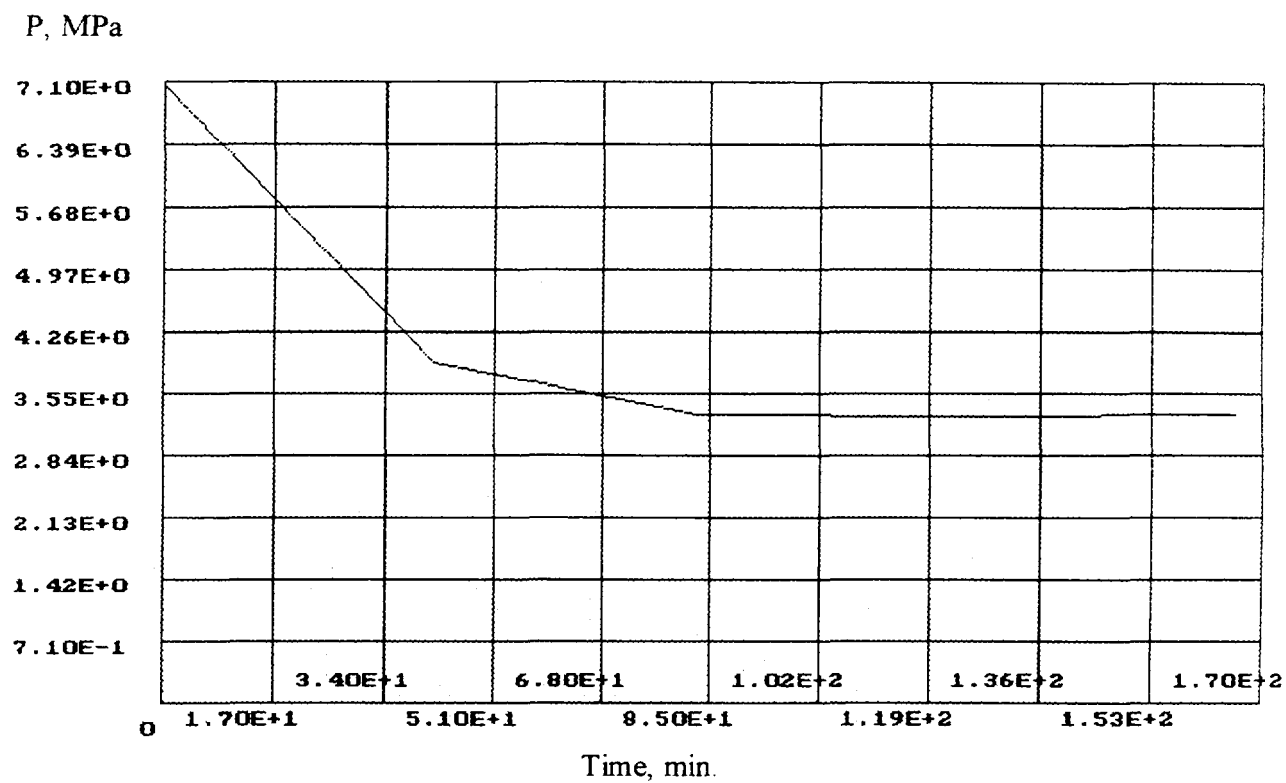


Fig. 3.8 The Pressure at the PWR. The VISA-II code

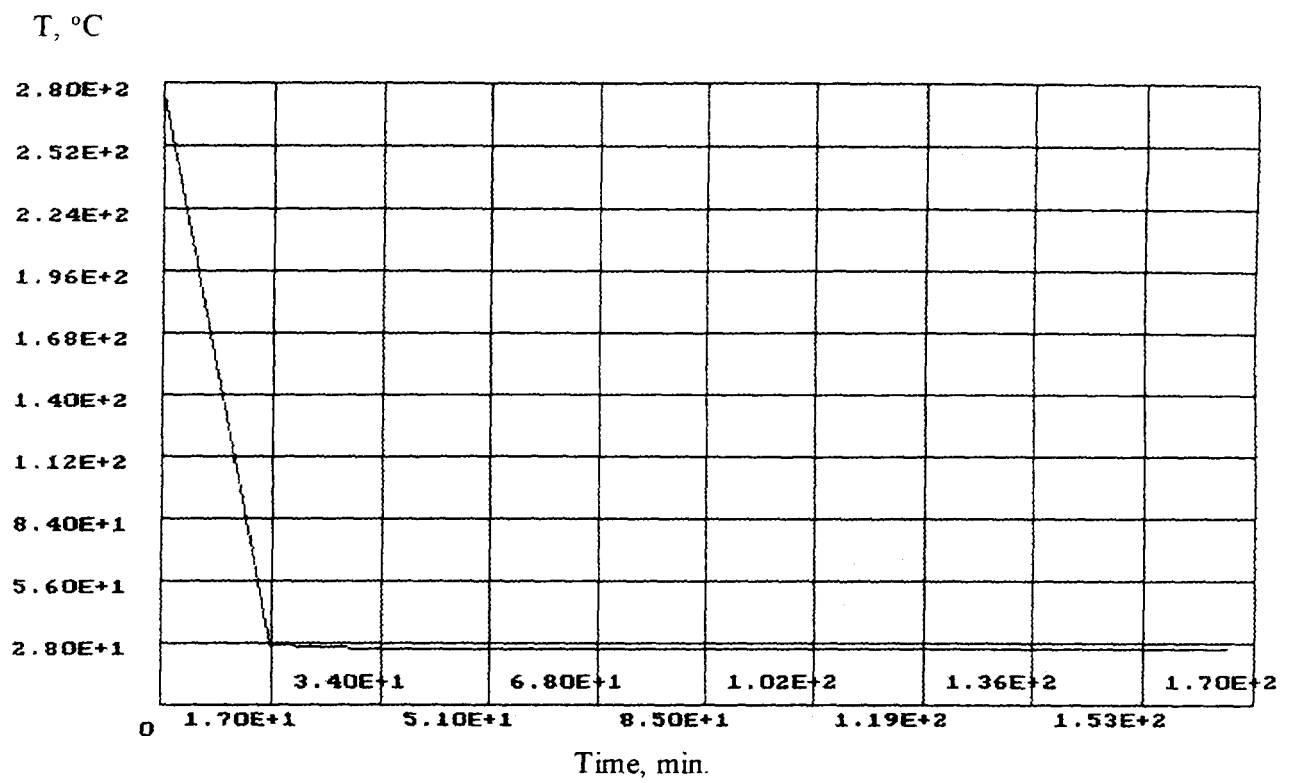


Fig. 3.9 The water temperature near weld 4. Option 1. The VISA-II code

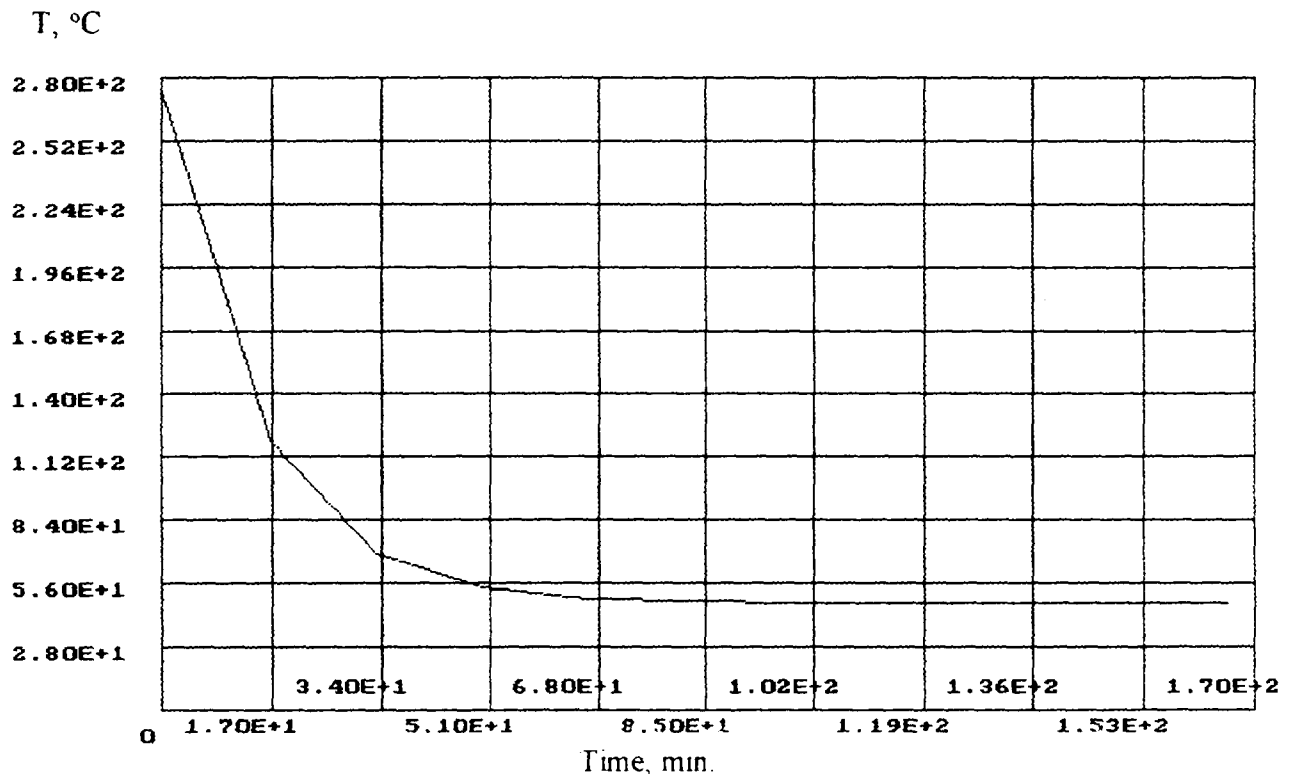


Fig. 3.10 The water temperature near weld 4. Option 2. The VISA-II code

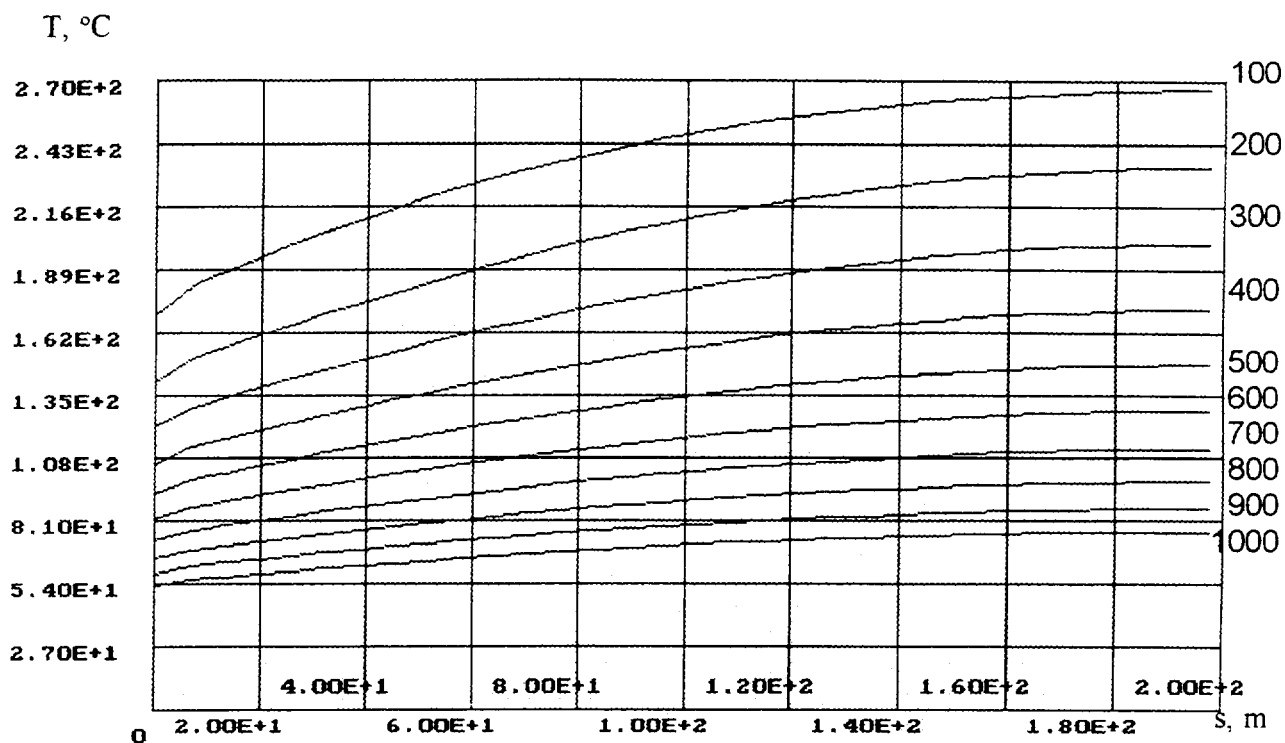


Fig. 3.11 The temperature distribution in the wall of pressure vessel near weld 4.
Option 1. The VISA-II code

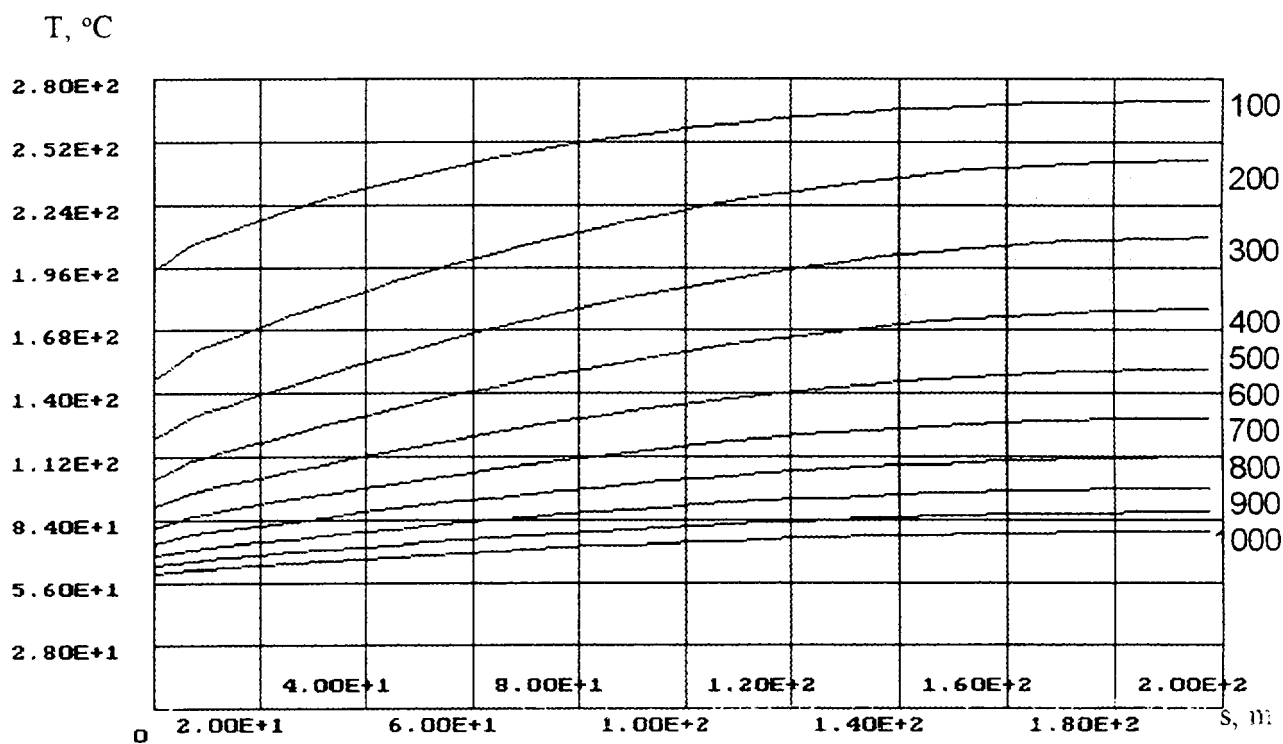


Fig. 3.12 The temperature distribution in the wall of pressure vessel near weld 4.
Option 2. The VISA-II code

σ_{θ} , MPa

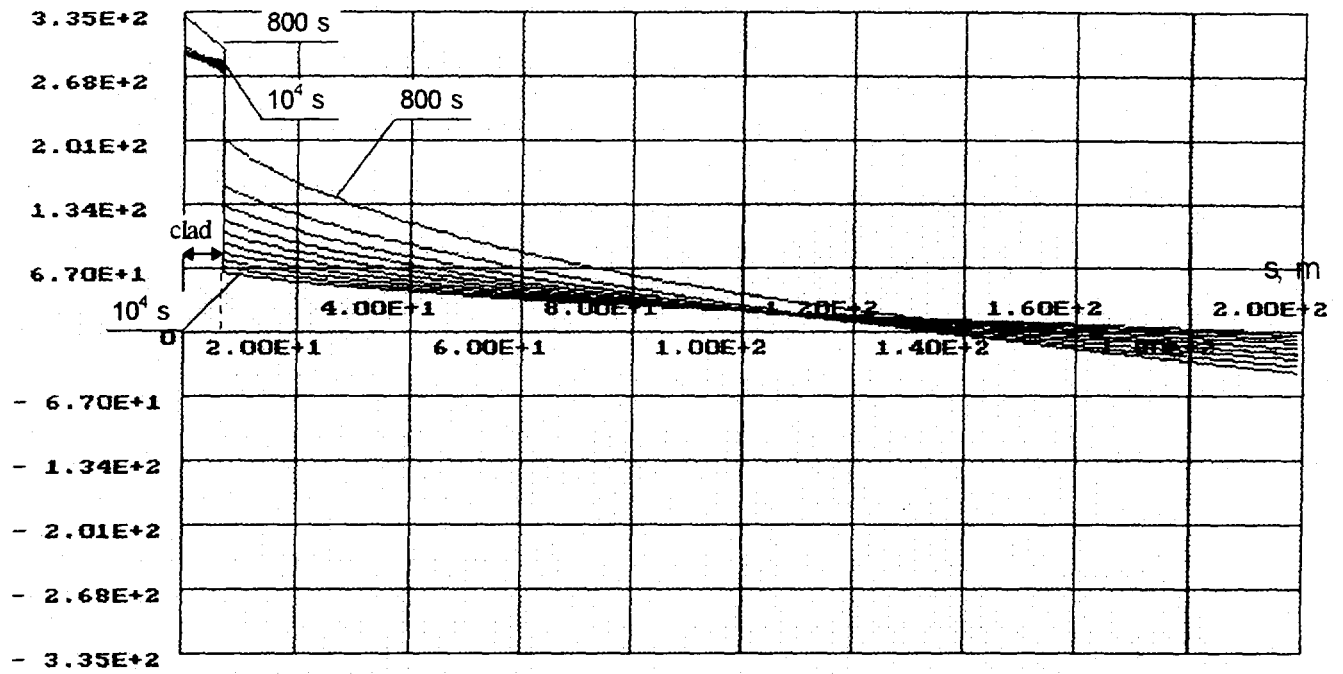


Fig.4.1 The Methods PNAE G-7-002-86. Option 1. The circumferential stresses distribution in the wall of pressure vessel near weld 4.

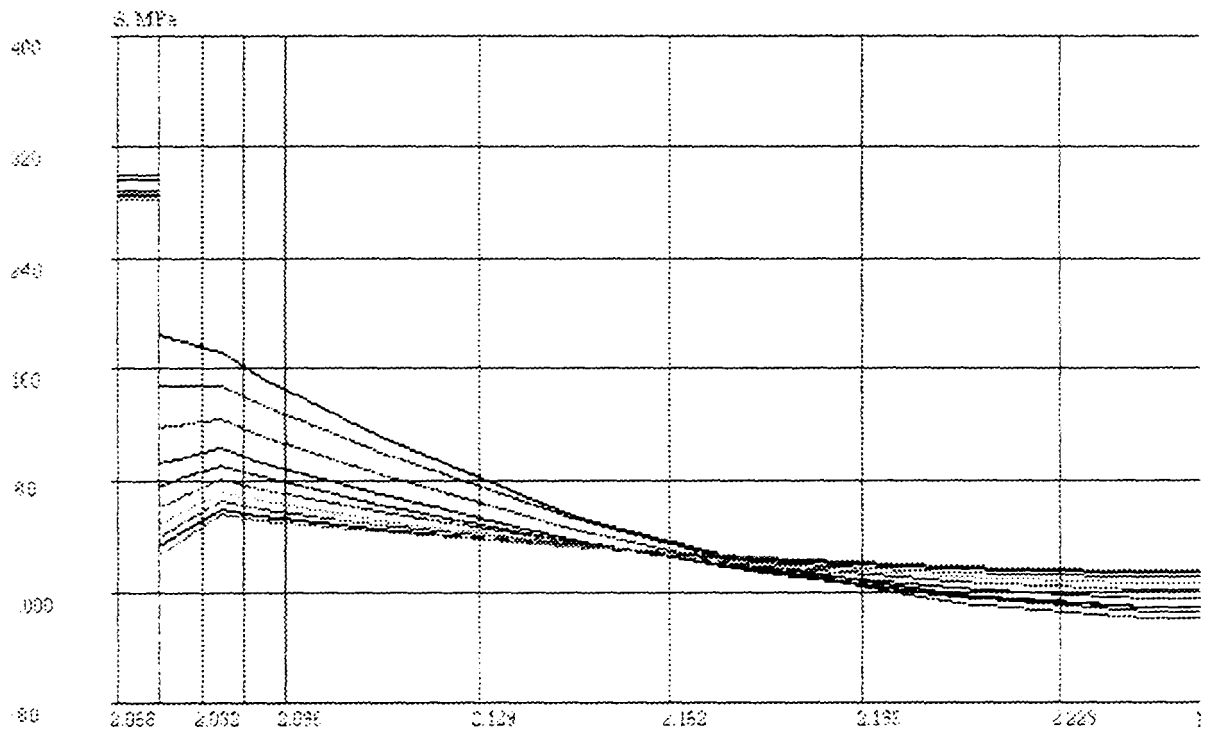


Fig. 4.2 Finite Element Method. Option 1. The circumferential stresses distribution in the wall of pressure vessel near weld 4.

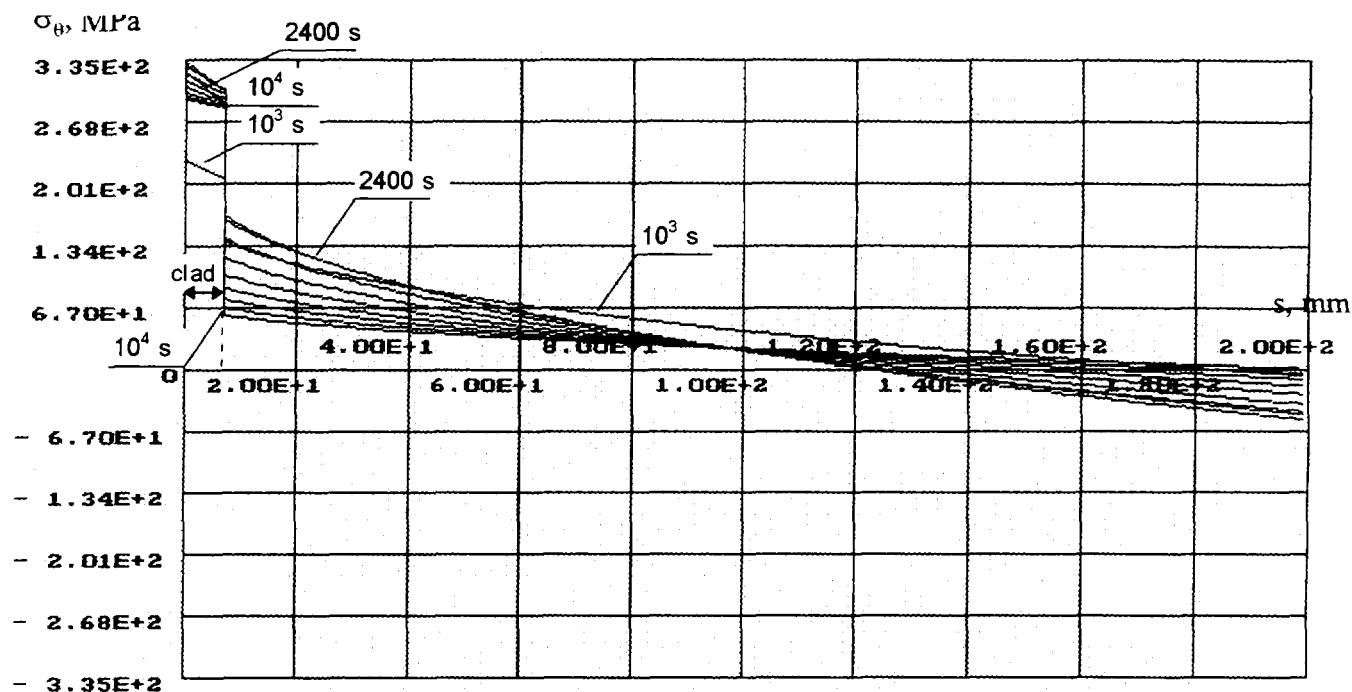


Fig. 4.3 The Methods PNAE G-7-002-86. Option 2. The circumferential stresses distribution in the wall of pressure vessel near weld 4.

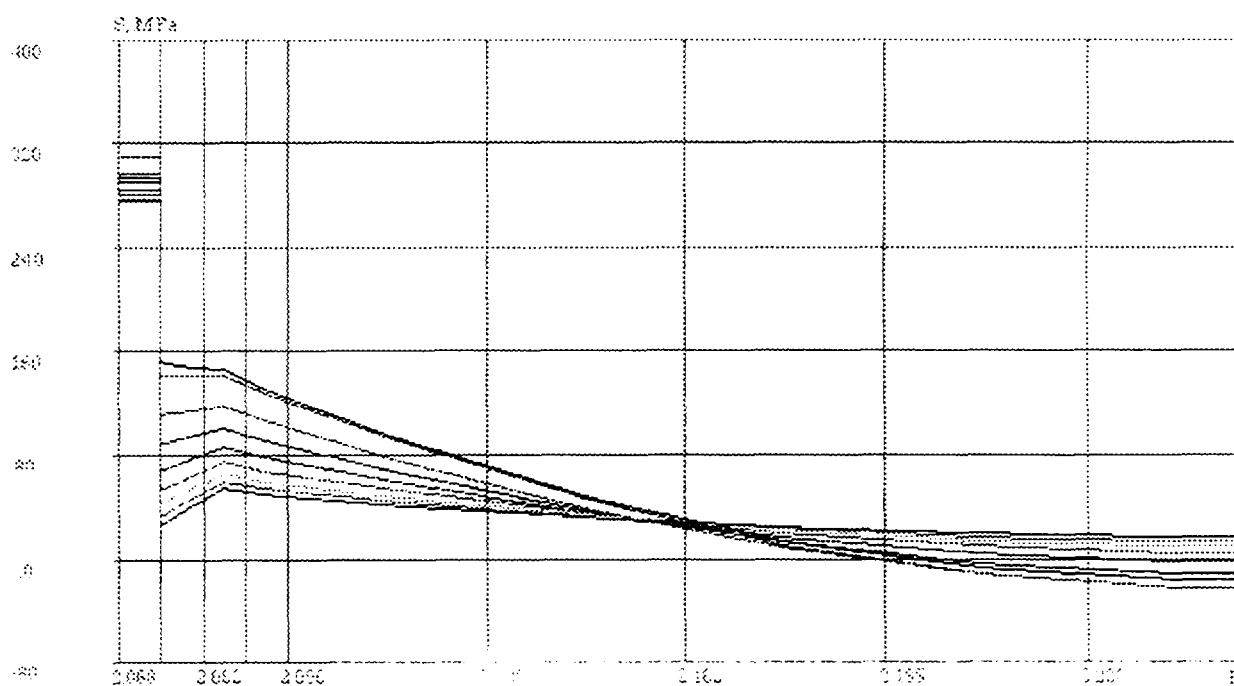


Fig. 4.4 Finite Element Method. Option 2. The circumferential stresses distribution in the wall of pressure vessel near weld 4.

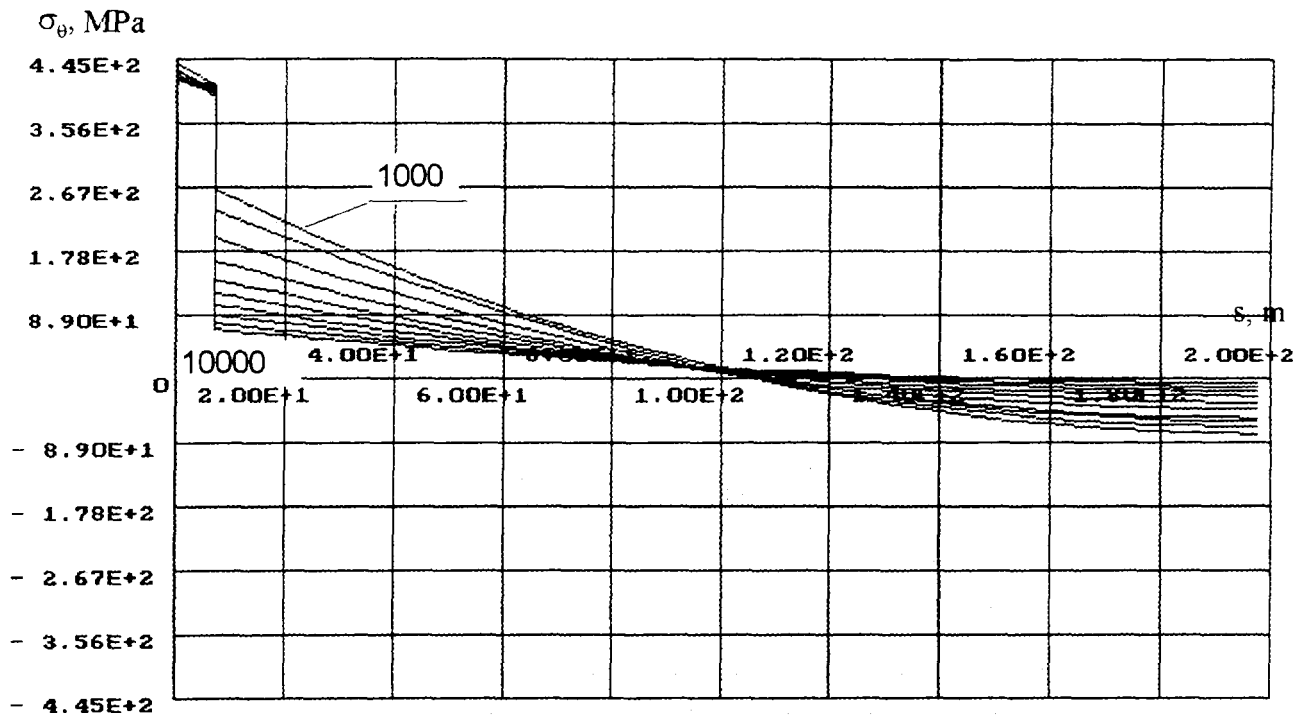


Fig. 4.5 The VISA-II code. Option 1. The values of circumferential stresses distribution in the wall of pressure wessel near weld 4.

**POOR QUALITY
ORIGINAL**

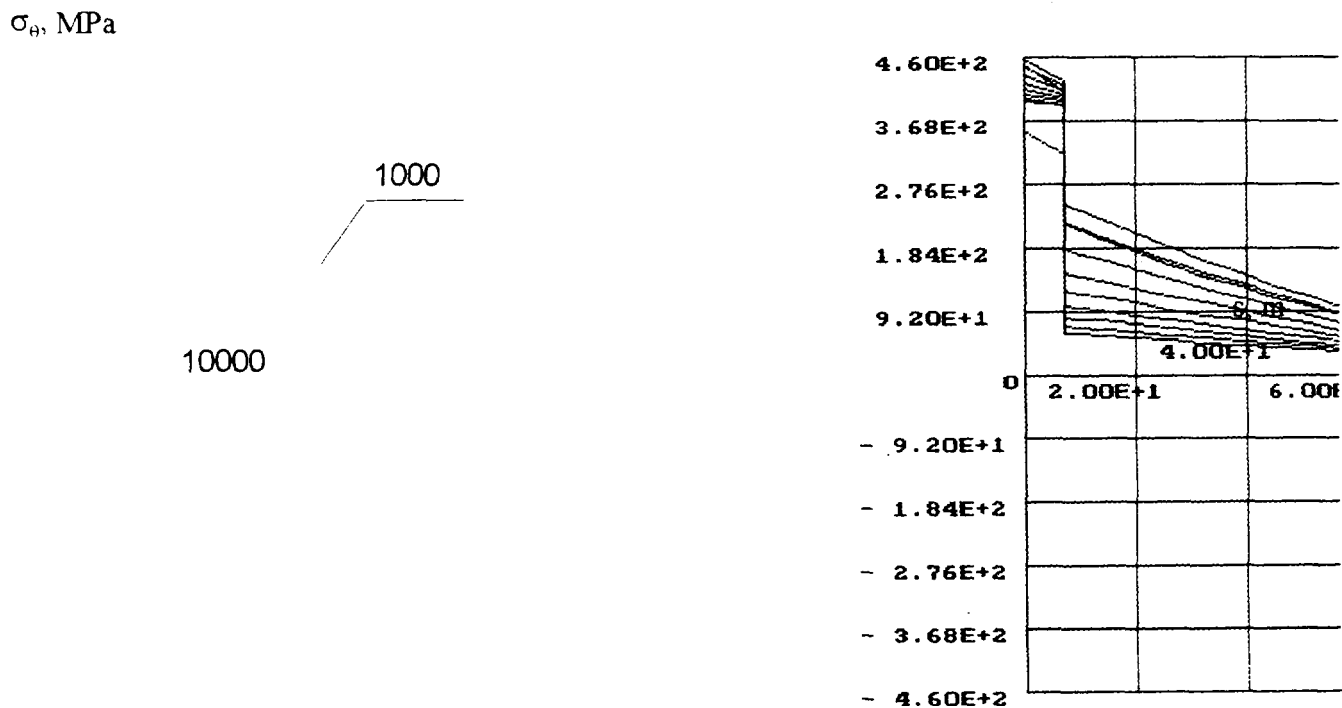


Fig. 4.6 The VISA-II code. Option 2. The values of circumferential stresses distribution in the wall of pressure wessel near weld 4

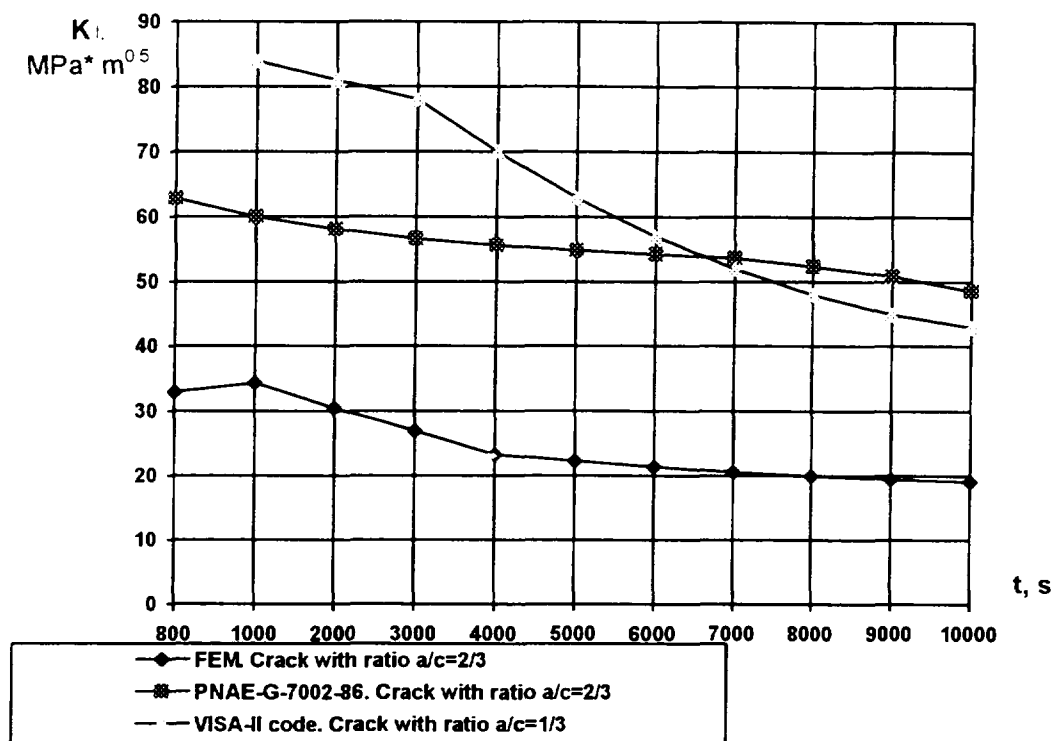


Fig. 4.7. Option 1. The values of K_I at the tip of the half elliptical crack with $a=0.25$ s.

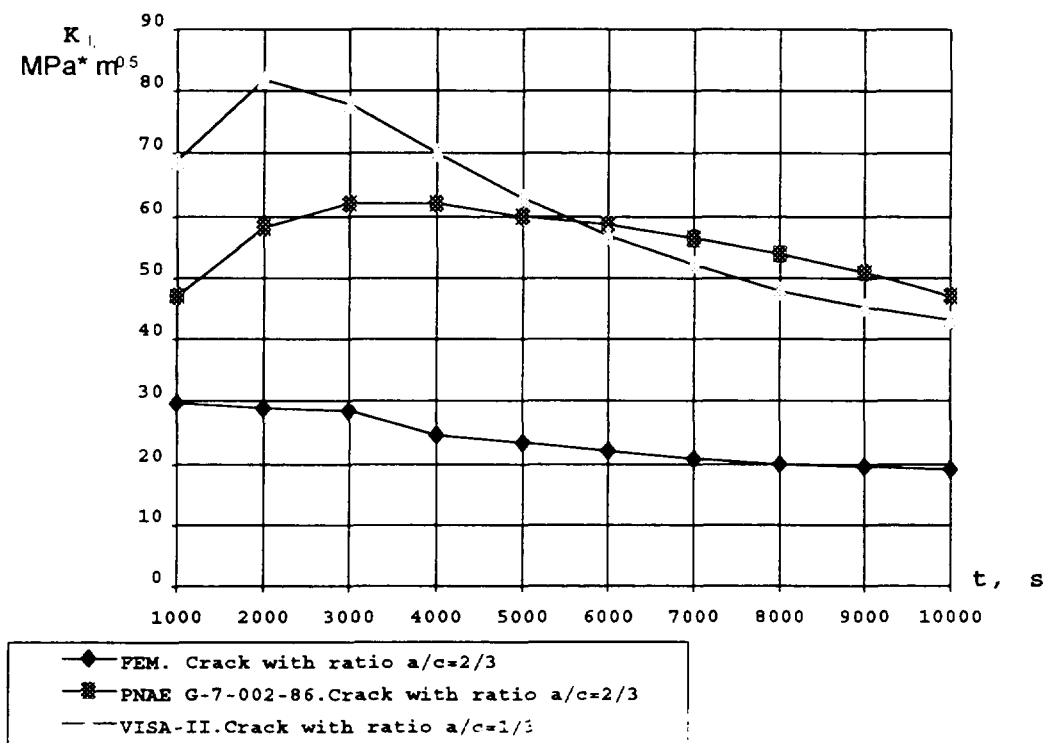


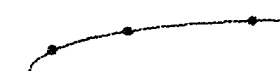
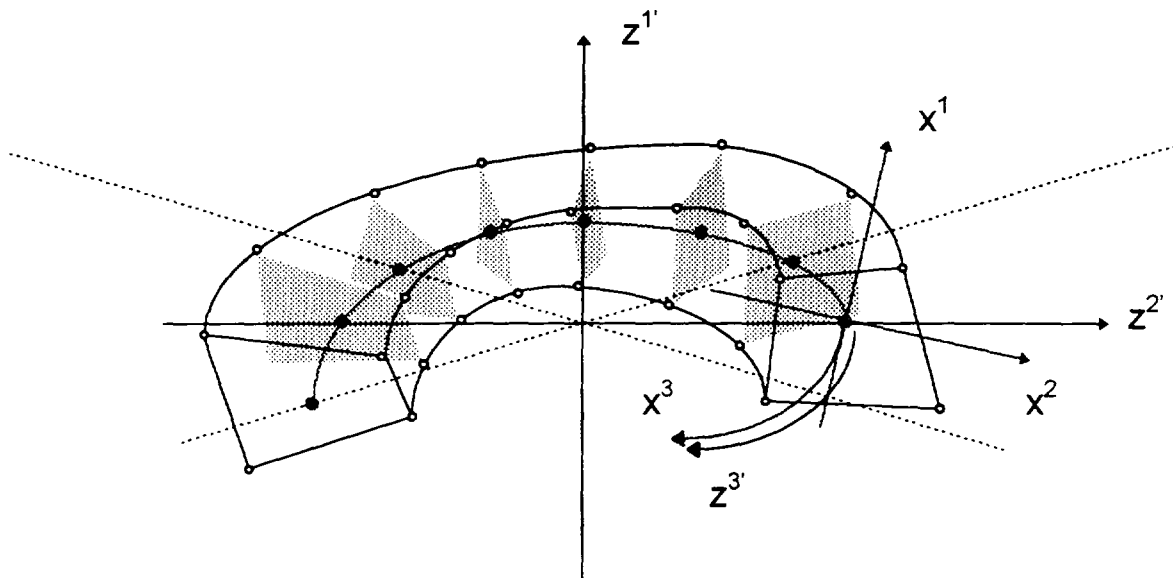
Fig. 4.8 Option 2. The values of K_I at the tip of the half elliptical crack with $a=0.25$ s.

Strength and integrity analysis of RPV by Semi-analytical Finite Element Method (SFEM)

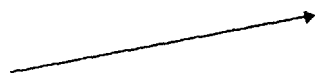
SFEM ensures high efficiency of numerical simulation for 3D problems of solids of revolution.

Main governing equations of SFEM

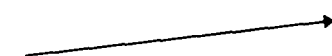
Annular closed finite element with variable in circumferential direction material properties is used.



- circle of cross-section centres with nodes for numerical integration in circumferential direction



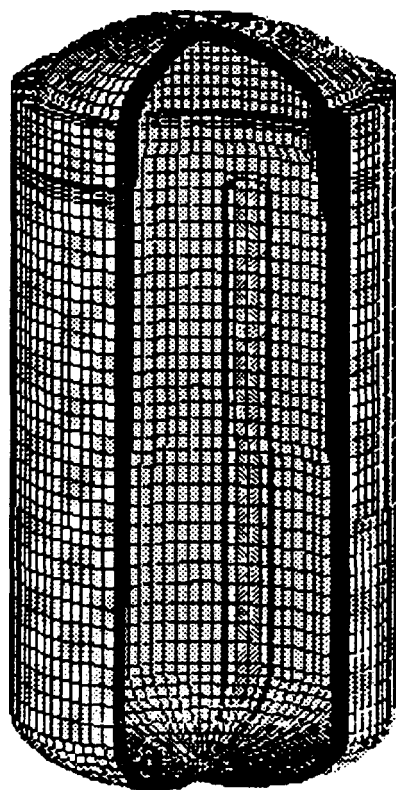
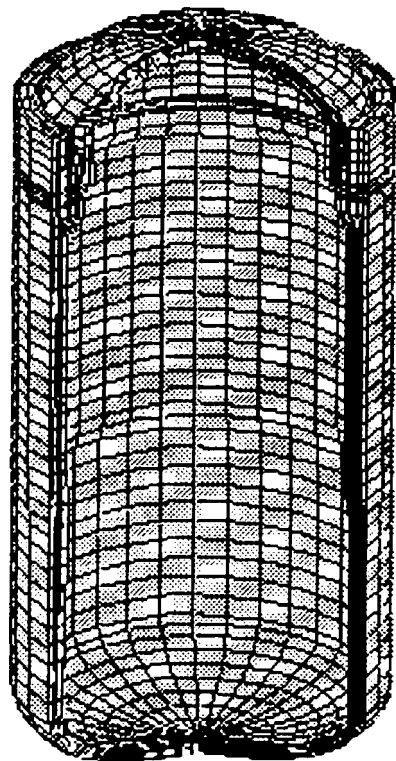
- local coordinate system



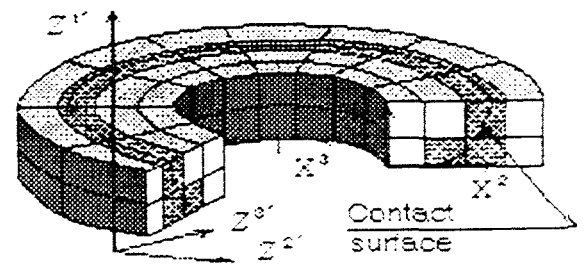
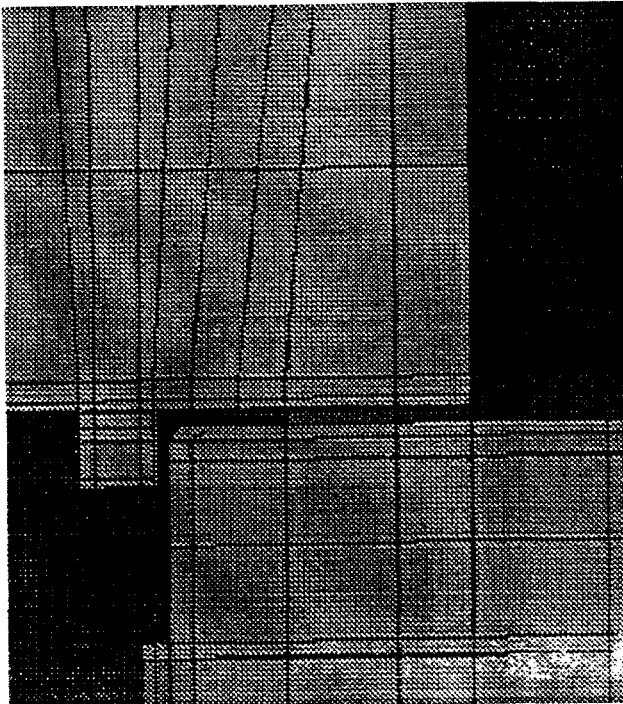
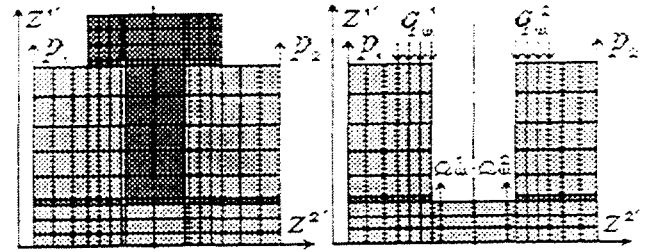
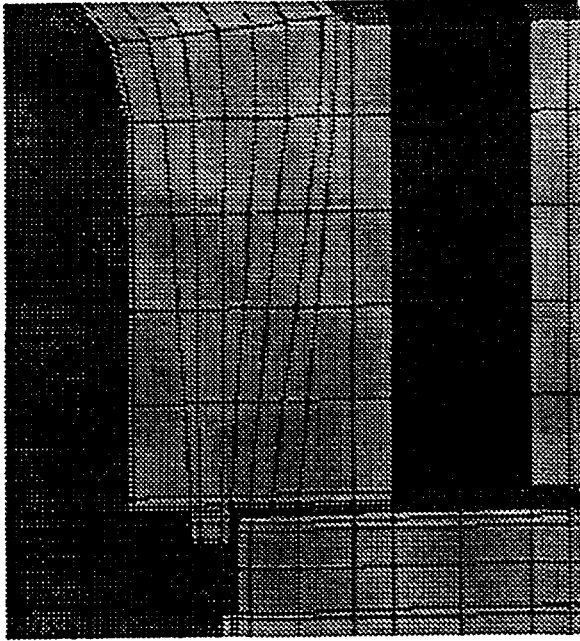
- global coordinate system



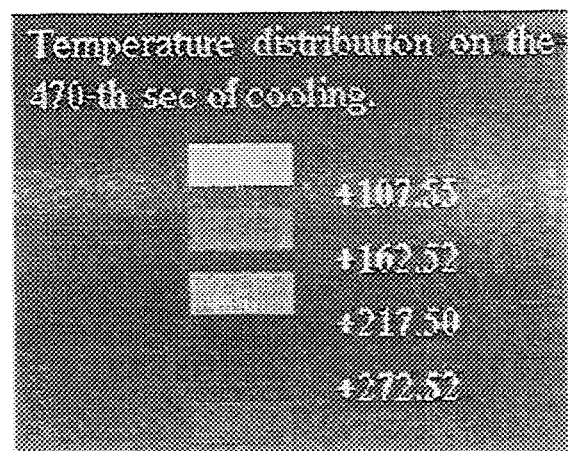
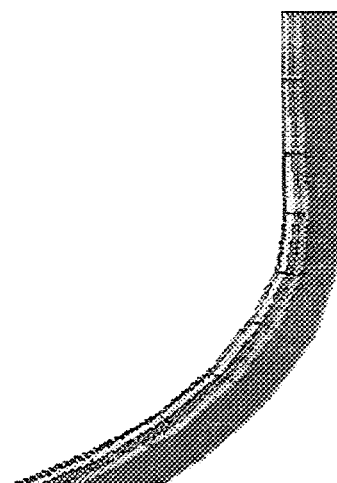
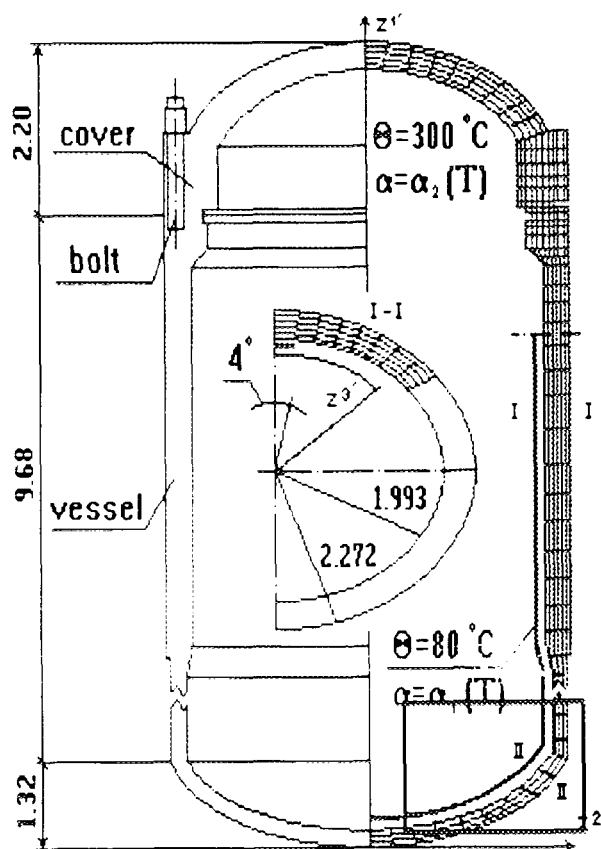
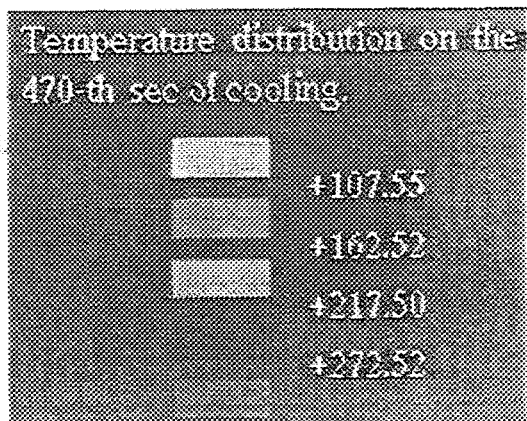
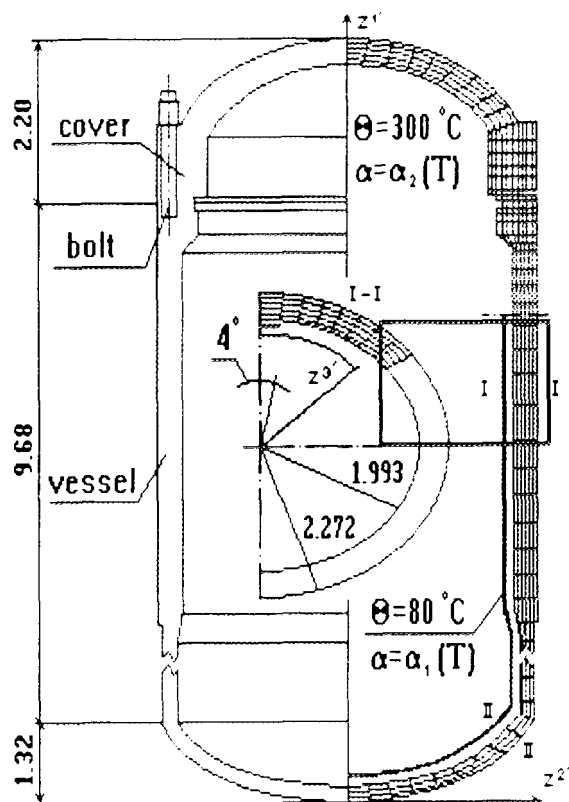
- cross sections for determination of displacements and stress components



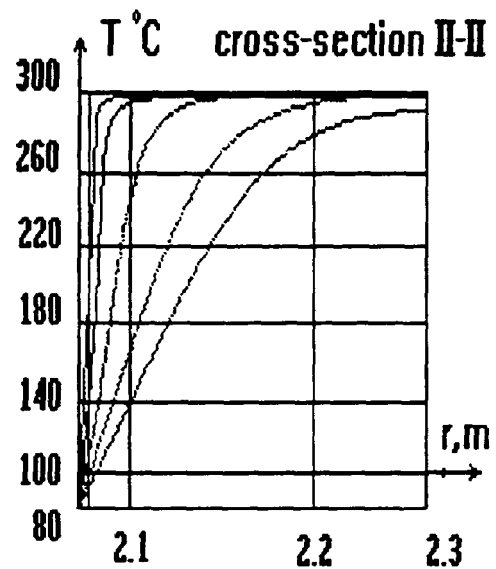
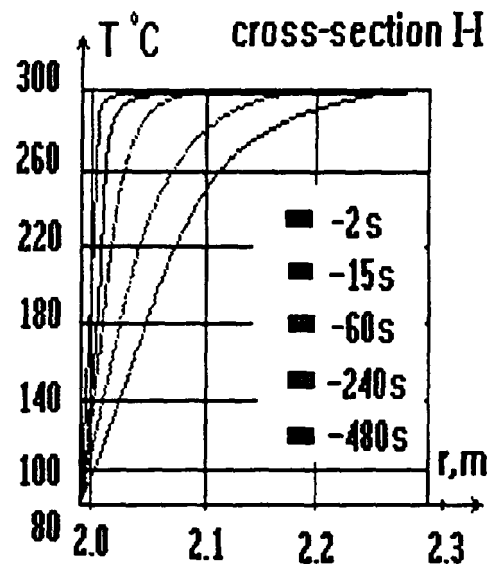
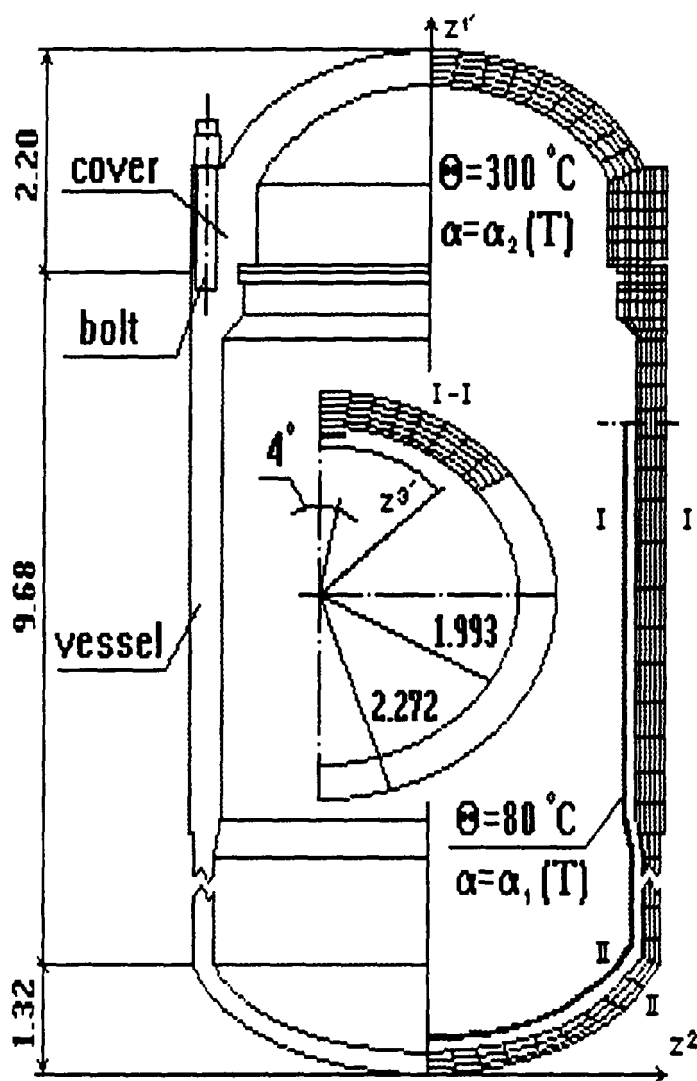
Finite Element model of RPV and a scheme of cooling



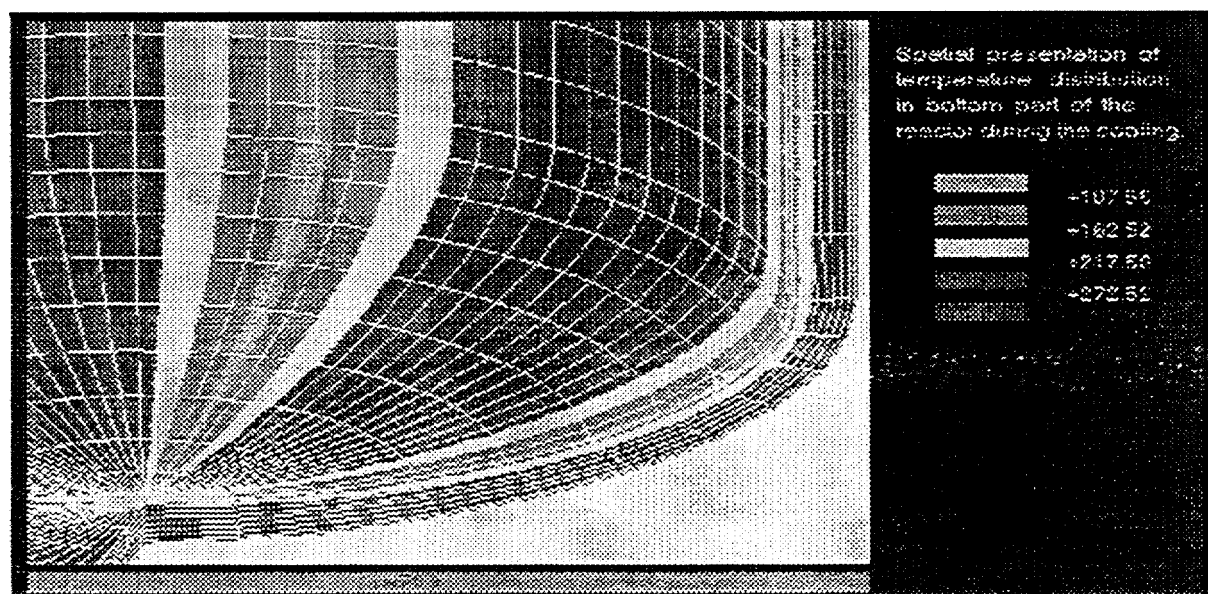
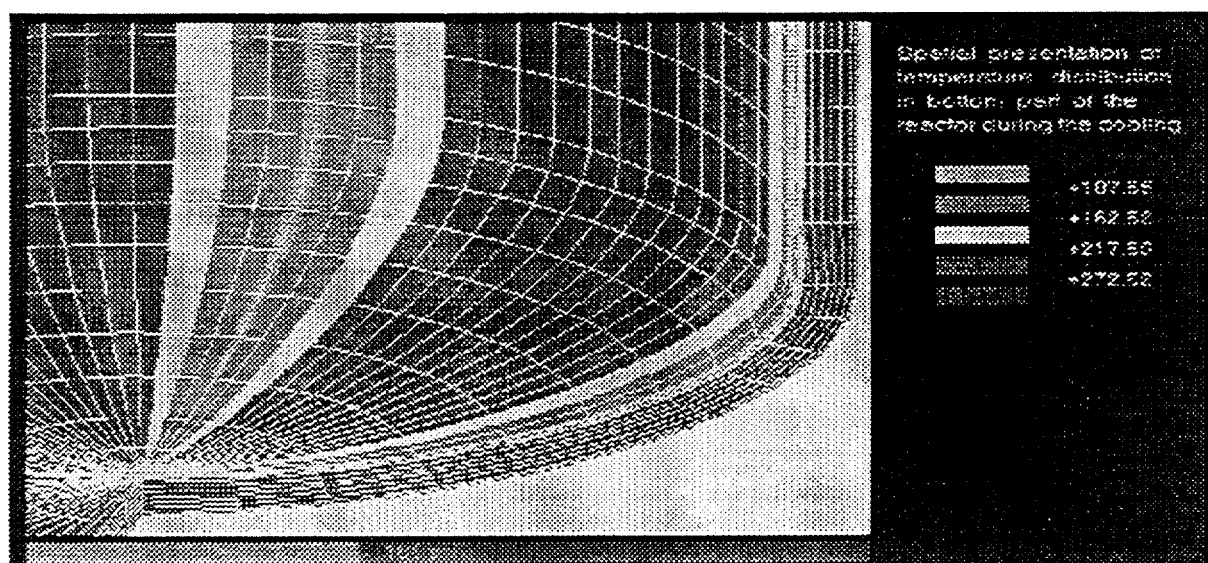
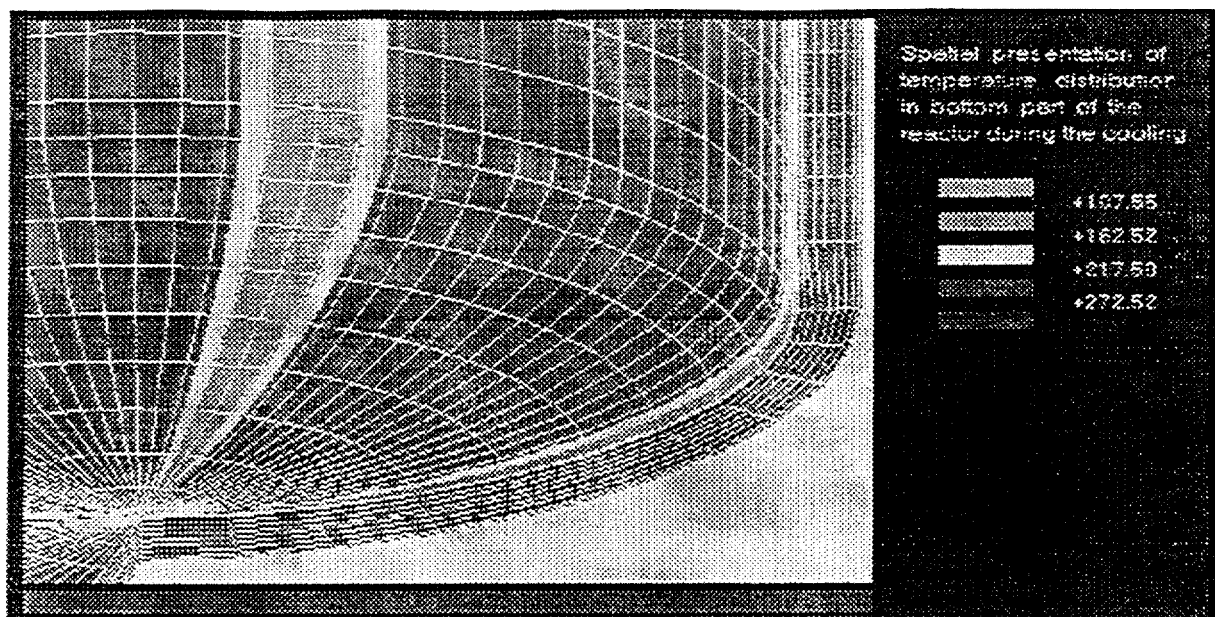
Simulation on main flanges area with inial stresses and contact interaction



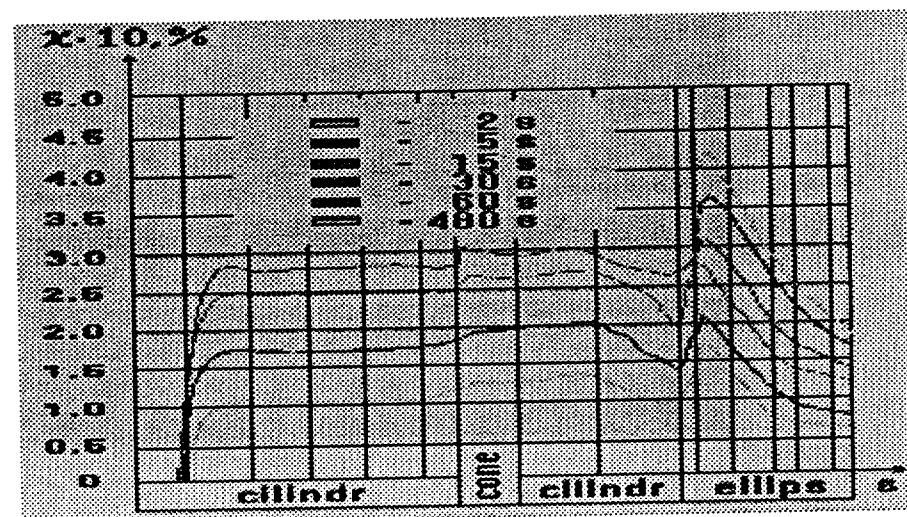
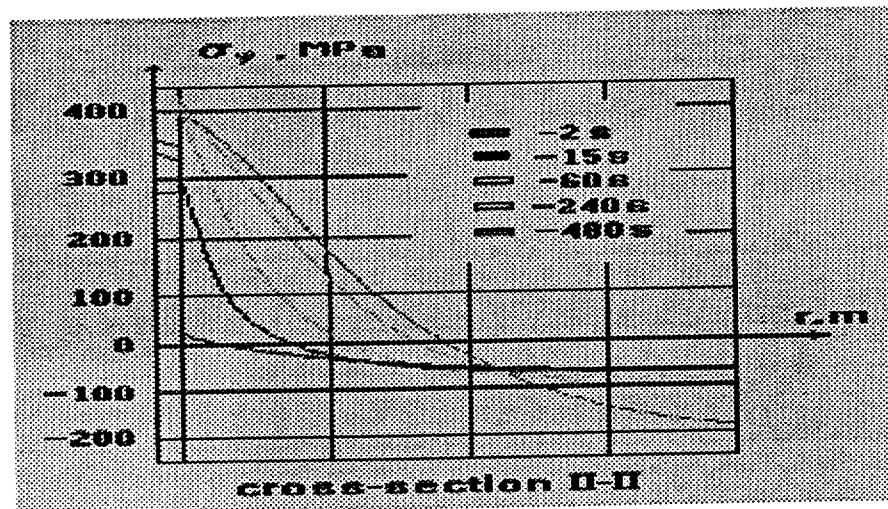
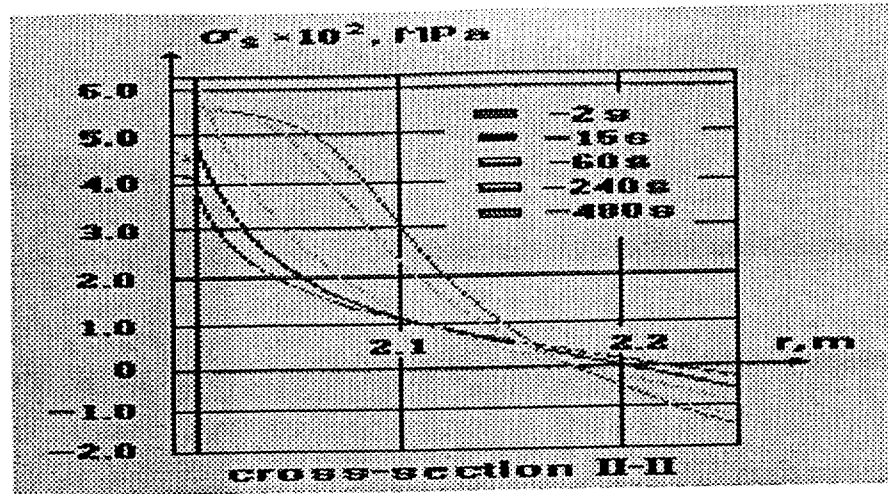
Cross-sections I-I and II-II and determination of temperature distribution along wall thickness



Temperature distribution along RPV wall thickness in various time moments



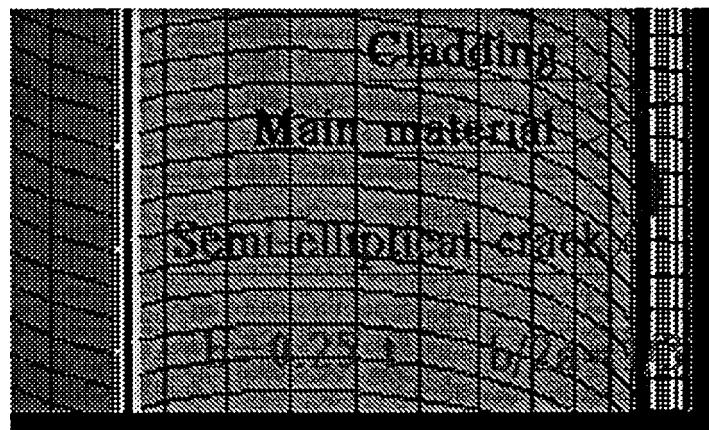
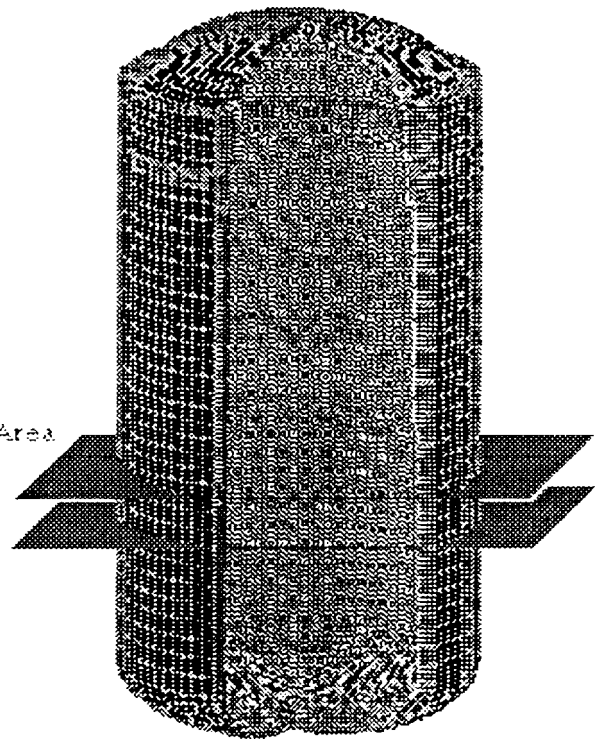
Increasing of cooled metal area for various time of cooling.
Solution of 3D transient heat transfer problem



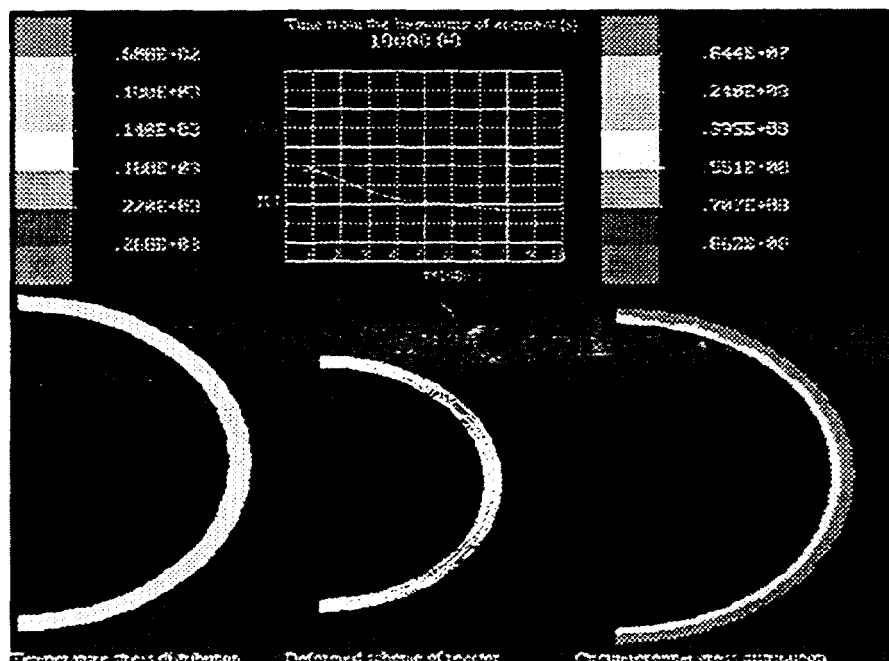
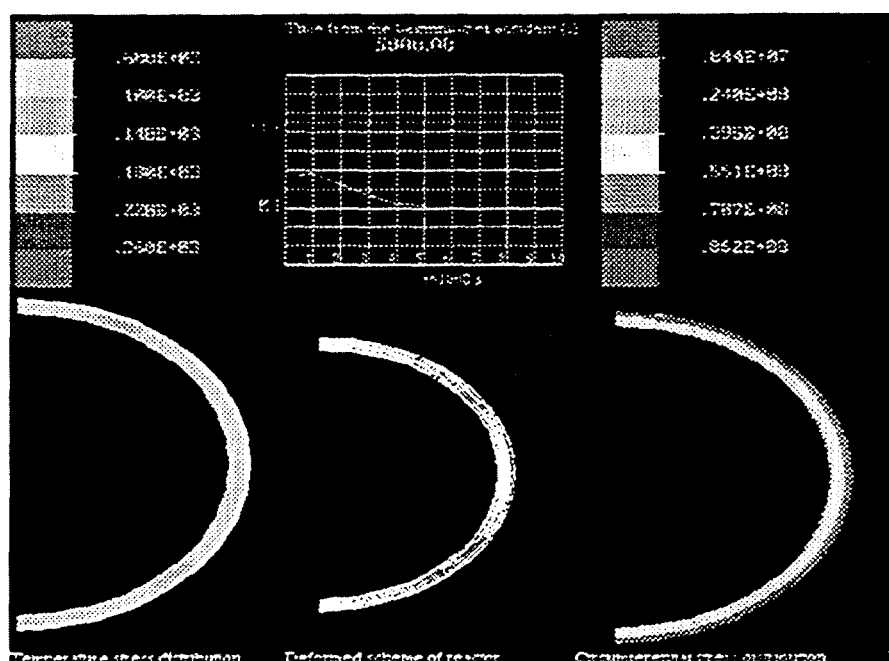
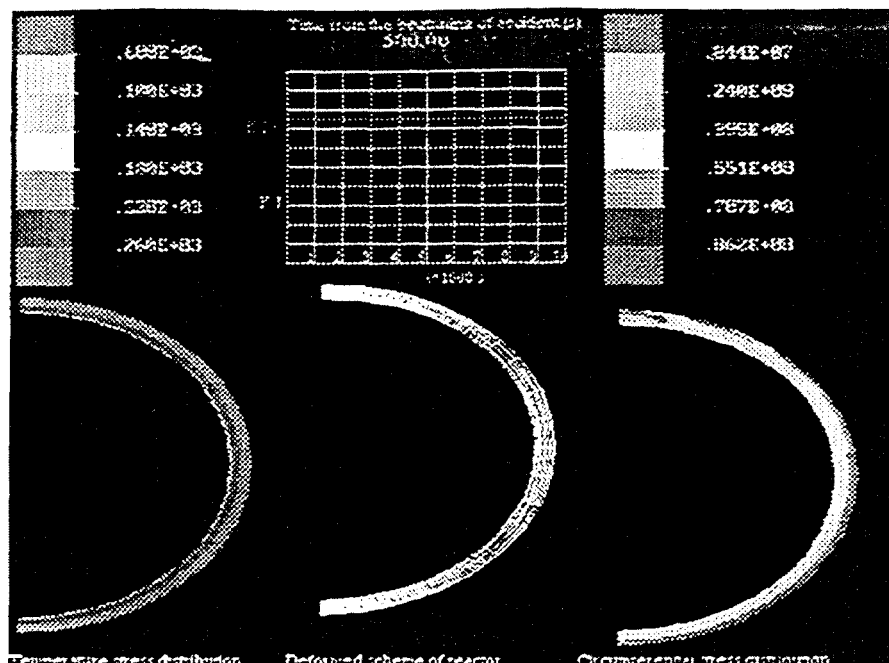
Stress and plasticity deformation distribution along time of cooling

Numerical Simulation of Accident
in Pressure Vessel of the 1-st Block
at Zaporozhje NPP 20.04.1995

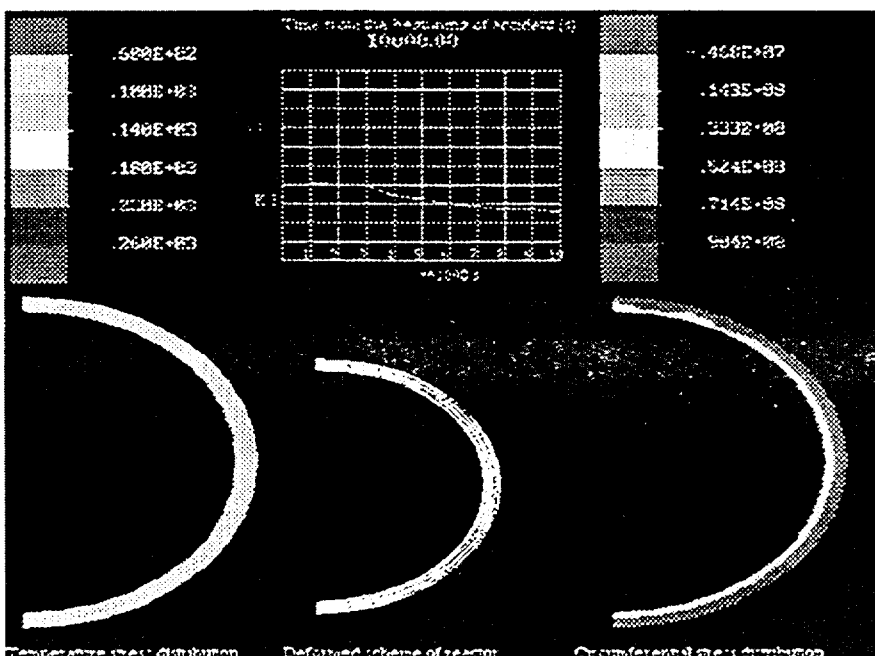
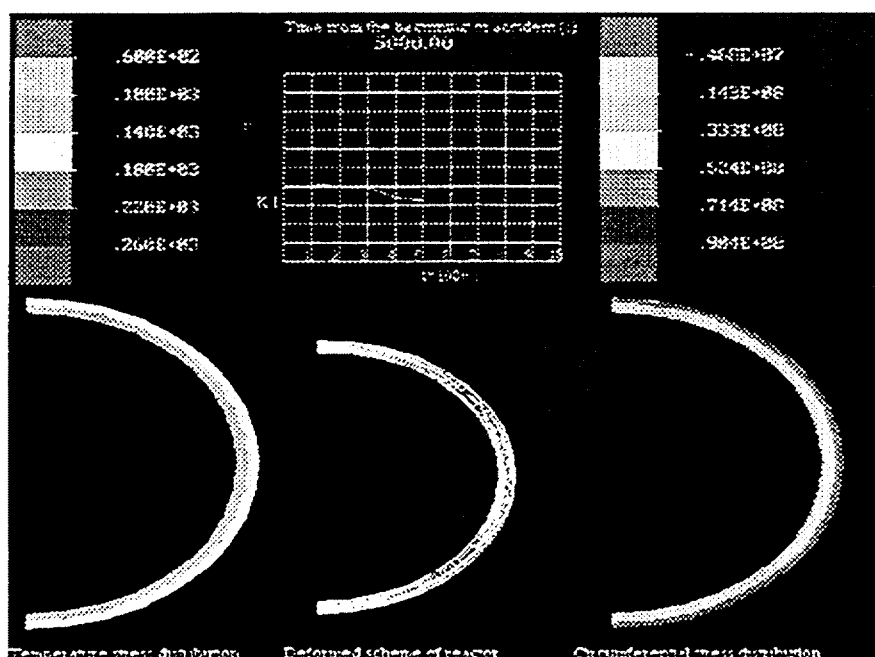
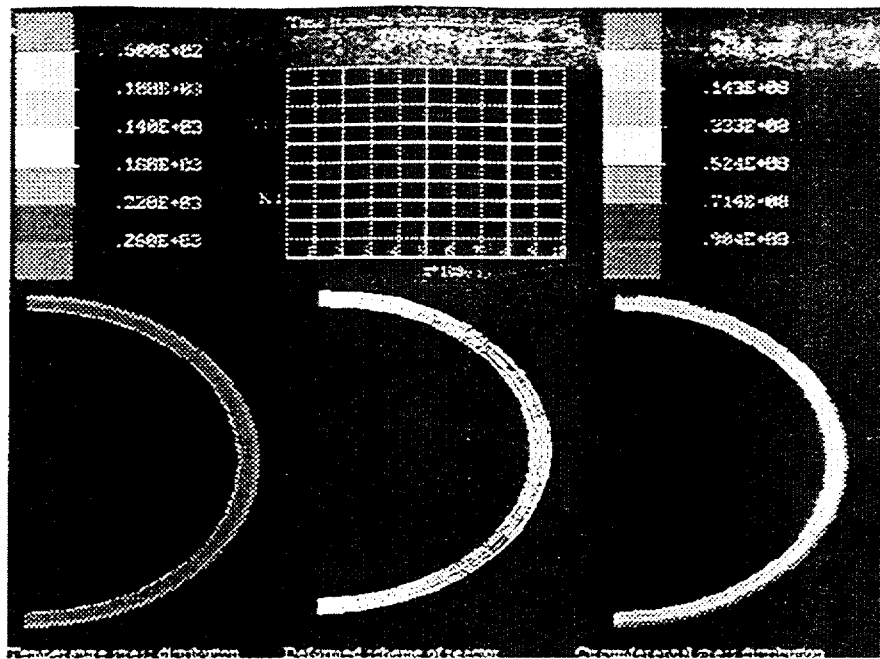
Investigated Area



Investigated area of RPV (near 4 th weld) and dimensions of postulated crack



First variant of cooling



Second variant of cooling

Session 9

7 MAY, WEDNESDAY

WWER reactor analysis 2.

Piminov, V. Dragunov, Y. Kostyrkin, S. Akbatchev, I.	Russia	Russian Practice of RPV Integrity Assessment under PTS Conditions
Hrázský, M. Mikula, M. Hermanský, P.	Slovakia	WWER 440 Integrity Assessment with Respect to PTS Events
Fekete, T.	Hungary	The Cladding Effect at PTS Assessment of WWER-440 RPV-s



Russian practice of RPV integrity assessment under PTS conditions

Prepared by: V.Piminov, Yu.Dragunov, S.Kostyrkin, I.Akbachev

**Specialists meeting on
Methodology for Pressurized Thermal Shock Evaluation**
Esztergom, Hungary

5-8 May 1997

INTRODUCTION

In this paper the approach used by Gidropress (main designer of Russian WWER reactors) for RPV integrity assessment is presented. Recently performed calculations for RPVs of Novoronezh NPP, units 3 and 4, are used as an example of practical application of this approach.

The calculations have been performed on the base of Russian regulatory requirements [1,2], at the same time the recommendations of IAEA Guidelines for PTS assessment [3] was also taken into account.

The scope of the work includes:

- Analysis of real state of NPP systems and PTS selection
- Analysis of material behavior including results of templets investigation
- Thermal hydraulic calculations
- Structural analyses for the leading transients
- Development of supplementary measures to reduce the risk of RPV fracture

The reactor pressure vessel drawing is presented in Fig. 1.

SELECTION OF THE LEADING SEQUENCES FOR THERMOHYDRAULIC ANALYSIS

The selection of leading sequences is performed on the basis of the list of design initial events which was developed for the WWER reactor plant safety analysis [4]. The experience of the other countries designing and operating PWR reactor plants and recommendations of IAEA given in the draft of Guide [3] are taken into account. The following groups of initial events should be extracted as potential unfavourable consequences for reactor vessel integrity:

1. Decrease in reactor coolant inventory (LOCA):

- Inadvertent opening of one pressurizer safety valve.
- Primary pipeline breaks:
- Rupture of instrumentation pipelines;
- Rupture of pipelines connected the primary side equipment units;
- Rupture of main coolant pipelines.
- Leaks from the primary to the secondary side of the SG:
- SG tube rupture;
- Primary collector leaks up to cover lift-up.

2. Increase in reactor coolant inventory:

- Inadvertent actuation of ECCS during power operation;
- Incorrect operation of make-up and blowdown primary system;
- Pressurizer level control system malfunctions.

3. Increase in heat removal by the secondary side:

- Inadvertent opening of one SG safety or relief valve or turbine bypass valve;
- Spectrum of steam system piping break inside and outside of containment;
- Rupture of feedwater pipeline;
- Feedwater system malfunctions that decrease feedwater temperature;
- Feedwater system malfunctions that increase feedwater flow rate;
- Secondary pressure regulator malfunctions that increase steam flow rate.

4. Accidents with the RPV cooling from outside:

- break of the biological shield tank.

The compilation of the list of initial events for each reference NPP is usually carried out using the engineering judgement and taking into account design features and performed modifications.

Proceed from the above mentioned list of initial events and taking into account real status of both plants the following accidents are considered for Novoronezh NPP, units 3 and 4:

1. Cold leg leak Du 32
2. Cold leg leak Du 20 (compensated break)
3. Hot leg leak Du 20 with later isolation of damage section
4. Inadvertent opening of one pressurizer safety valve
5. Inadvertent opening of one pressurizer safety valve with reclosure
6. Primary-to-secondary leaks Du 38 and 108
7. Primary blowdown collector break (Du 64 from lower plenum)
8. One SG steam line break
9. One SG steam line break with stuck open pressurizer safety valve
10. Inadvertent opening of one SG safety valve
11. Inadvertent opening of one BRU-A

The feature of these units is that HPIS pumps are connected into two hot legs.

The accident with the RPV cooling from outside is not considered, because of the construction of the biological shield tank is designed so that in case of seal failure water from the biological shield tank do not hit on the reactor vessel outside surface.

All accidents are considered for real status of both nuclear plant units taking into account available protections and interlockings. Also possibility of loss of off-site power and low residual heat level are taken into account in the most conservative way.

The examination of calculational results shows that the following consequences are the most important from the point of view of RPV integrity:

1.	One SG steam line break.
2.	Isolable primary leaks
3.	Inadvertent opening of pressurizer safety valve with closure at any time

Such accidents are characterised by high primary pressure due to HPIS pumps operation and low coolant temperature in downcomer.

Accident with steam line break is characterised by cold plum generation due to great heat removal in damaged SG (see Fig. 2). Isolation valve on steam line from emergency SG starts to close at 50 s after DG connection to the reliable power supply section (closing time is equal to 140 s). Two HPIS pumps start to operate at 177 s.

Reclosure of PRZ SV (flow rate capacity 10,5 kg/s) is considered in 1 hour after beginning of accident (see Fig. 3). Two HPIS pumps start to operate at 120 s.

Coolant temperatures at downcomer for different primary leaks are presented in Fig. 4.

DETERMINATION OF STRESSES IN THE VESSEL WALL

Stresses in the vessel wall in the region of grinding-out are determined with 3-D model by FEM. The computer code TACT was used for stress calculations. The isoparametrical curvilinear finite elements are used with 20 nodes and quadratic interpolating functions. Discrete model comprising 2810 finite elements is presented in Fig. 5. With regard for symmetry the 1/2 part of the reactor is simulated. Single grinding-outs in welds No 4 and No 5 are considered.

In the calculation the residual stresses in the welds were also taken into account. Distribution of residual stresses through the weld thickness is assumed in the form of:

$$\sigma_z = \sigma_\theta = 60 \cos(2\pi x/S)$$

where σ_z , σ_θ - axial and circumferential stresses, x - thickness coordinate, S - vessel wall thickness.

PROCEDURE FOR EVALUATION OF THE ALLOWABLE CRITICAL BRITTLE FRACTURE TEMPERATURE

Evaluation of brittle fracture resistance of the RPV at the design stage is performed in accordance with the former Soviet Union "Standards for Strength Evaluation of Components and Piping of Nuclear Power Plants", PNAE G-7-002-86 [1]. The same approach is usually used for RPV residual lifetime evaluation for units under operation.

The evaluation is performed on the basis of the linear elastic fracture mechanics. The main characteristics of the materials used in the calculation are static fracture toughness, K_{IC} , and the critical brittle fracture temperature T_k as function of operational history (with respect to the material degradation). Change in material properties in the course of operation is taken into account by means of introducing the shifts of initial critical brittle fracture temperature T_k due to different operational effects (radiation embrittlement, thermal ageing, fatigue damage) in the calculation.

RPV resistance to brittle fracture during a particular plant state is considered to be ensured if, for all defect sizes up to the postulated quarter wall thickness size defect, the following condition is met:

$$K_I < [K_I]_i$$

where K_I is the intensity factor and $[K_I]_i$ is the allowable value of stress intensity factor for the plant state considered, i.e.:

- $i = 1$ for normal operating conditions,
- $i = 2$ for operational occurrences and hydraulic tests,
- $i = 3$ for accident conditions.

Statistically evaluated lower envelope of all available experimental data is taken as the K_{IC} temperature dependence. Allowable stress intensity factors $[K_I]_i$ are obtained from the K_{IC} by applying safety factors:

- for normal operating conditions
 $n_k = 2$, $\Delta T = 30^\circ C$,
- for operational occurrences and hydraulic tests
 $n_k = 1.5$, $\Delta T = 30^\circ C$,
- for accident conditions
 $n_k = 1.0$, $\Delta T = 0^\circ C$.

The n_k is a safety factor with respect to fracture toughness values and ΔT is a safety factor with respect to calculated crack tip temperature. The allowable stress intensity curve is obtained as a lower envelope of two curves, the first of which is obtained by dividing the K_{IC} by n_k and the other one by a horizontal shift of the initial curve by ΔT . The recommended temperature dependencies of $[K_I]_i$ for different RPV materials are given in the applicable standard [1].

Surface semi-elliptical cracks are postulated and with depth up to $a=0.25 S$ (where S is the vessel wall thickness) and with aspect ratio $a/c = 2/3$. Stress intensity factor K_I is determined using a formula given in the Standard [5], which takes into account real distribution of stresses in the defect depth. Mechanical as well as thermal and residual stress components are taken into account.

Comparing calculated loading path in terms of K_I values of the whole set of postulated defects with temperature dependencies of allowable values of stress intensity factors $[K_I]_i$, a maximum allowable critical brittle fracture temperature T_{ka}' for the analysed PTS sequence is obtained. The lowest of these temperatures for the whole set of analysed PTS sequences is taken as the maximum allowable critical brittle fracture temperature T_{ka} .

This temperature is then compared with the critical brittle fracture temperature T_k of the analyzed vessel. Based on this assessment, decisions on further operation, annealing, etc, could be made.

PROCEDURE FOR EVALUATION OF ALLOWABLE DEFECT SIZE

Evaluation of allowable defect size is performed in accordance with the procedure "Method for evaluation of allowability of defects in materials of components and pipings in NPPs during operation", M-02-91 [2].

This procedure is in principle divided into three main parts.

1. Defects, found during ISI are schematised using conservative approach, i.e. equivalent defect diameter obtained from ultrasonic tests is transformed into fatigue like crack with the same surface area but with semiaxis ratio a/c equal to 0.5 for internal defects (subsurface) and to 0.4 for surface defects, respectively. Detailed rules and formulas for evaluation of closely spaced defects or their groups are also given. All defects are defined as located in the plane perpendicular to the RPV surface as well as to the principal stresses.

2. Possible growth of the given defect due to operational loading is calculated. The standard provides the coefficients of Paris law for RPV materials. This calculated defect growth for the whole remaining lifetime is added to the initially schematised defect sizes

3. Calculation of defect allowability is then performed using a complex approach, including linear elastic fracture mechanics, elastic-plastic fracture mechanics as well as theory of plasticity.

The following safety factors for different operational conditions are used:

- for normal operating conditions
 $n_k = 3$, $\Delta T = 30^\circ\text{C}$,
- for operational occurrences and hydraulic tests
 $n_k = 2$, $\Delta T = 20^\circ\text{C}$,
- for accident conditions
 $n_k = 1.4$, $\Delta T = 10^\circ\text{C}$.

Values of K_I are calculated both for the crack deepest point as well as for the intersection with the surface for a surface defect or the closest point to the inner surface for an

internal defects. These values are then compared with allowable values of fracture toughness derived from static fracture toughness K_{IC} used in the standard [1].

The following procedure based on requirements of the Standard M-02-91 [3] was used for evaluation of allowable defect size in RPVs of units 3 and 4 of Novovoronezh NPP.

The spectrum of postulated defects of different sizes of the most hazardous type in the most stressed zone of the weld is selected. In the present calculation such defects are considered to be the surface semi-elliptical cracks of different depths with ratio of semi-axes being $a/c=0,4$ [2], located in the pole of grinding-out (the zone with maximum stress concentration).

For each calculated crack the temperature boundaries of the zones of brittle, quasi-brittle and plastic mechanisms of failure are determined as per the criteria of [1]. Hereat as the temperature dependence $K_{IC}=f(T-T_K)$ the curve $[K_I]_3$ is used for the welds of steel 15Kh2MFA from [1].

For each calculated time moment of the considered conditions the stresses in the region of calculated cracks are determined and, using methods of [4] the values of K_I are calculated for two points of the crack front: the point at maximum depth and the point in the region of the crack front outgoing to the free surface. Hereat the crack is located in the plane perpendicular to the direction of action of maximum tensile stresses.

For the crack front points indicated above by metal temperature the character of failure is determined (brittle, quasi-brittle or plastic) and the permissible value of $[K_I]$ with regard for the character of failure and margins for the conditions of the category considered (NOC, OO, AS). The permissible value of $[K_I]$ is determined by formula presented in [2].

By means of comparison of K_I and $[K_I]$ values for all time moments of all considered conditions the maximum depth of the permissible crack $[a]_c$ is determined from the condition of $K_I = [K_I]$.

By means of calculation of the crack kinetics [2] the depth of the initial crack $[a]$ is determined, which may extend to the size of $[a]_c$ due to cyclic loads for four years of operation (time interval between ISI).

On the basis of recommendations of [5] on schematisation of defects, detected in the course of in-service inspection, the minimum areas $[F]$ of surface defects and subsurface (located close to the surface) defect are determined which shall be schematised by the surface semi-elliptical crack of $a=[a]$ depth and ratio of semi-axes $a/c=0,4$.

Subsurface defects of larger depth of occurrence in accordance with [5] are schematised by subsurface elliptical cracks. As the values of K_I for the subsurface cracks with the same field of stresses is considerably lower than for the surface cracks, and also taking into account the fact that stresses under emergency cooldowns drop quickly with increasing the distance from the inner surface into the depth of thickness, the permissible area of such defects will be considerably larger than the permissible area of surface and subsurface defects determined above.

RESULTS OF CALCULATIONS FOR THE LEADING TRANSIENT «STEAM GENERATOR STEAMLINE BREAK»

Results of calculation T_{ka} using the procedure from PNAE G-7-002-86 [1] for welds No 4 and 5 are presented in Table 1. Figs 6-7 give the results of calculation K_I , the position of the curve of static fracture toughness K_{IC} is also shown there at $T_k = T_{ka}$.

Table 1

Results of calculation T_{ka} using the procedure PNAE G-7-002-86
for the conditions «Break of steam generator steamline»

Section	Residual stresses	T_{ka} for weld No 5 °C	T_{ka} for weld No 4 °C
grinding-out	60 MPa	163	176
grinding-out	no	-	183
without grinding-out	60 MPa	169	182
without grinding-out	no	-	190

Permissible sizes of defects in weld No 4 are determined depending on T_k with regard for cyclic extension for the preceding 4 fuel cycles (interval between non-destructive examination of the weld metal). The calculations were performed both with the use of safety factor $n_k=1.4$, $\Delta T=10^\circ\text{C}$ [2], and without these factors. In the latter case for obtaining the permissible defect the safety factor 2 should be applied for sizes of the calculated defect. In accordance with IAEA recommendations the least of two defects, obtained in such a way, shall be assumed. It follows from the results obtained that application of safety factors of $n_k=1.4$, $\Delta T=10^\circ\text{C}$ is, in the given case, more conservative.

Analysis of sensitivity of the results to different factors has also been performed. The specific care was paid to effect of aspect ratio of the crack. It follows from the results of calculations that if the requirements for sensitivity of ultrasonic inspection system are worded in terms of equivalent area of defect (in accordance with the Russian standards), then the use of relation of semi-axes of the postulated crack $a/c=0.4$ (in accordance with [2]) is more conservative, than that assumed in Western countries $a/c=0.3$, because it results in somewhat less equivalent area of the permissible defect.

RESULTS FOR ISOLABLE PRIMARY LEAKS

As it follows from the analysis of results of thermohydraulic calculations in case of leak isolation the rapid primary pressure increase takes place to the value of the head of the emergency makeup pumps. At the same time with the pressure increase the gradual rise of coolant temperature in the reactor pressure chamber begins due to loss of cold water supply from the emergency makeup pumps.

The calculations for these conditions were made in the form of determination of permissible pressure in the reactor vessel depending on the time of the process and depending on the coolant temperature in the reactor pressure chamber that allows to determine the limiting conditions of the mode which could be laid down in the basis of elaboration of the instructions for the operator or an algorithm of operation of automatic system of the reactor vessel protection against cold overpressure.

The lower envelope coolant temperature curve for all considered transients was used for this calculation (presented in Fig. 4).

Calculation of the permissible pressure was performed both as per the criteria of PNAE G-7-002-86 [1], and with the use of methods M-02-91 [2]. In the latter case the calculation was made for the postulated crack of 5mm depth with the use of safety factors of $n_k=1.4$; $\Delta T=10^\circ\text{C}$ that is more conservative than using the postulated crack of 10mm depth with the following using of safety factor 2 for the size of reliably detected defect.

Results of the calculation are given in the form of graphs in Fig 8.

Specific attention was paid to the transient «Inadvertent opening of Prz safety valve». According to IAEA recommendation a possibility of safety valve closing at any time moment of the accident was considered. Results of the calculation for this transient are given in the form of graphs in Fig 9 .

It follows from the results presented in Figs 8-9 that the repeat pressure increase in the reactor vessel, cause by leak isolation, could result in severe situation from the viewpoint of reactor vessel brittle strength at reaching the high values of T_k of weld No 4 material if closing of the valve takes place at low temperature of the primary coolant. Special measures are to be taken on prevention of the reactor vessel cold overpressure. Such measures may be operator's interference or introduction of automatic system of the reactor protection against cold overpressure.

Operator could make an attempt to isolate the leak without the risk of hazardous overpressure of the reactor vessel only till the primary coolant temperature goes down the definite value depending on the current ductile-to-brittle transition temperature of the reactor vessel material.

The relevant requirements have been included into instructions for operator.

MAIN CONCLUSIONS OF ANALYSES PERFORMED

- The conditions "Break of steam generator steamline" is the leading accident governing the reactor vessel service lifetime. Permissible ductile-to-brittle transition temperature of weld No 4 material T_{ka} , determined for the worst conditions (with regard for stress concentration in the region of grinding-outs after taking templets, with regard for residual stresses in the weld) is $T_{ka}=176^{\circ}\text{C}$ for such mode.

- The primary leaks, in case of their isolation (by operator's actions or due to spontaneous closing of Prz safety valve after its failure to fit) need special care. The repeat increase of the reactor vessel pressure, caused by leak isolation, could result in severe situation from the viewpoint of the reactor vessel brittle strength at reaching the high values of T_k of weld No 4 material, if isolation of leak is performed at low temperature of the primary coolant. The special measures are to be taken on prevention of cold overpressure of the reactor vessel.

- To provide for safe operation of the reactor vessels during the design service life it is necessary to apply the system of non-destructive examination of the reactor vessel that allows to detect reliably the small crack-type surface and subsurface defects in the vessel cylindrical part (including circumferential welds).

It should be noted that the same approach was used by Gidropress for integrity assessment of Kozloduy NPP, unit 1, under leading PTS conditions. Independent calculations had been performed for this RPV by Siemens and Westinghouse using Western approaches. The final results obtained by all three companies (in terms of allowable transition temperature T_{ka}) were very close. The T_{ka} values were obtained within the range of 173-180°C.

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REACTOR PRESSURE VESSEL (B-179)

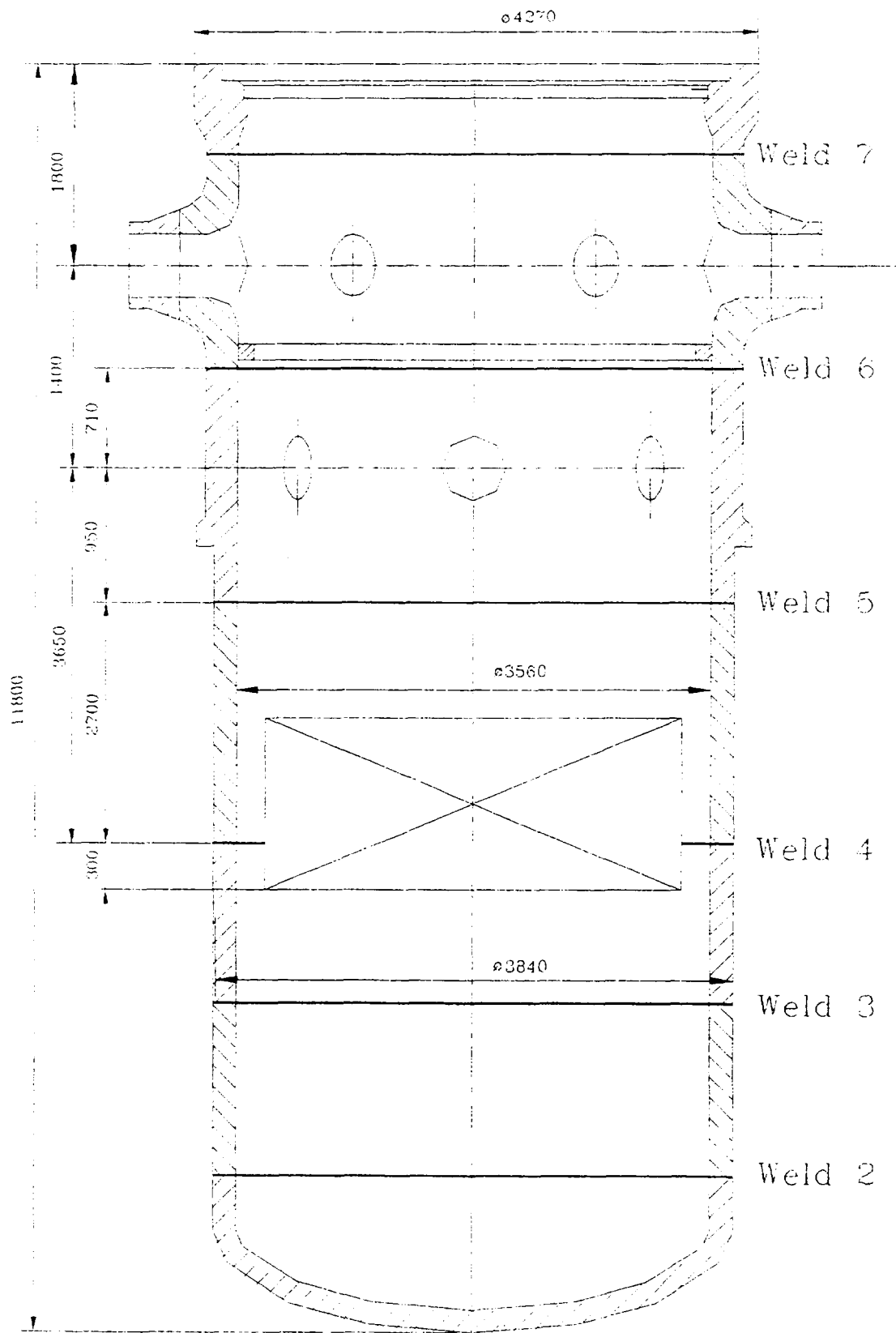


Fig 1.

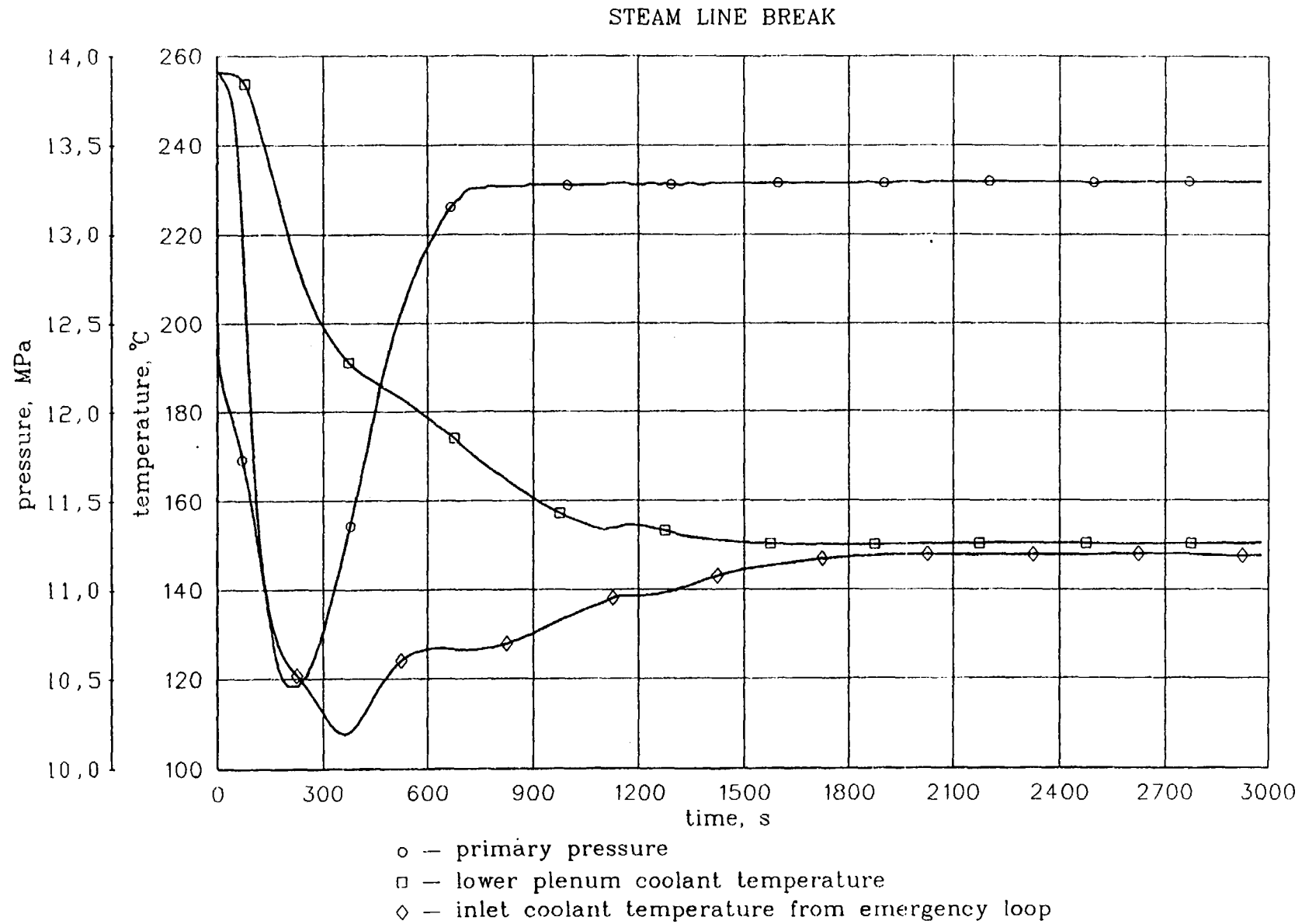


Fig 2

INADVERTENT OPENING OF PRZ SV WITH RECLOSURE

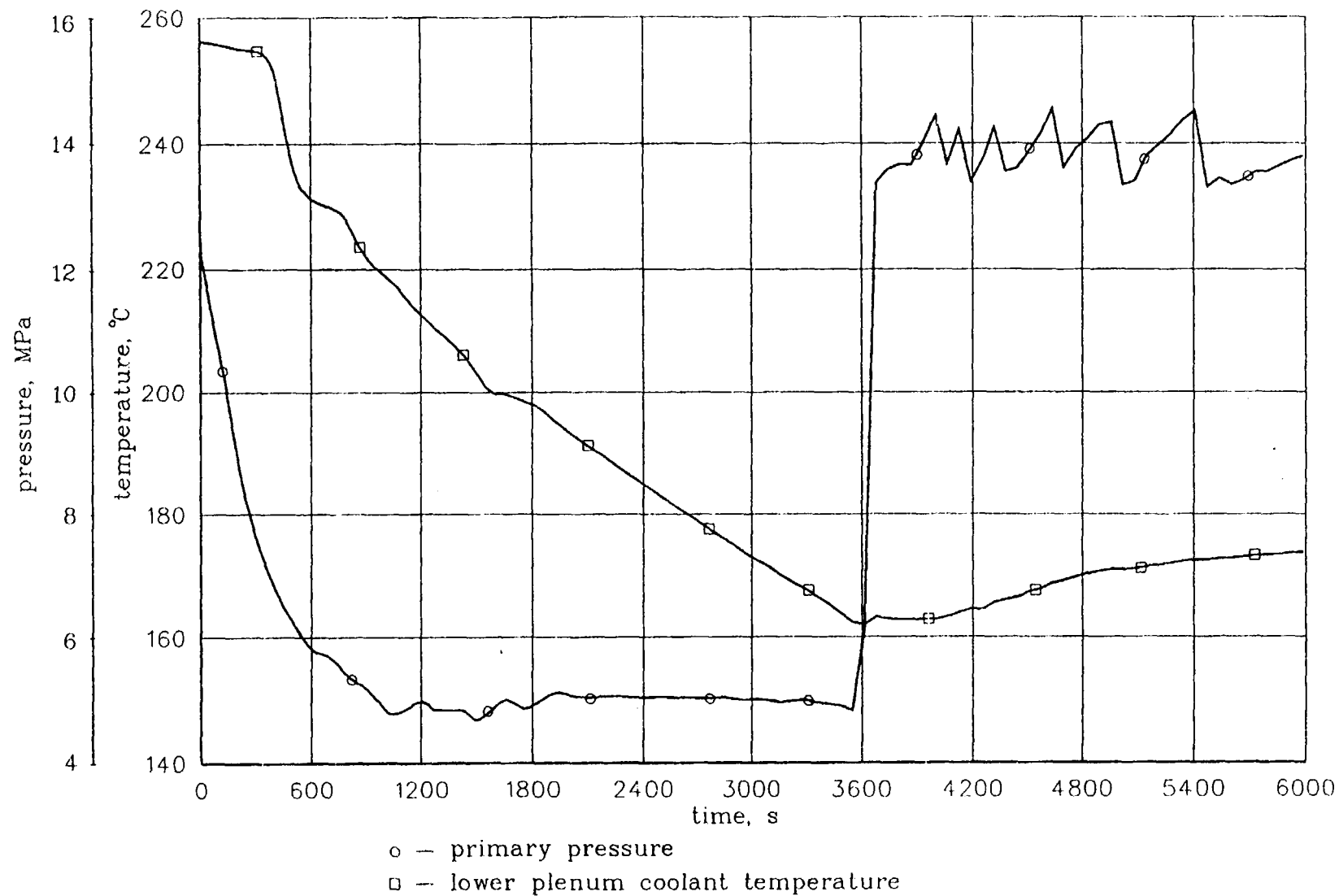


Fig 3

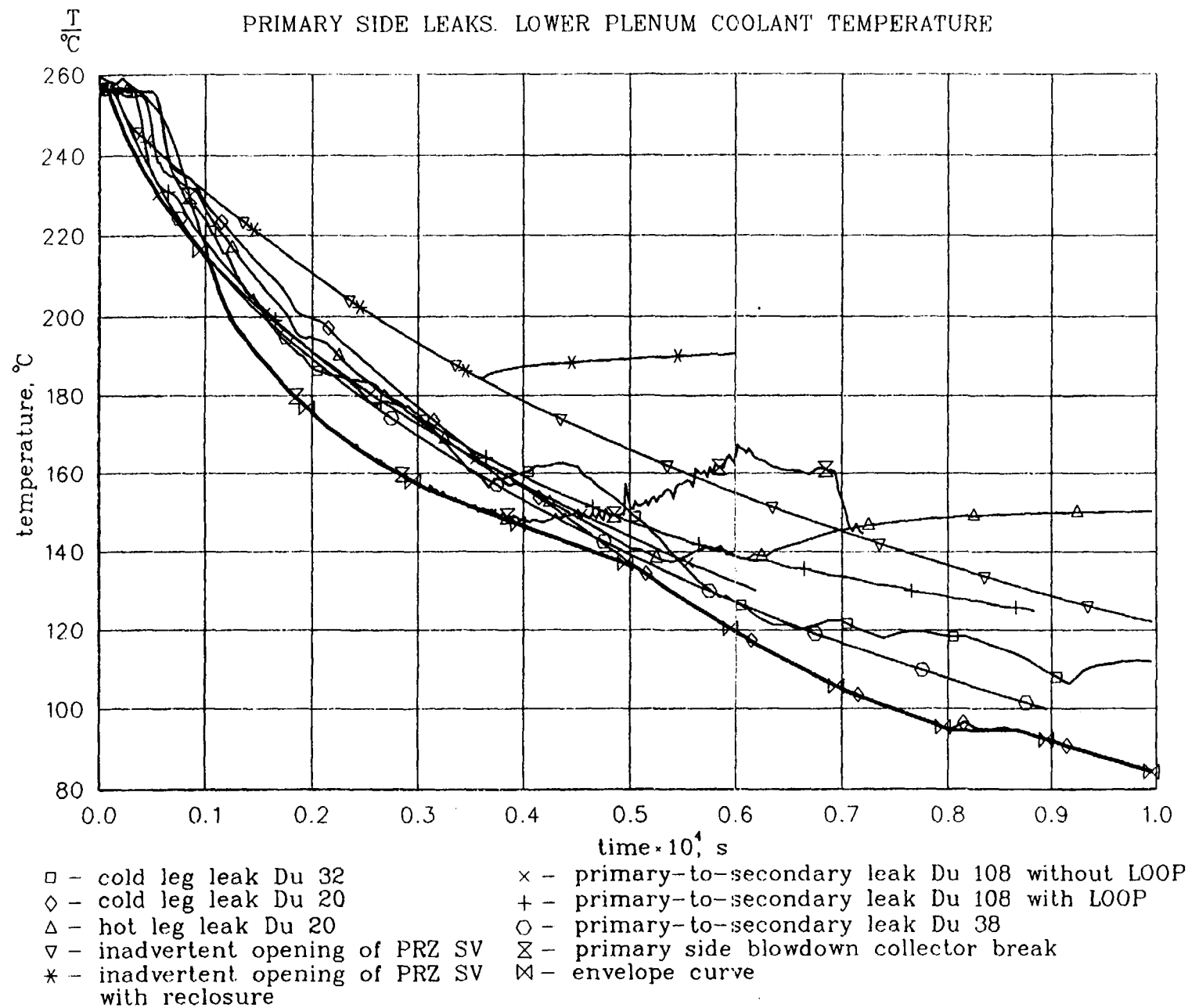


Fig 4

RPV of Novovoronezh 3, 4

Finite element mesh

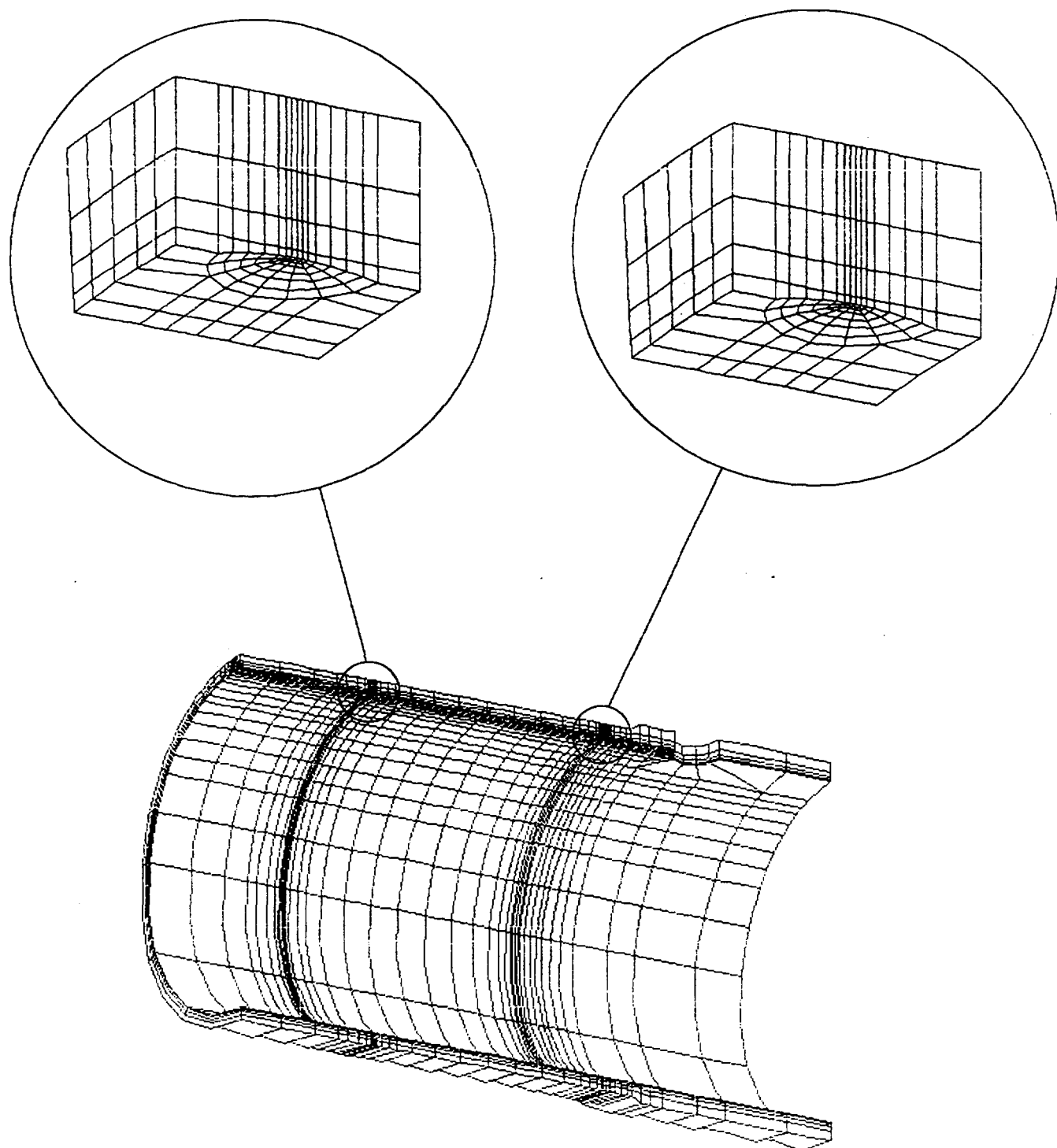


Fig 5

Stem generator steamline break.
Weld 4. Gringing section. Residual stress 60MPa.
Axial surface cracks with $a/c=2/3$.
Deepest point of crack front

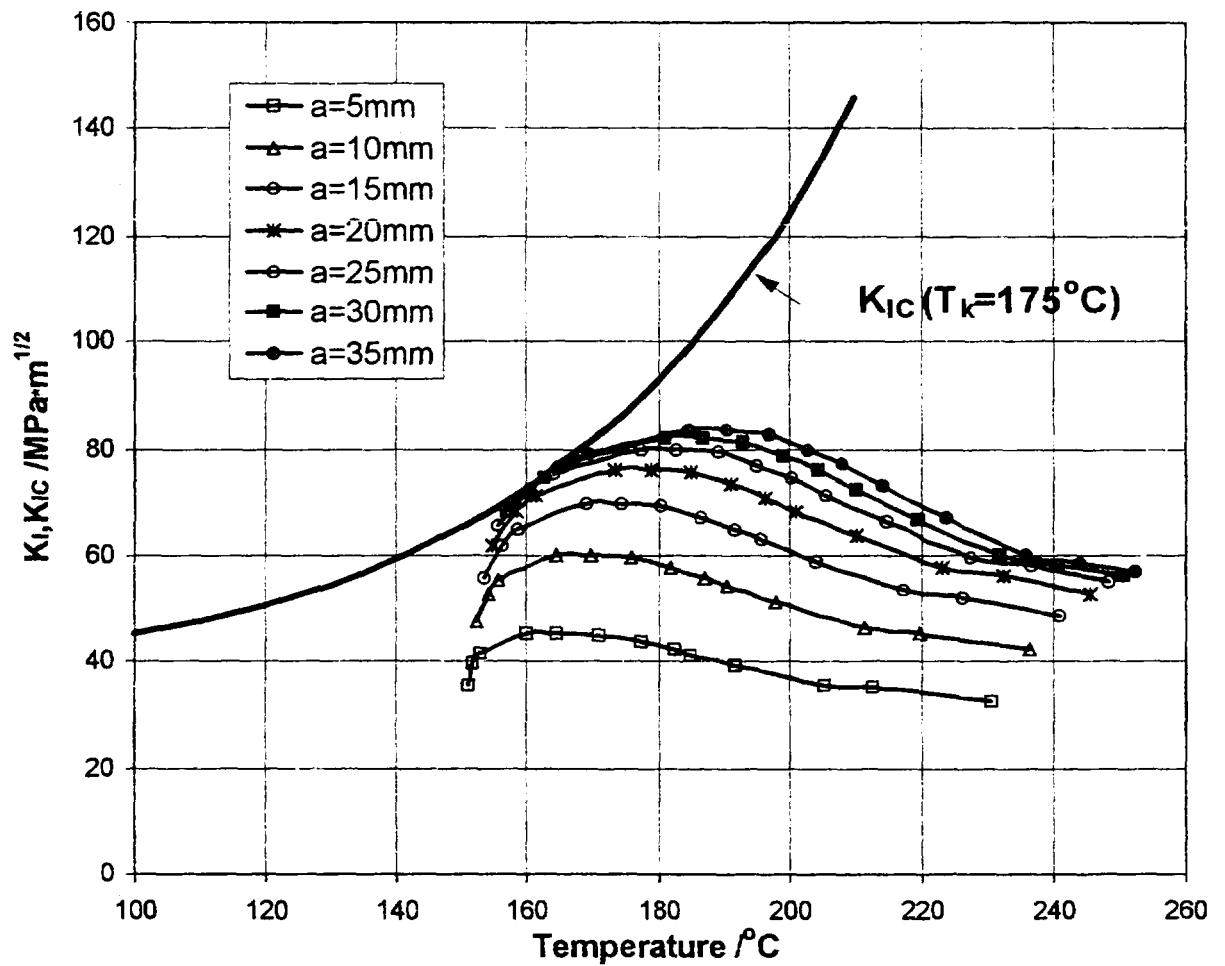


Fig. 6

Stem generator steamline break.
 Weld 5. Gringing section. Residual stress 60MPa.
 Axial surface cracks with $a/c=2/3$.
 Deepest point of crack front

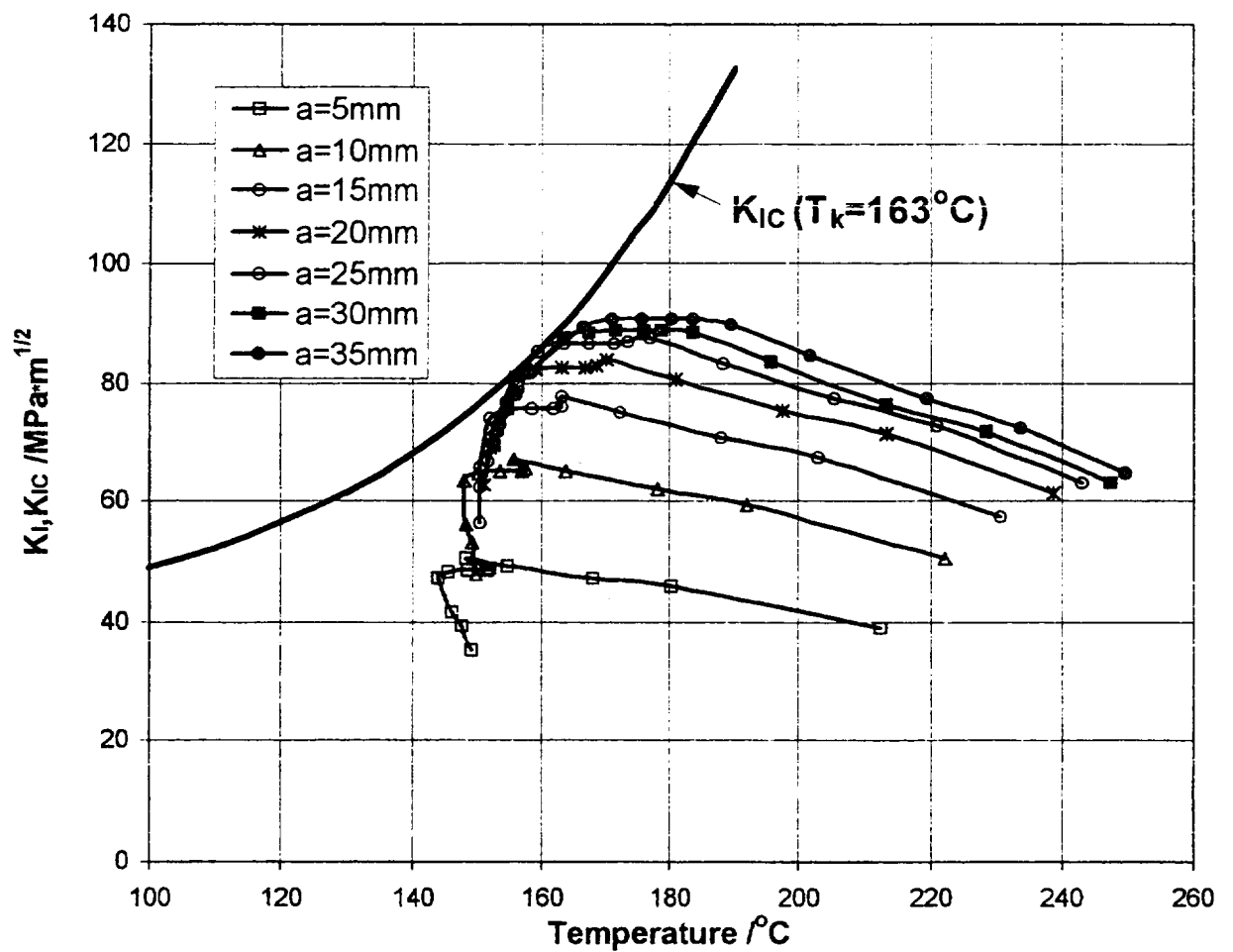


Fig. 7

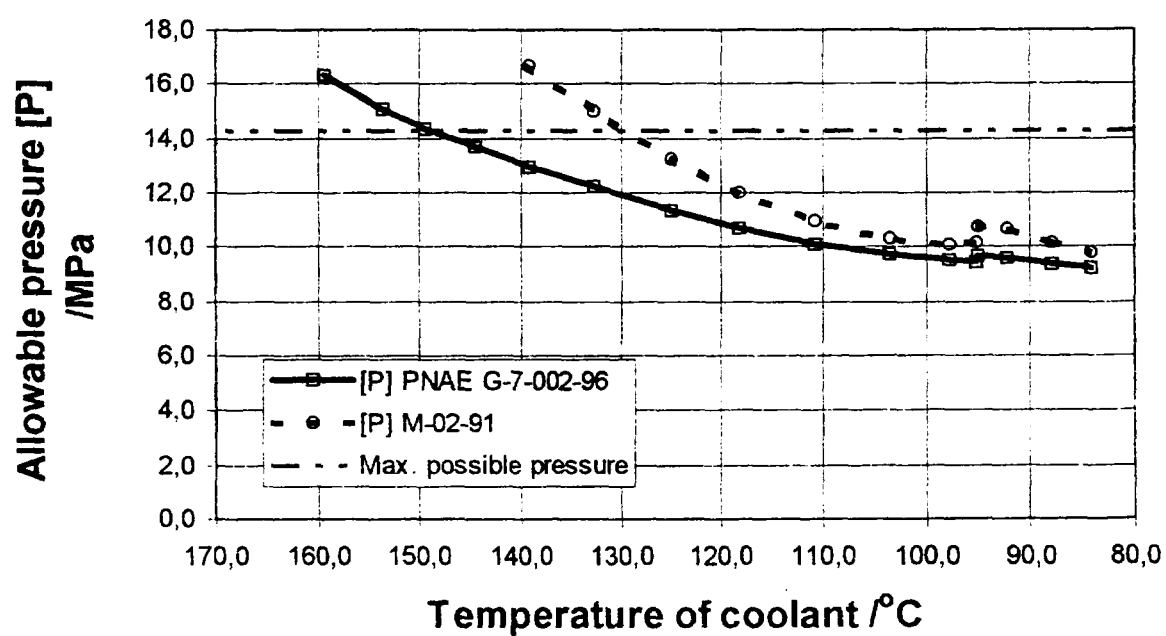
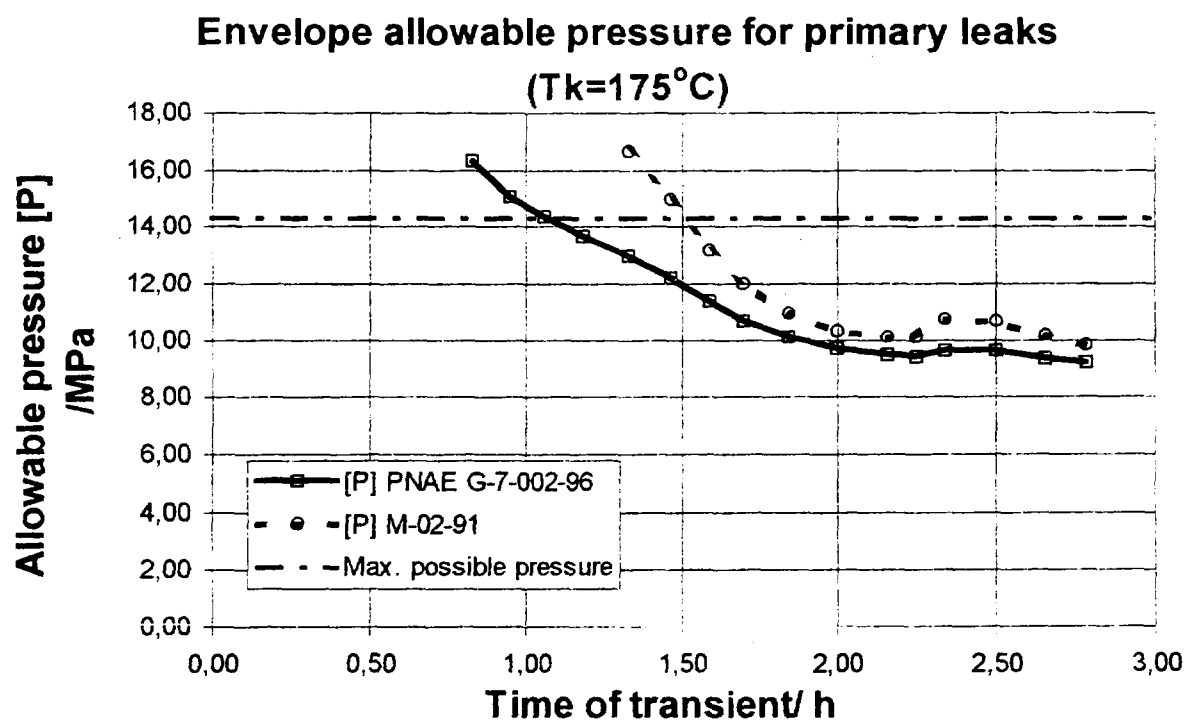


Fig. 8

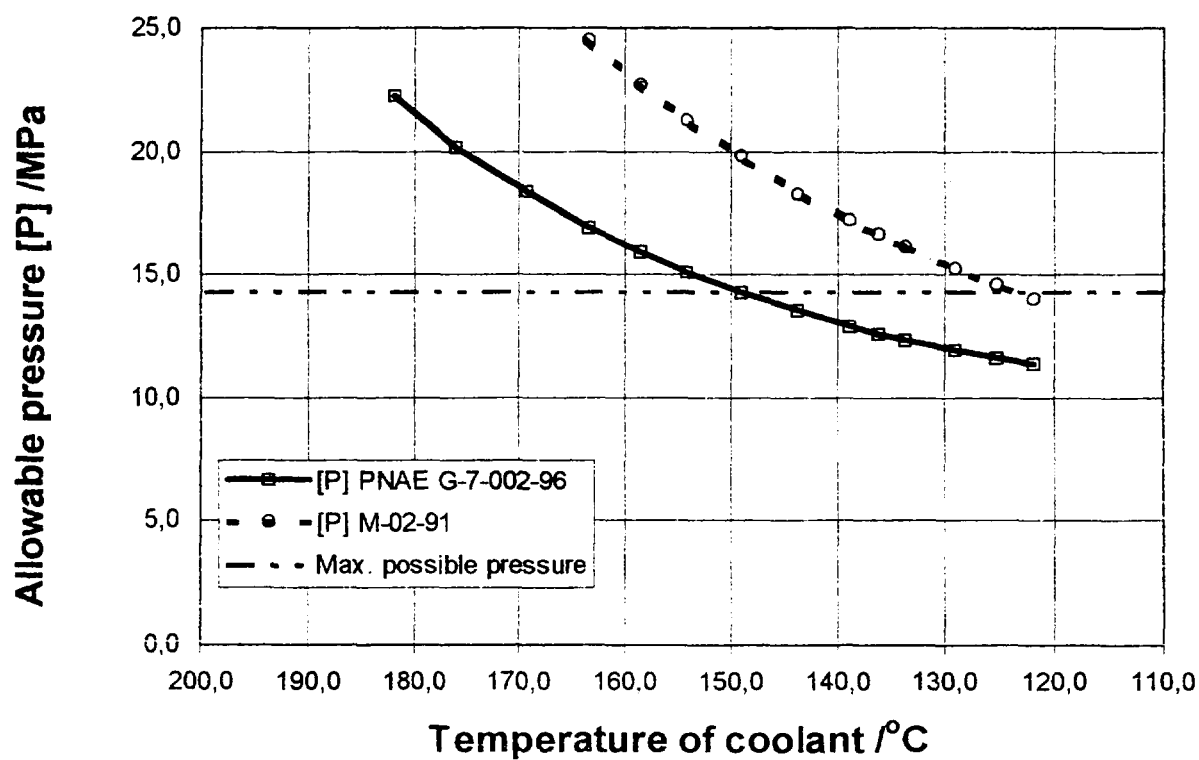
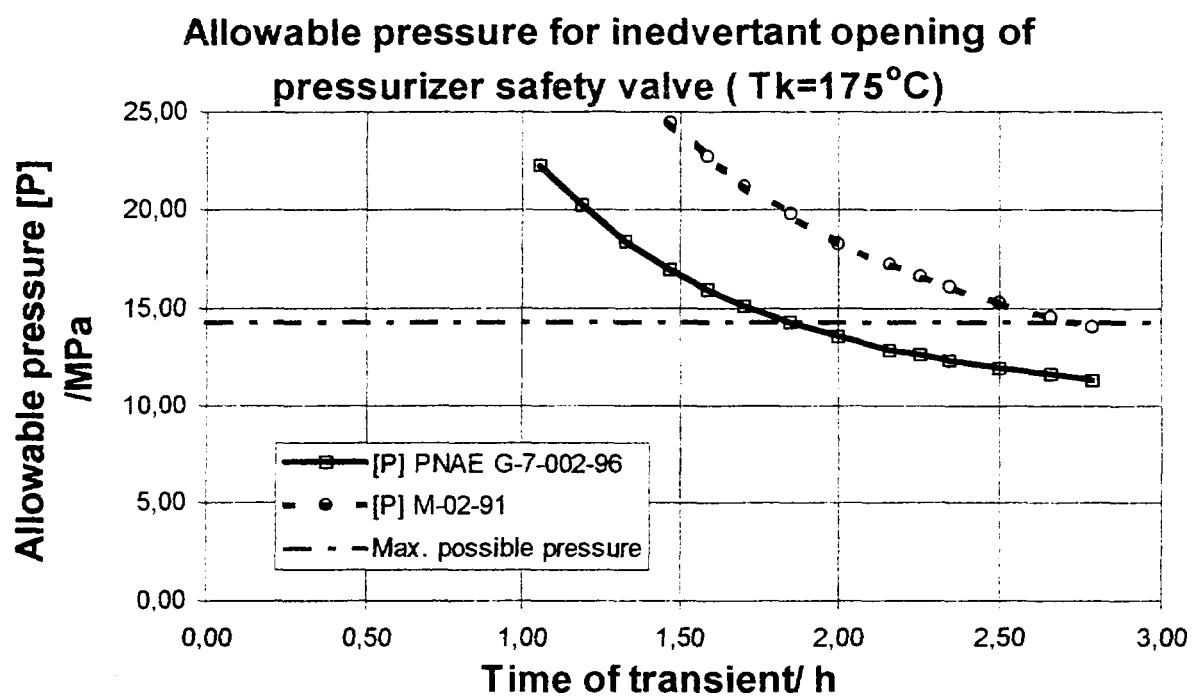


Fig. 9



VVER 440 INTEGRITY ASSESSMENT WITH RESPECT TO PTS EVENTS

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ABSTRACT

The present state of art in VVER 440 RPV integrity assessment with respect to PTS events utilized in Slovakia is reviewed briefly in this paper. Recent results of some PTS's (very severe) analyses, shortly described in our paper, have confirmed the necessity of elaboration of new more sophisticated procedures again. Such methodology should be based on prepared IAEA Guidance.

NOMENCLATURE

K_I	stress intensity factor
K_{IC}	fracture toughness
J	J-integral
T_K	critical temperature of brittleness
T_{ka}	allowable critical temperature of brittleness
t	vessel wall thickness a semielliptical surface crack depth
c	semielliptical surface crack half-length
RPV	reactor pressure vessel
NPP	nuclear power plant
FEM	finite element method
LEFM	linear elastic fracture mechanics

1. INTRODUCTION

RPV structural integrity is one of the most limiting factors with regard to NPP operational safety. Therefore experimental and calculational assessment of pressure vessel integrity has to be carried out both in design and in service stages. Structural integrity assessment approach commonly applied in analyses performed for operating Slovak VVER 440 RPV's is briefly described in this paper. Recent results of some PTS's analyses, shortly presented in our paper, have confirmed the necessity of elaboration of new more sophisticated procedures again. Such methodology should be based on internationally accepted recommendations.

2. INTEGRITY ASSESMENT

Common approach in the area of NPP RPV integrity assessment should be based on following steps in principle :

- a) selection of integrity limiting events (PTS),
- b) thermal hydraulic analyses,
- c) RPV structural integrity analysis

Only problems referring to the RPV structural analysis will be dealt with in this presentation. Up to now applied approach to the evaluation of resistance against non-ductile fracture of Slovak VVER NPP RPV's has been based on LEFM procedures and namely on the philosophy of Russian Strength Calculation Code [1]. With no regard to the fact this Code is valid only for design stage it has been commonly utilized in inservice stage analyses too. Using other Code (ASME) without any additional very deep analysis not only of used methods and approaches but also of main principles put into Code construction is practically impossible (detail comparison of both Codes was presented by Brumovský in [3]). For the reason of calculation rationality (including time and money consumption) typical RPV structural integrity analysis has been divided into separate steps :

- a) temperature and stress field time course calculation by means of FEM computer programmes (PMD, ADINA),
- b) postulated defect stability assessment by means of LEFM criterion $K_I(t)$ ($K_{IC}(t)$)

Commonly used linear elastic material model seems to be adequate for most stress analyses conditions, but in some PTS cases vessel cladding exhibits stresses over yielding point and plasticity effects should be taken into account. Nevertheless the influence of plasticity on structural analysis results seems to be very limited under applied approach due to the fact only RPV wall thickness without cladding is considered in stage b), i.e. in postulated defect stability calculations cladding is not considered as load bearing component. The second important problem comprised in stress analysis is connected with a way how to respect the existence of residual stresses which arise by vessel manufacturing but which course is "deformed" by vessel operation cycles. According to our knowledge one conservative solution of this problem, which has been utilized also in our practise, consists in the application of TREF (temperature when thermal stresses are at zero level) equal to the temperature of RPV under normal operation conditions. This approach supposes that residual stresses are eliminated under normal operation conditions on account of arisen thermal stresses (with the same value but opposite sign).

Subsequent step of RPV structural integrity analysis utilizes stress and temperature fields calculated for the whole event time course in the first step. K_I and K_{IC} dependences on time are calculated and compared in all RPV important locations and allowable value of critical temperature of brittleness T_{ka} is identified. K_I are evaluated for reference circumferential and axial surface defects (on the border between cladding and base metal

or weld and on the outer vessel surface) with a/t (0.25, $a/c=2/3$ in the deepest crack point and on the surface. K_I is calculated by the use of stress component courses through the vessel wall. Various K_I calculation schemes are available which differ on the way of stress course processing. Both well known simplified analytical methods utilizing stress linearization and weight function method were applied. Stress linearization is able to be carried out by different manners and output membrane and bending stress can lead to very different results with respect to K_I . In accordance with our experience and with results of some comparative calculations weight function method seems to be most suitable (and accurate) analytical method of K_I calculation. The weakness of all above mentioned analytical procedures consist in they process results of linear elastic stress analysis with no respect to possible influence of plastic deformation arising especially in the area close to cladding.

In case of plastic deformations due to cladding existence can not be neglected numerical calculations of J integral by means of FEM computer codes using meshes with crack presence should be applied to validate analytical procedures and to establish correction factors applicable on pure K_I linear elastic solutions for given RPV geometry. Routine use of FEM J-integral calculation in common and frequently repeated integrity analyses should be avoided.

With the aim to solve problems connected with Bohunice VVER V 230 RPV's integrity evaluation a new research project "Assessment of Resistance of Reactor Pressure Vessels in the NPP V-1 Jaslovske Bohunice against Non-ductile Failure" has been started in 1996. The analysis of above mentioned problems shall be a part of much more broadly defined solution.

3. THE RESULTS OF THE ANALYSIS OF SEVERE PTS EVENTS

In 1996 calculations were carried out to prove the resistance against nonductile fracture of both Bohunice V1 NPP RPV's and Bohunice V2 NPP RPV's with respect to a small set of PTS events which were expected to be probably most severe ones. The list of these events was determined studying international experience and conditions of our NPP's operation. The definition of time course of these events gave us very severe loading combining high thermal and pressure stresses. We would like to illustrate some problems existing in applied current integrity assessment methodology (shortly described in chapter 2) on the example of V1 NPP RPV's analyses.

The V1 NPP RPV's integrity assessment with respect to selected PTS events was carried out. Two severe transients were chosen for RPV structural integrity analysis in accordance with present experience. The first PTS is connected with spurious opening and later reclosing of pressurizer safety valve and the second one is the result of SG header opening with later isolation of affected loop by operator. In the first stage

thorough stress and strain calculations were performed. Boundary conditions describing temperature and heat transfer coefficients time course were defined during preceding thermal-hydraulic analysis. In the second stage RPV integrity was investigated.

Stress and deformation components arising within vessel wall due to thermal and pressure changes connected with above mentioned PTS transients were determined at important time points of analysed regime by means of FEM Computer code PMD II. V1 NPP RPV resistance against brittle fracture was investigated with the use of computer code INTEG [5] partially based on Russian Strength Calculation Code [1]. Boundary conditions inside the RPV vessel during investigated event were supplied as results and input data of calculations performed by computer codes RELAP and NEWMIX. Applied RPV vessel FEM mesh containing vessel cladding and all welds is shown on Fig.1. Reference temperature equal to 269 C (the beginning of event) was used for the purpose of thermal stresses calculation.

K_I and K_{IC} dependences on time were calculated and compared in RPV important locations (weld No.4 and basic metal near to core) and allowable value of critical temperature of brittleness T_{ka} was identified. K_I was evaluated for postulated circumferential and axial surface cracks (on the border between cladding and base metal or weld and on the outer vessel surface) with a/t (0.25, $a/c=2/3$ in the deepest crack point (point A) and on the "surface" (point B). K_I was calculated by the use of stress component courses through the vessel wall. In accordance with Russian Code [1] only stress and temperature courses through the thickness of basic or weld material without cladding were used for K_I and K_{IC} calculation in evaluated RPV sections. Various available K_I calculation schemes (5 variants) were applied. They differ in the way of stress course processing. Both well known simplified analytical methods utilizing stress linearization [6], [7], [1] and weight function method [8], [10] were applied. Stress linearization is able to be carried out by different manners and output membrane and bending stress can lead to very different results with respect to K_I . Some calculation results are presented in Fig 2 - , single K_I curves correspond to following K_I calculation schemes :

- var.1 - calculation according to [7] - K_{Icsn} on figures
- var.2 - calculation according to [6] - K_{Irne} on figures
- var.3 - calculation according to [8] - K_{Ikfk} on figures
- var.4 - calculation according to [10]- K_{Iom} on figures
- var.5 - calculation according to [1] K_{Intd} on figures (calculation only for point A of postulated crack)

All applied K_I calculation schemes (including original Russian Code procedure [1] !!) provide for V1 2nd unit RPV the same result, i.e. the weld 0.1.4 integrity can't be ensured for cracks with dimensions postulated by Russian Code during both analysed PTS events. All applied K_I calculation schemes provide for 1.st unit weld 0.1.4 the same result, i.e. its integrity is ensured for cracks with dimensions postulated by Russian Code during both analysed PTS events. Results obtained by K_I calculation variants 2,3 and 4 are very close,

values obtained by two different weight function methods application (variants 3 a 4) are almost identical in all cases.

For postulated cracks with dimensions up to 20 mm, RPV integrity would be ensured in all events and all vessel sections. Therefore similar reference cracks with assured high detection probability could be used instead of Russian Code postulated maximum cracks preparing more sophisticated RPV integrity assessment methodology. The assessment of V1 NPP RPV integrity is complex problem including material properties determination, selection of important PTS events, thermal-hydraulic analyses and structural integrity analyses. One must consider that these problems should be solved in the whole complexity, therefore longterm research project has been started for this purpose. All questions connected with partial problems such as suitability of various K_I calculation methods, their accuracy in compare with direct FEM calculation results and development of suitable correction factors are contained in this project. Therefore all partial results achieved at present stage are not final with regard to V1 NPP RPV integrity assessment. Nevertheless some preliminary measures ought to be considered. Both analysed PTS events, and especially the first of them, have very severe time course. High stress level is caused by the combination of thermal stresses due to temperature shock and stresses due to high pressure appearing during this shock in primary circuit. Both effects are the result of PTS definition (or course), therefore operational measures to limit the possibility of such PTS events occurrence should be adopted.

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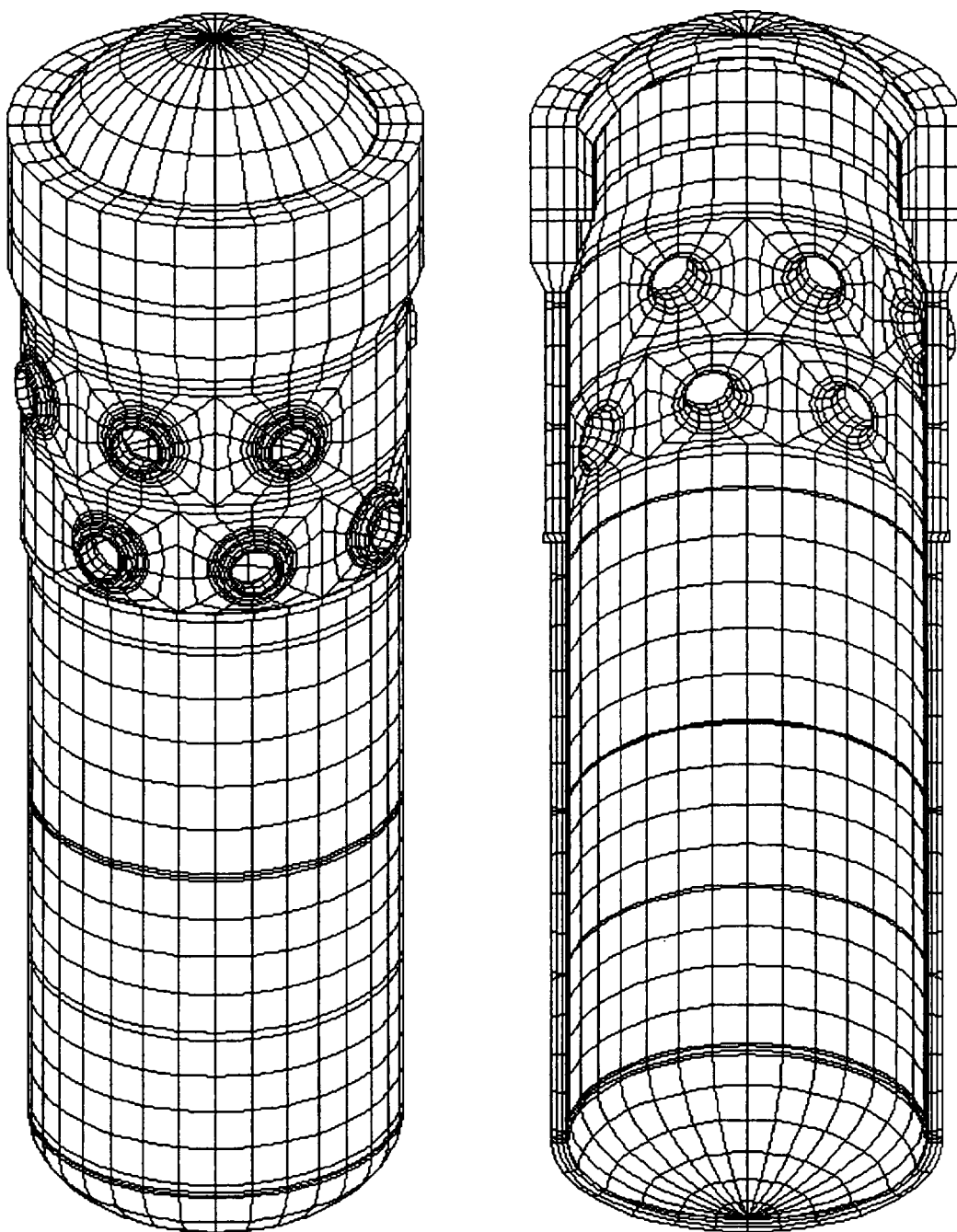


Fig.1 - V1 NPP RPV FEM Mesh



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PRESSURISED THERMAL SHOCK
Esztergom, Hungary, 5-8 May 1997.

The Cladding Effect at PTS Assessment
of VVER-440 RPV-s

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1. Pressure Vessel: VVER 440-213. (Fig. 1.)

2. Calculation model and Code used

The analytical computer program called Analytical Calculation for Integrity of Beltline Reactor Pressure Vessel (**ACIB-RPV**) was used for the analysis of the stability of defects in reactor vessel walls during pressurised thermal shock.

To verify the use of the ACIB-RPV program a comparison was with the IVOFEM code, with the finite element calculation results made by ADINA.

This verification proved, that the results calculated with the analytical ACIB-RPV code are within 4% scatter compared with results obtained by other, internationally used codes.

3. Postulated defects

Spectrum of elliptical circumferential surface cracks with a depth range 0,..79 mm in the 15H2MFA weldment, and the clad is postulated broken $a/c=2/3$. Location: weld 5/6.

4. Material parameters used for fracture mechanics calculations:

$$K_{Ic} = 35 + 53 \cdot \exp(0.0217 \cdot (T - T_k)) \quad [\text{MPa}\sqrt{\text{m}}]$$

$$K_{Ic}^{\text{max}} = 200 \quad [\text{MPa}\sqrt{\text{m}}]$$

$$T_{ki} = 87 \text{ } [^{\circ}\text{C}]$$

5. Transient analysed

In this study the selected transient is the Cold Leg Large Brake Loca. (see Fig. 2.)

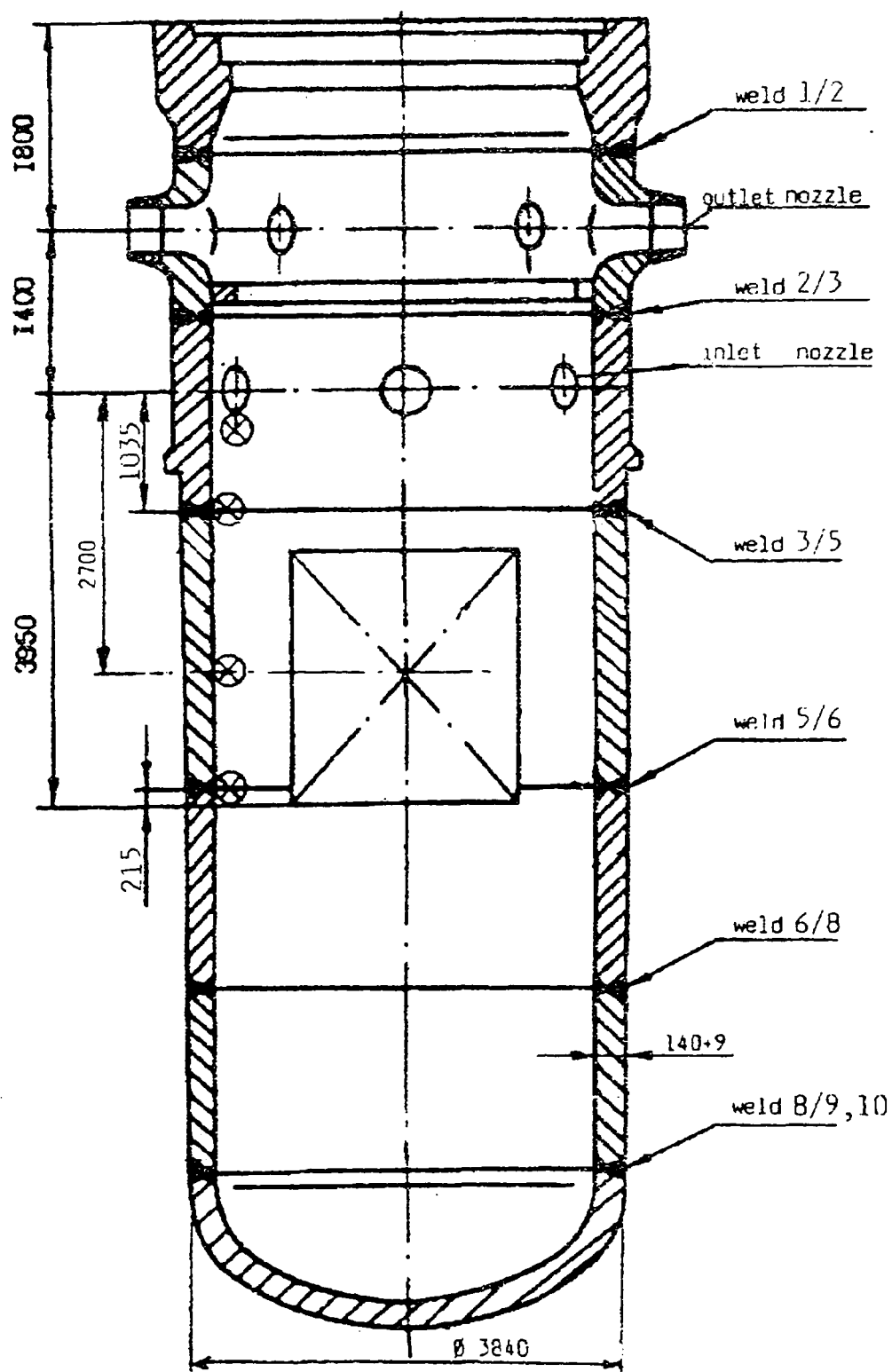
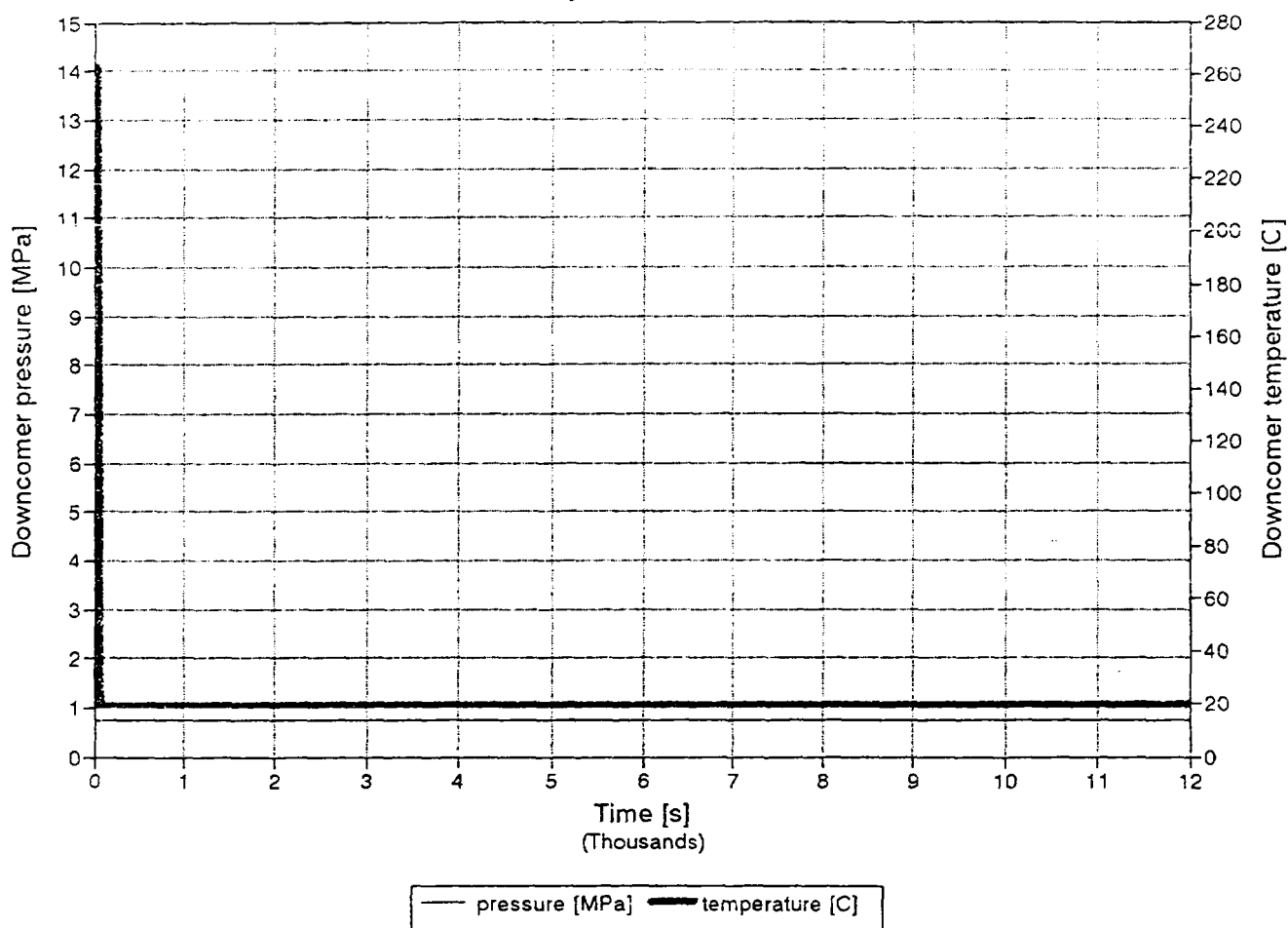


Fig. 1

Fig. 2 Time history for PTS calculation



Case 1.: PTS calculation of an uncladded Reactor Pressure Vessel

Geometrical Data

Vessel geometry:	inner diameter:	[mm]	1771
	outer diameter:	[mm]	1920
Cladding thickness:		[mm]	0
15H2MFA thickness:		[mm]	149
Heat exchange. Coeff.:		[W/m²K]	5000

Material Data

Material Data		15H2MFA	Cladding
Heat conductivity.	[W/mK]	42.0	-
Heat capacity	[J/m ³ K]	3.92x10 ⁶	-
Heat exp. Coeff.	[1/K]	11.5x10 ⁻⁶	-
Elastic. Modulus	[MPa]	2x10 ⁵	-
R _{p0.2}	[MPa]	430	-
Poisson number		0,3	-

Temperature, stress and K_I vs. K_{Ic} distributions at $t=400$ [s] and $t=6000$ [s] are presented in Fig. 3, Fig. 4, Fig. 7 and Fig. 8.

Fig. 5. and Fig. 6 show, that linear elastic calculations are valid.

Fig. 3 Temperature Distributions in RPV Wall

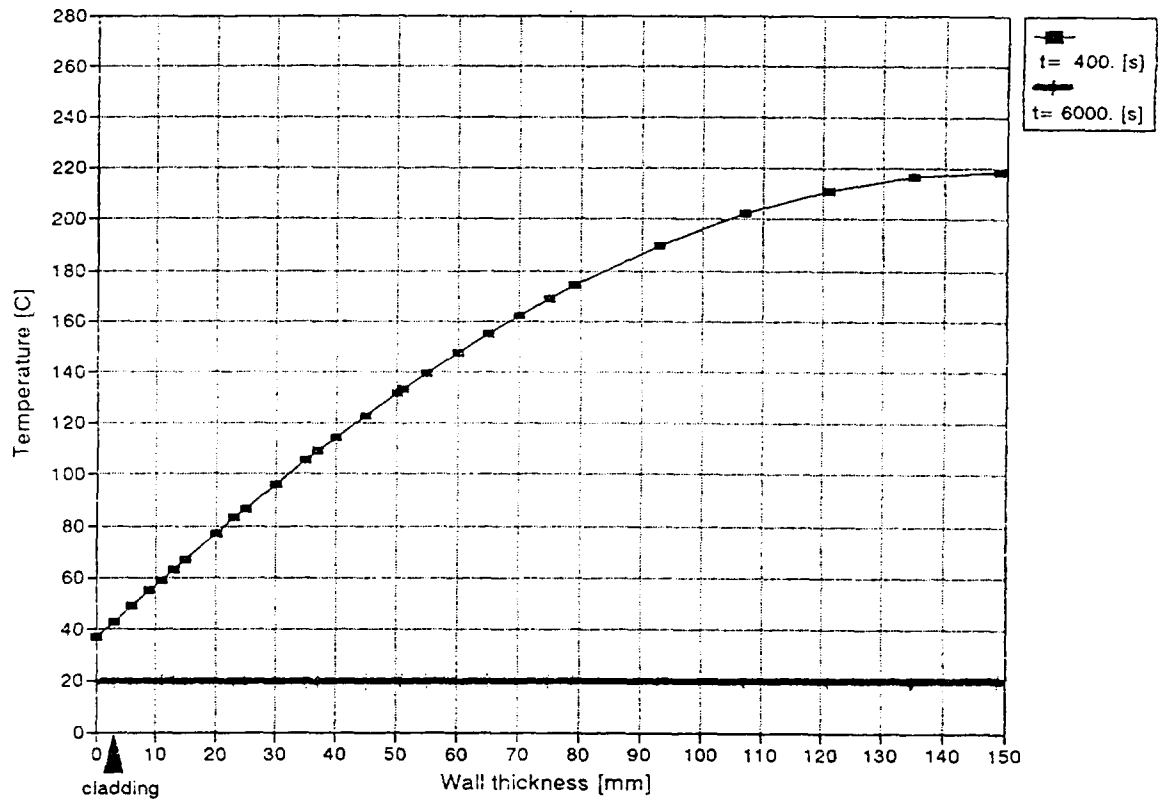


Fig. 4 Stress Distributions in RPV Wall

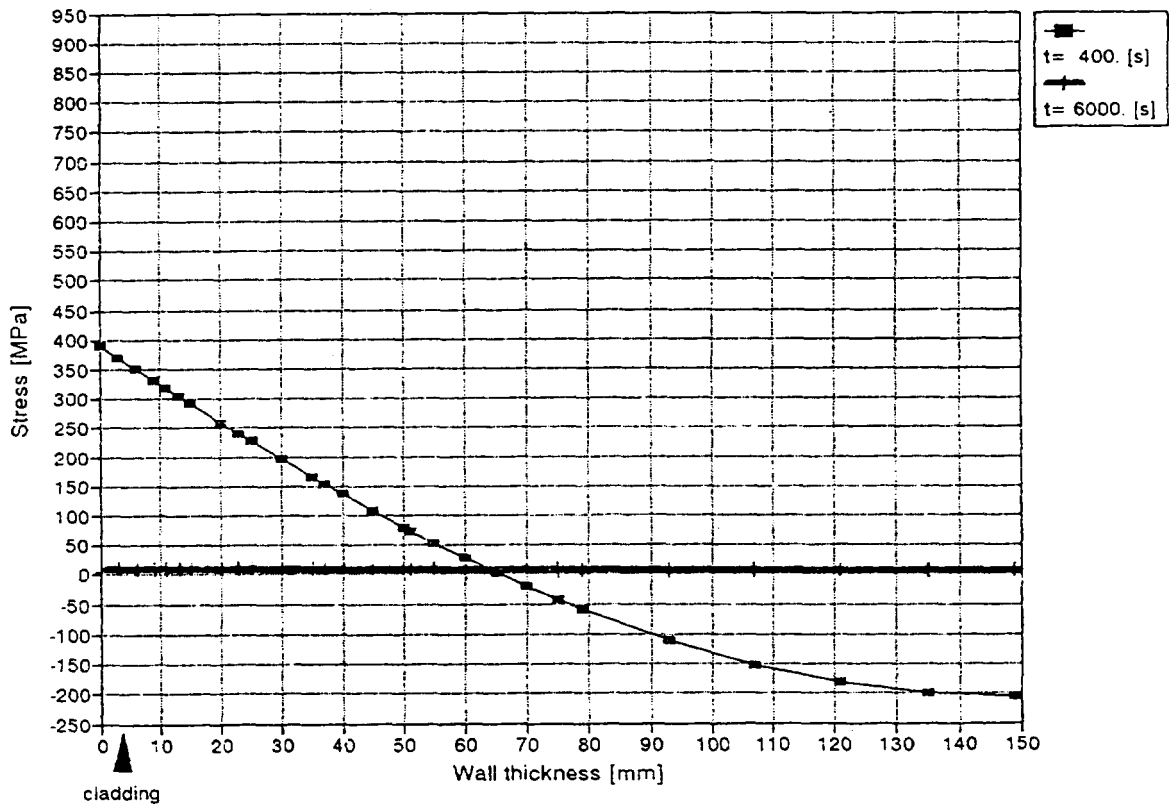


Fig. 5 Stress-strain curves of RPV materials
(15H2MFA)

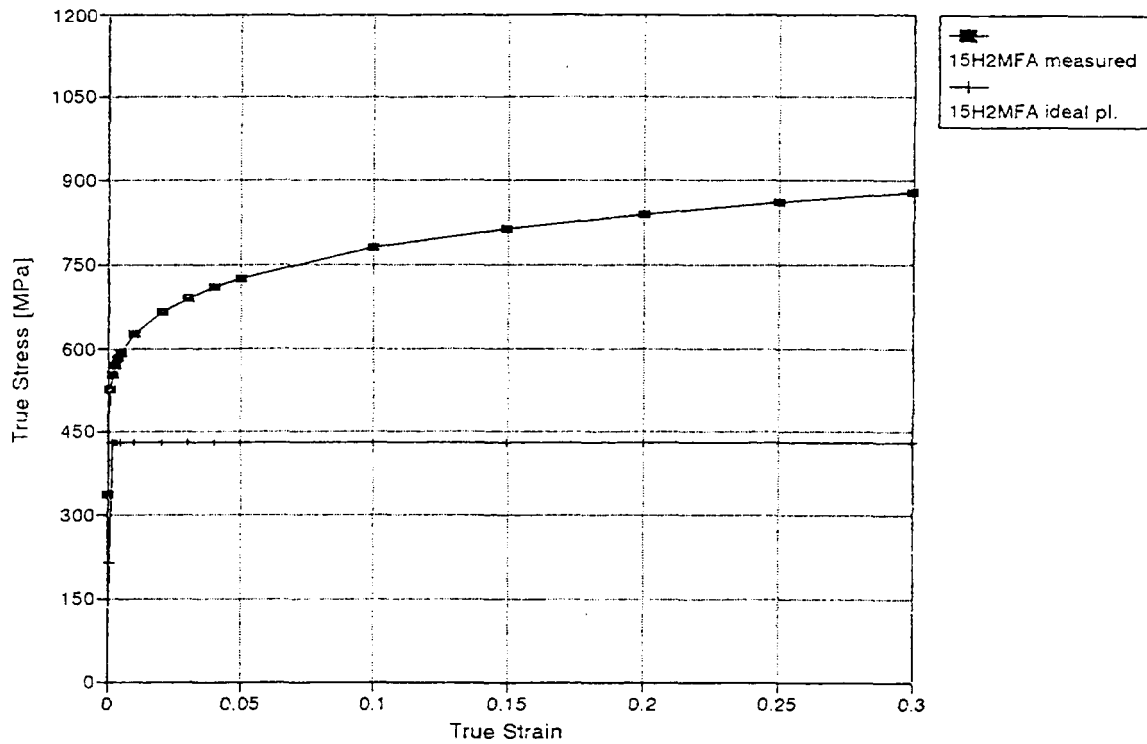


Fig. 6 Stress Distribution in RPV Wall
when no cladding exist

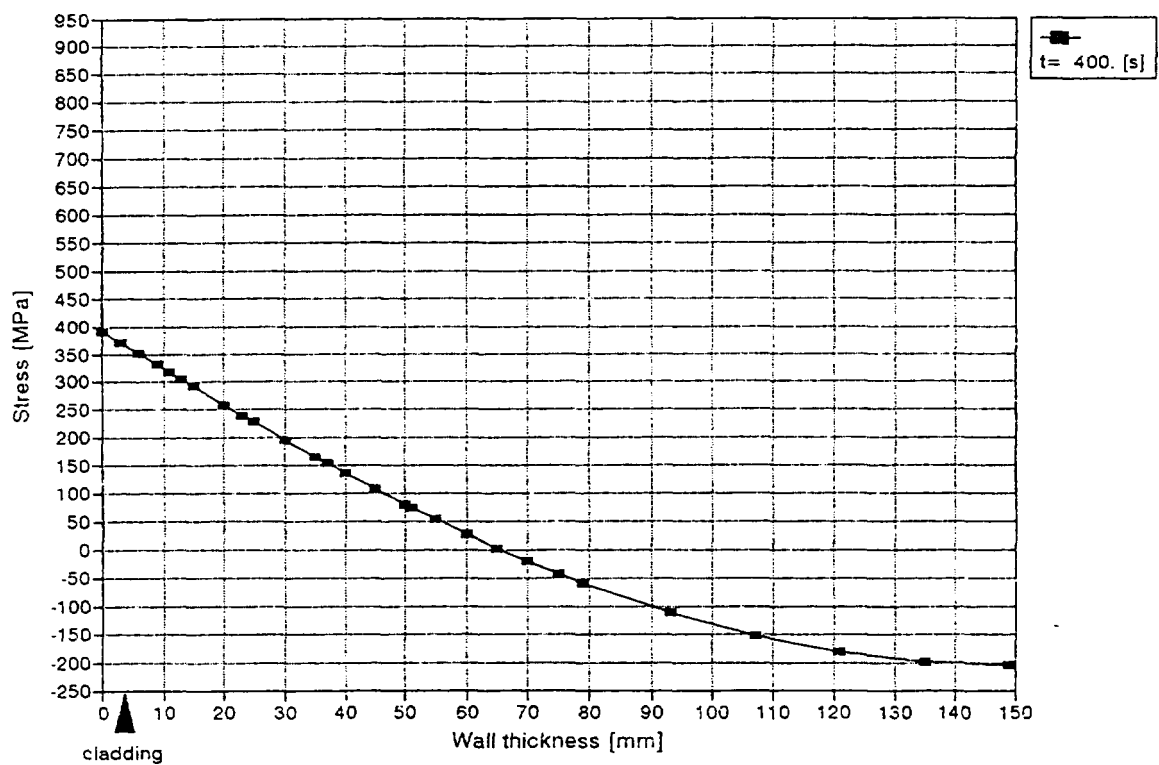


Fig. 7 KI-KIc Distributions in RPV Wall

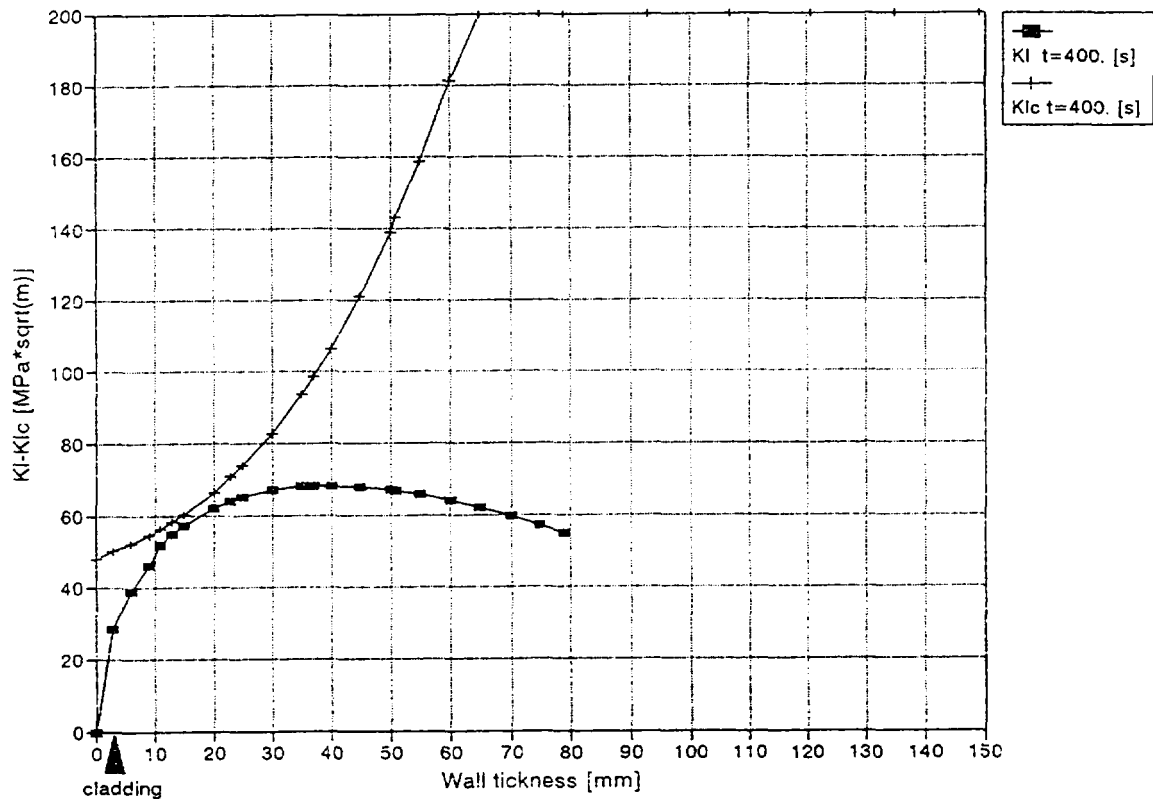
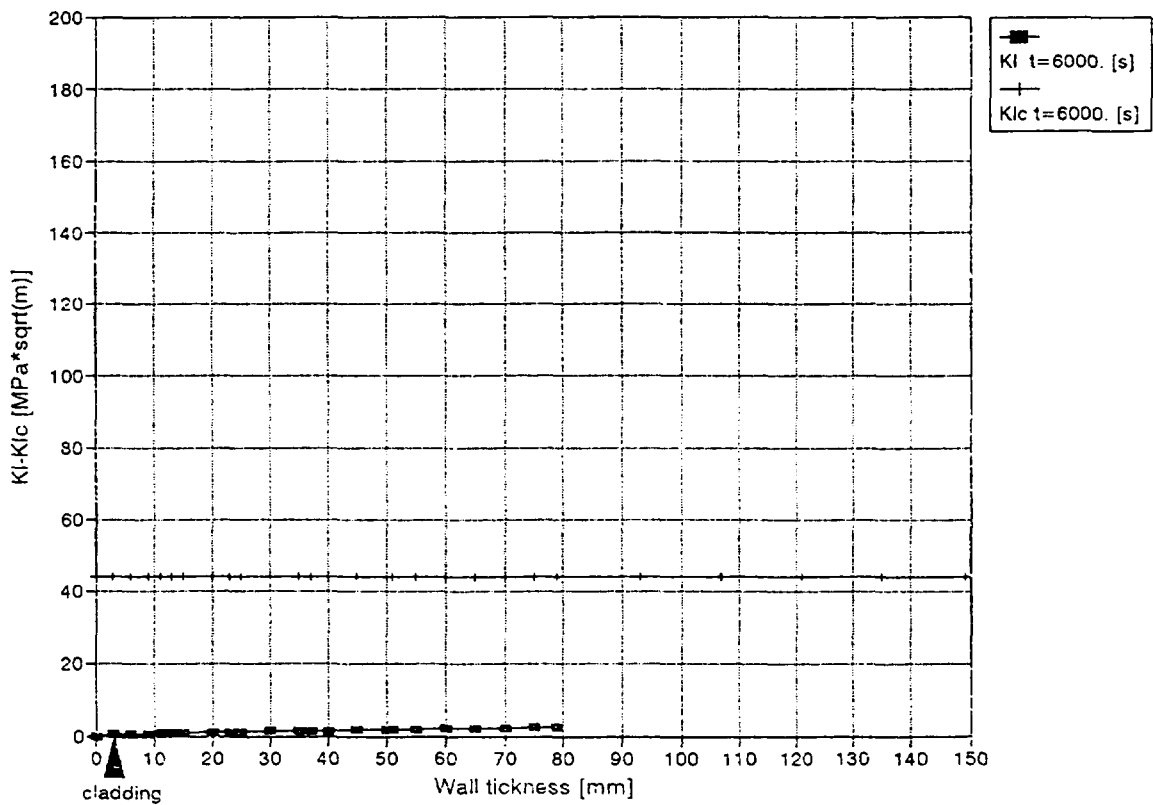


Fig. 8 KI-KIc Distributions in RPV Wall



Case 2.: PTS calculation of a cladded Reactor Pressure Vessel (‘soft cladding’)

Geometrical Data

Vessel geometry:	inner diameter:	[mm]	1771
	outer diameter:	[mm]	1920
Cladding thickness:			[mm] 9
15H2MFA thickness:			[mm] 140
Heat exchange. Coeff.:			[W/m ² K] 5000

Material Data

Material Data		15H2MFA	Cladding
Heat conductivity.	[W/mK]	42.0	14.6
Heat capacity	[J/m ³ K]	3.92x10 ⁶	3.66x10 ⁶
Heat exp. Coeff.	[1/K]	11.5x10 ⁻⁶	11.5x10 ⁻⁶
Elastic. Modulus	[MPa]	2x10 ⁵	2x10 ⁵
R _{p0.2}	[MPa]	430	430
Poisson number		0.3	0.3

Temperature, stress and K_I vs. K_{Ic} distributions at $t=400$ [s] and $t=6000$ [s] are presented in Fig. 9, Fig. 10, Fig. 13. and Fig. 14.

Stresses are over-estimated by elastic calculations, as Fig. 11. and Fig. 12 show, but safety against crack initiation is higher, then in Case 1.

Fig. 9 Temperature Distributions in RPV Wall

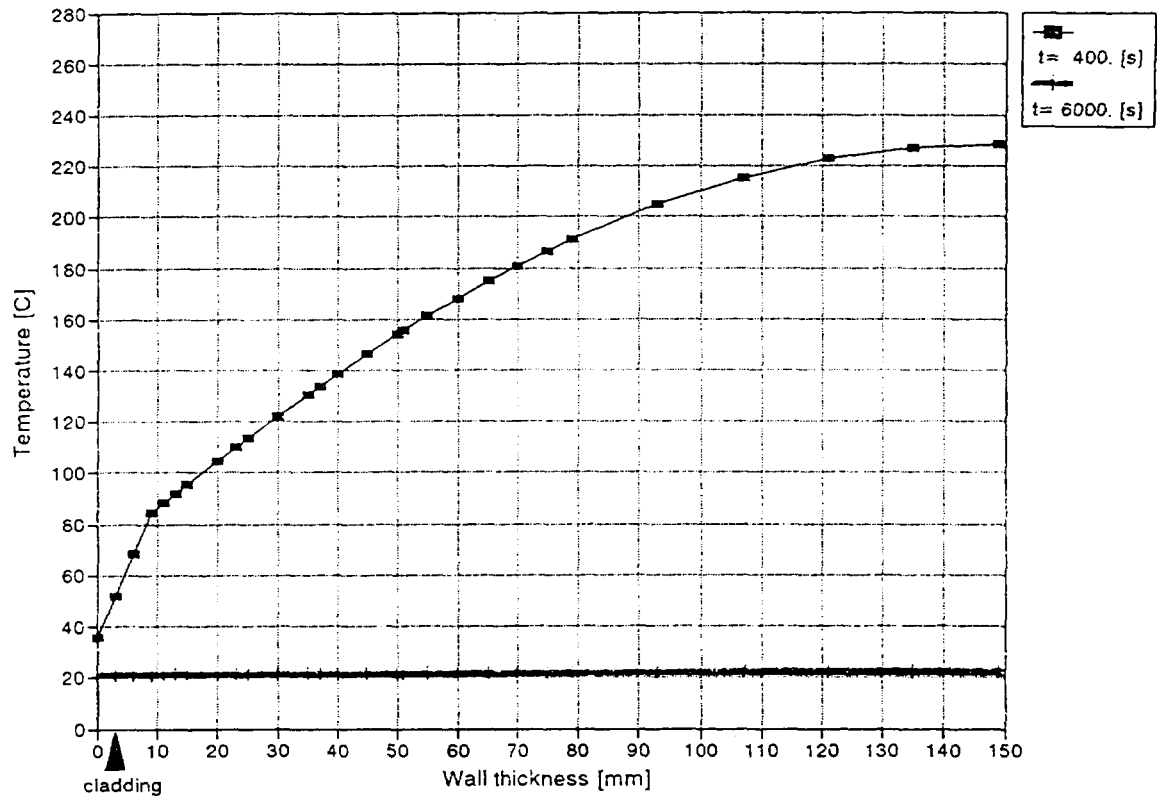


Fig. 10 Stress Distributions in RPV Wall

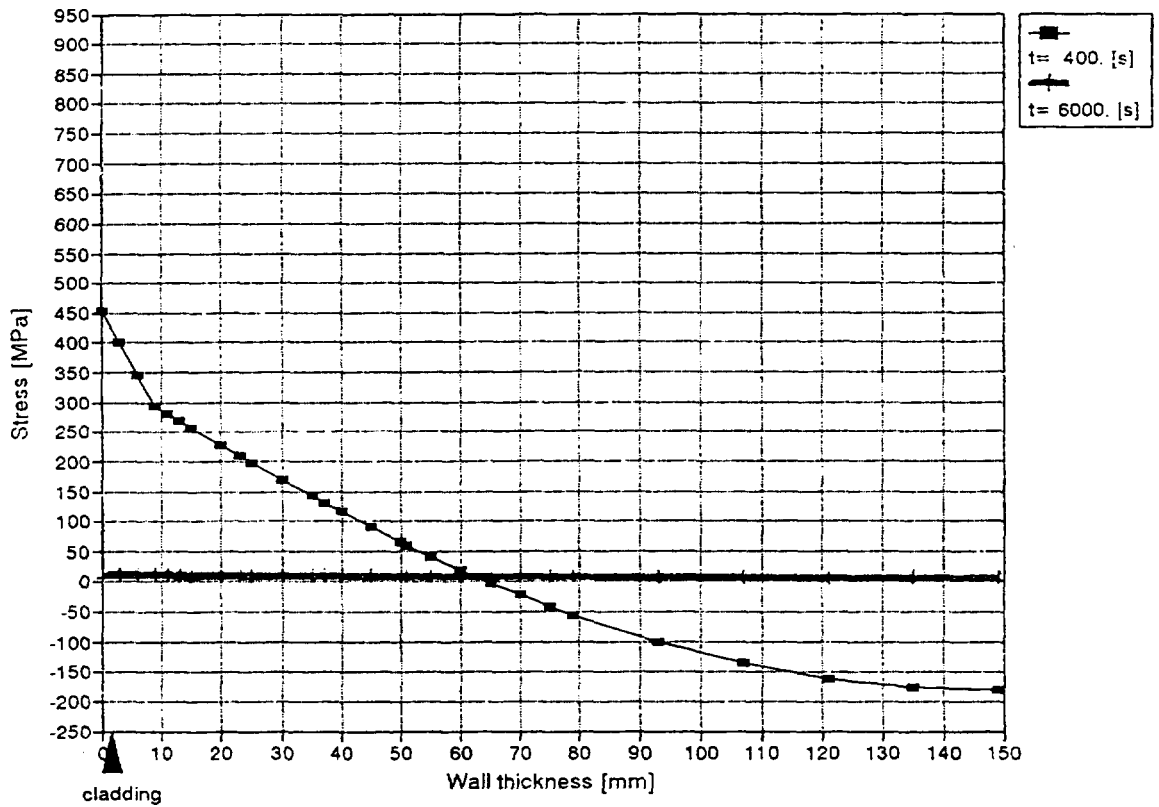


Fig. 11 Stress-strain curves of RPV materials
(15H2MFA and cladding)

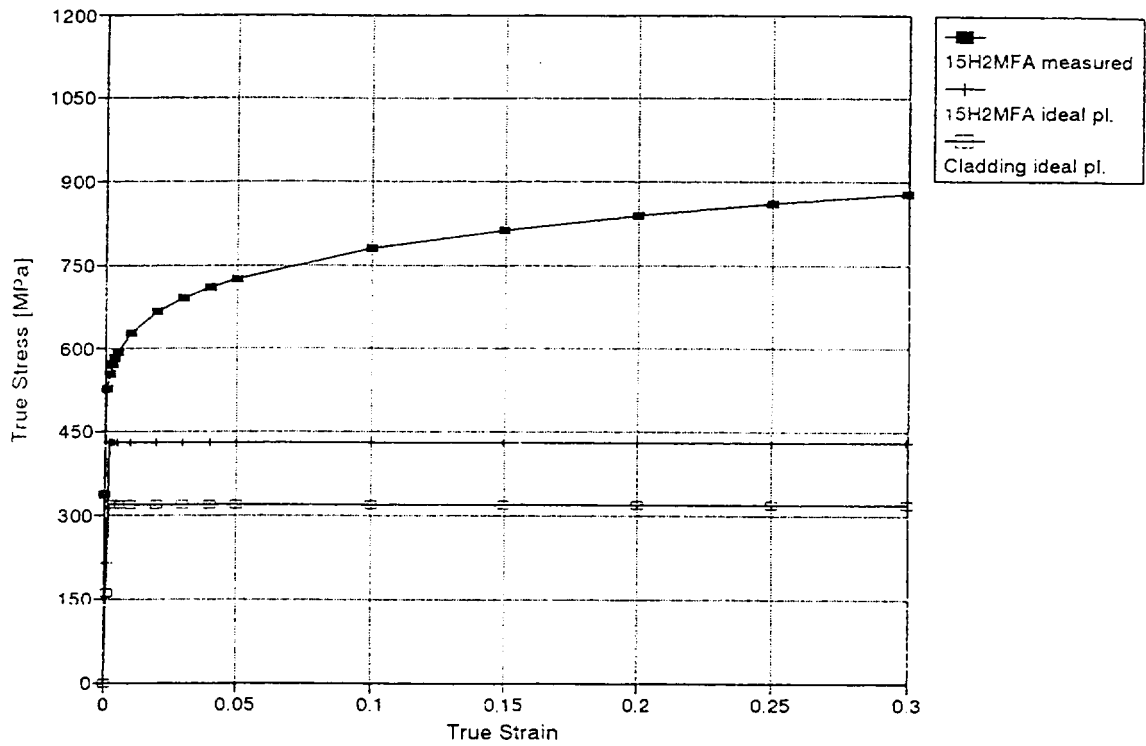


Fig. 12 Stress Distributions in RPV Wall
in the case of soft cladding

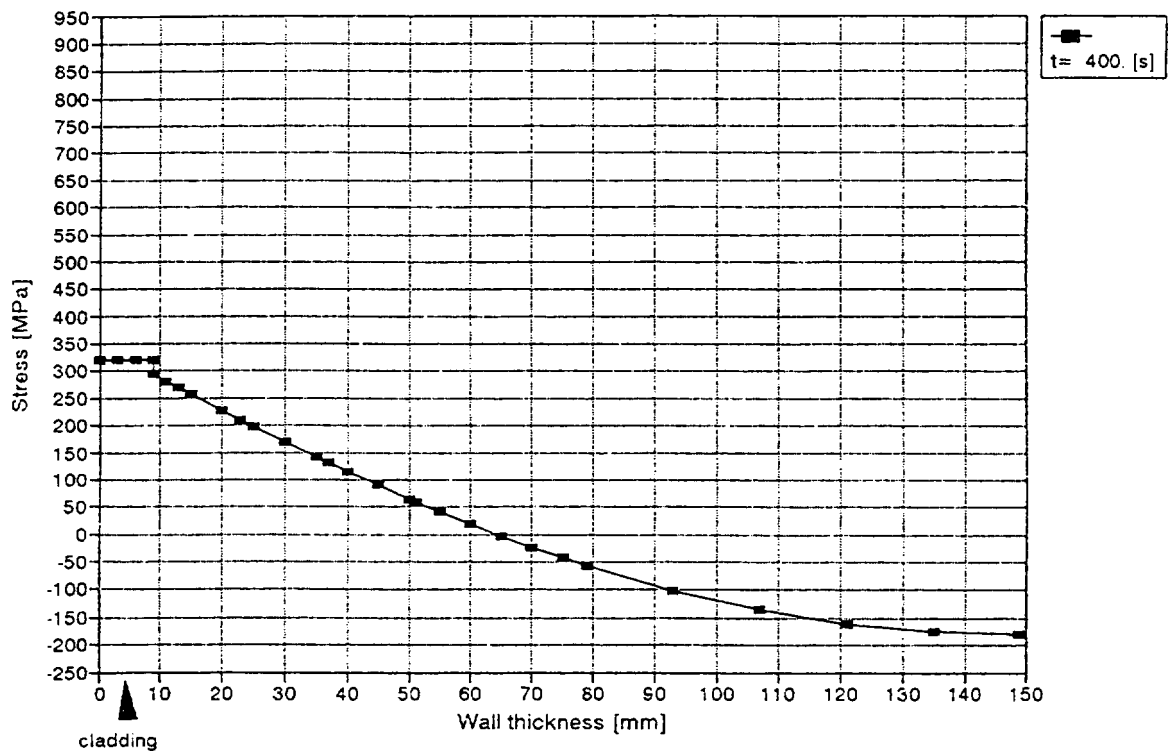


Fig. 13 KI-KIc Distributions in RPV Wall

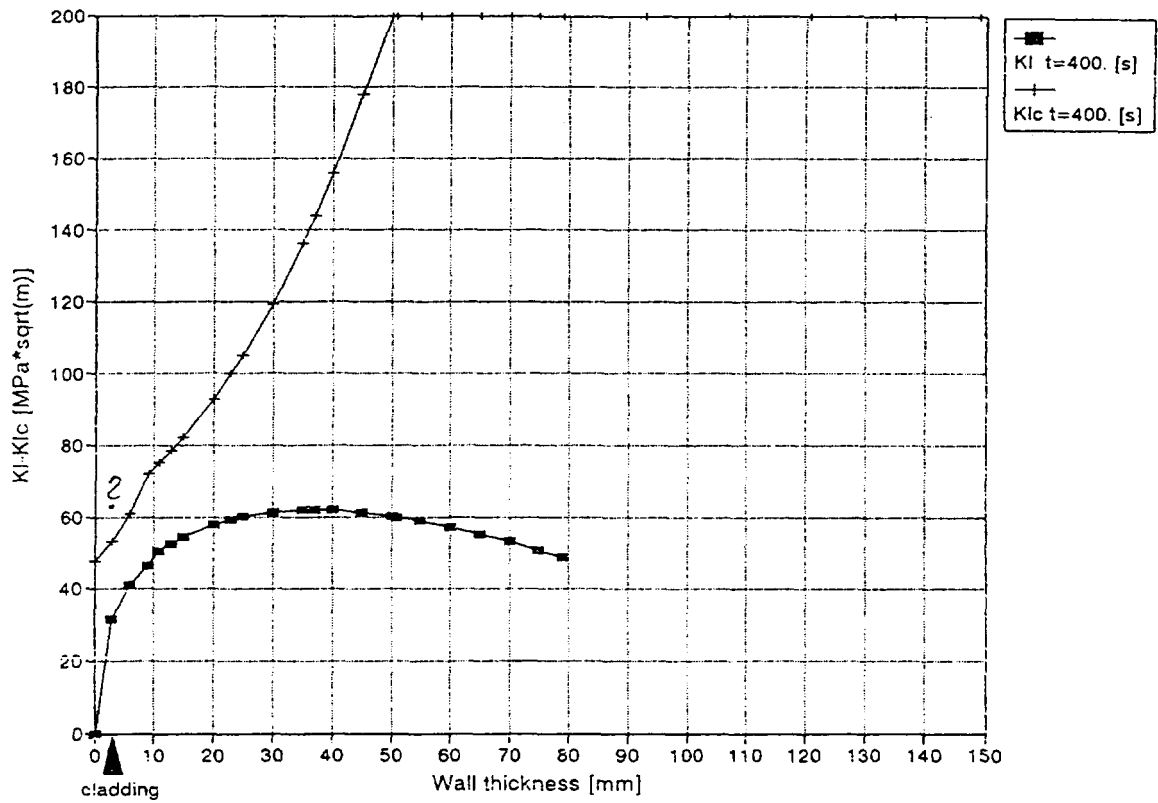
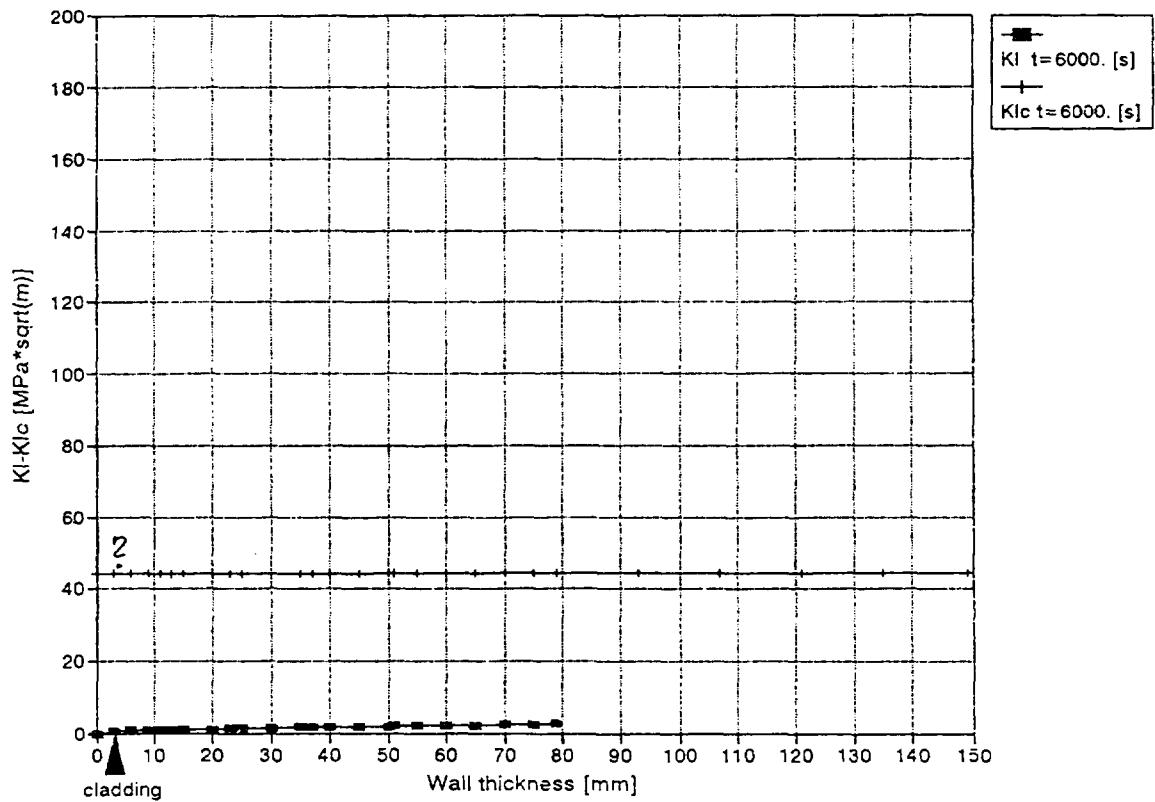


Fig. 14 KI-KIc Distributions in RPV Wall



Case 3.: PTS calculation of a cladded Reactor Pressure Vessel (‘stiff cladding’)

Geometrical Data

Vessel geometry:	inner diameter:	[mm]	1771
	outer diameter:	[mm]	1920
Cladding thickness:		[mm]	9
15H2MFA thickness:		[mm]	140
Heat exchange. Coeff.:		[W/m ² K]	5000

Material Data

Material Data		15H2MFA	Cladding
Heat conductivity.	[W/mK]	42.0	14.6
Heat capacity	[J/m ³ K]	3.92x10 ⁶	3.66x10 ⁶
Heat exp. Coeff.	[1/K]	11.5x10 ⁻⁶	16.81x10 ⁻⁶
Elastic. Modulus	[MPa]	2x10 ⁵	2x10 ⁵
R _{p0.2}	[MPa]	430	1020
Poisson number		0.3	0.3

Stress free temperature of cladding is operational temperature.

Temperature, stress and K_I vs. K_{Ic} distributions at t=400 [s] and t=6000 [s] are presented in Fig. 15, Fig. 16, Fig. 19. and Fig. 20.

Fig. 7. and Fig. 18. show, that linear elastic calculations are valid. Safety against crack initiation is lower, then in Case 1. or Case 2. This is due to very high flow stress rate of the irradiated cladding material, according to measurements of F. Gillemot et. al. in AERI. in 1996. on irradiated tensile specimens.

Fig. 15 Temperature Distributions in RPV Wall

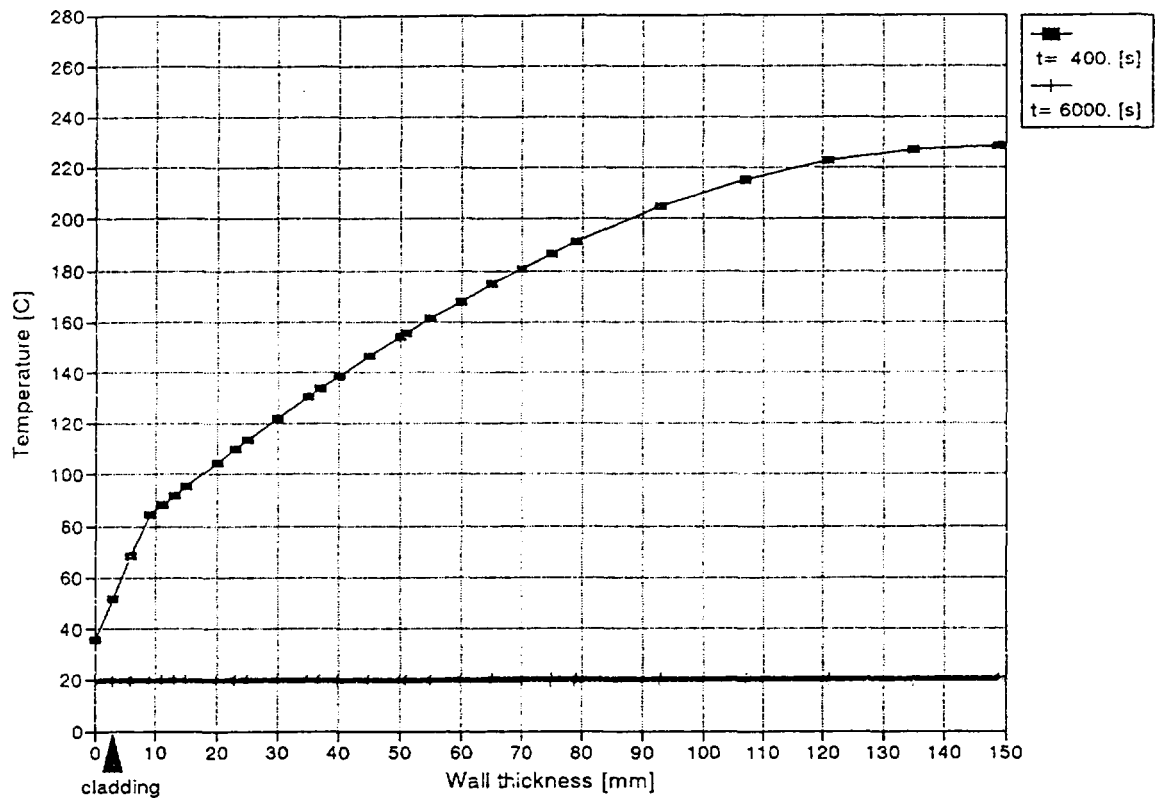


Fig. 16 Stress Distributions in RPV Wall

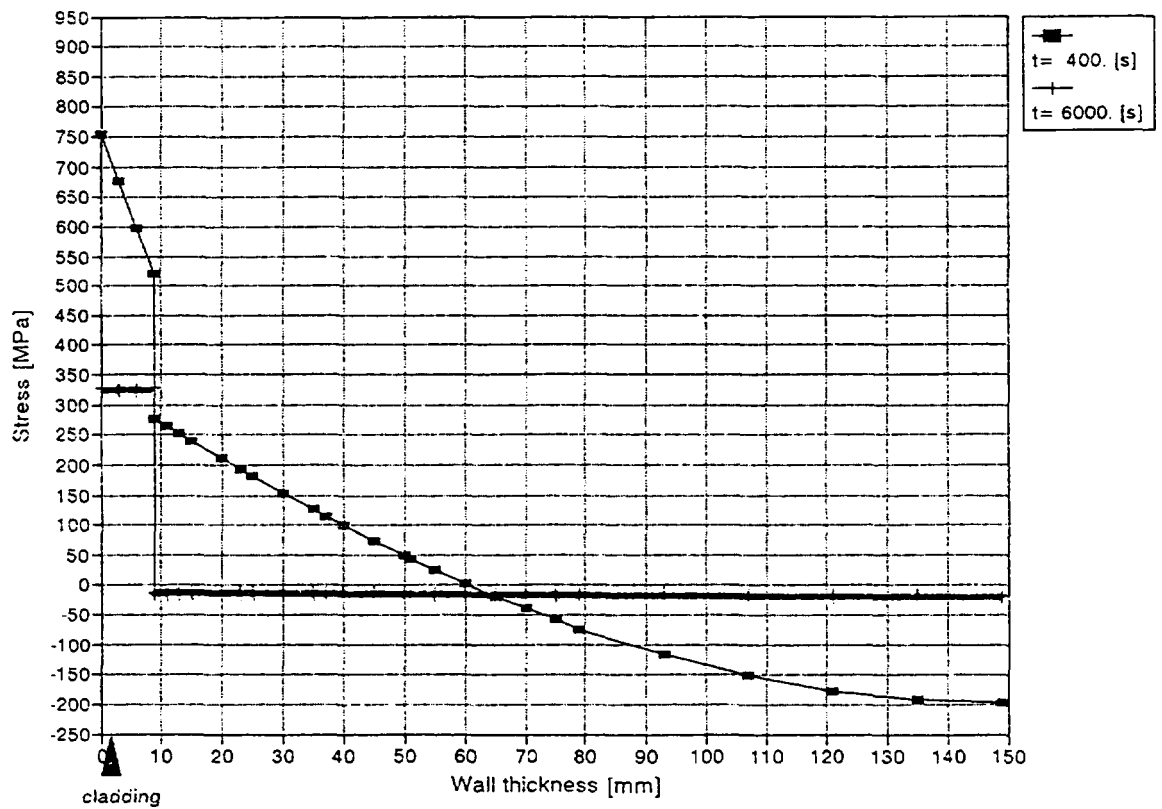


Fig. 17 Stress-strain curves of RPV materials
(15H2MFA and cladding)

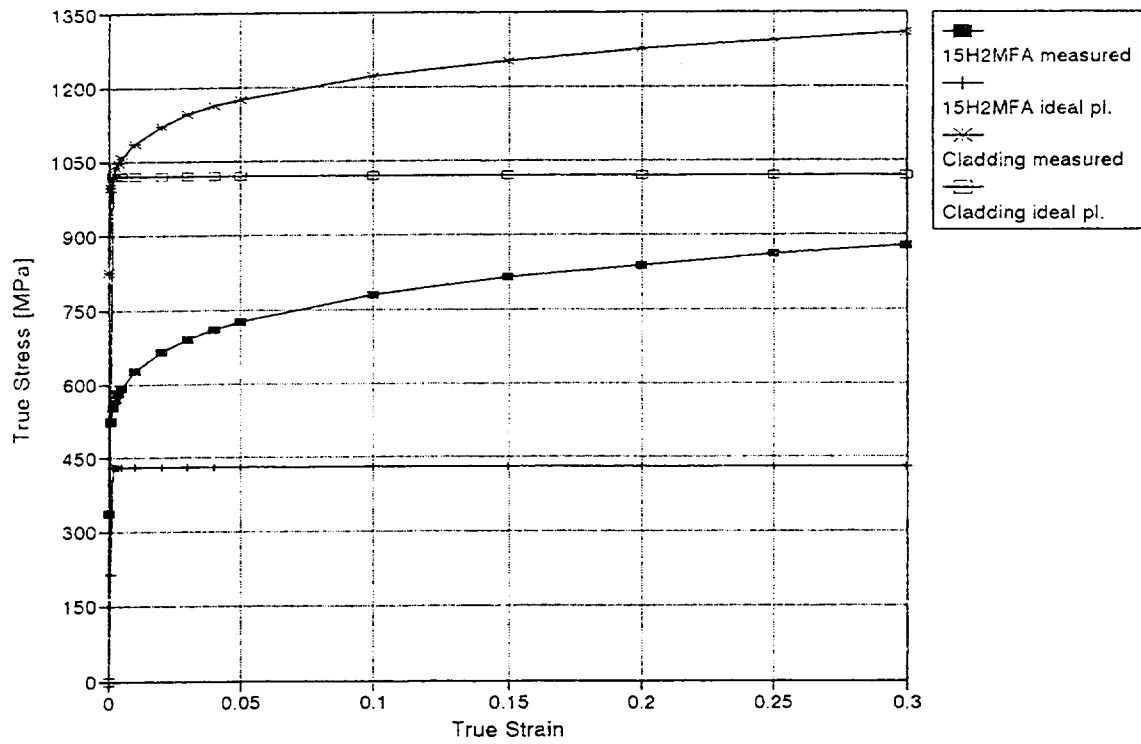


Fig. 18 Stress Distribution in RPV Wall
in the case of stiff cladding

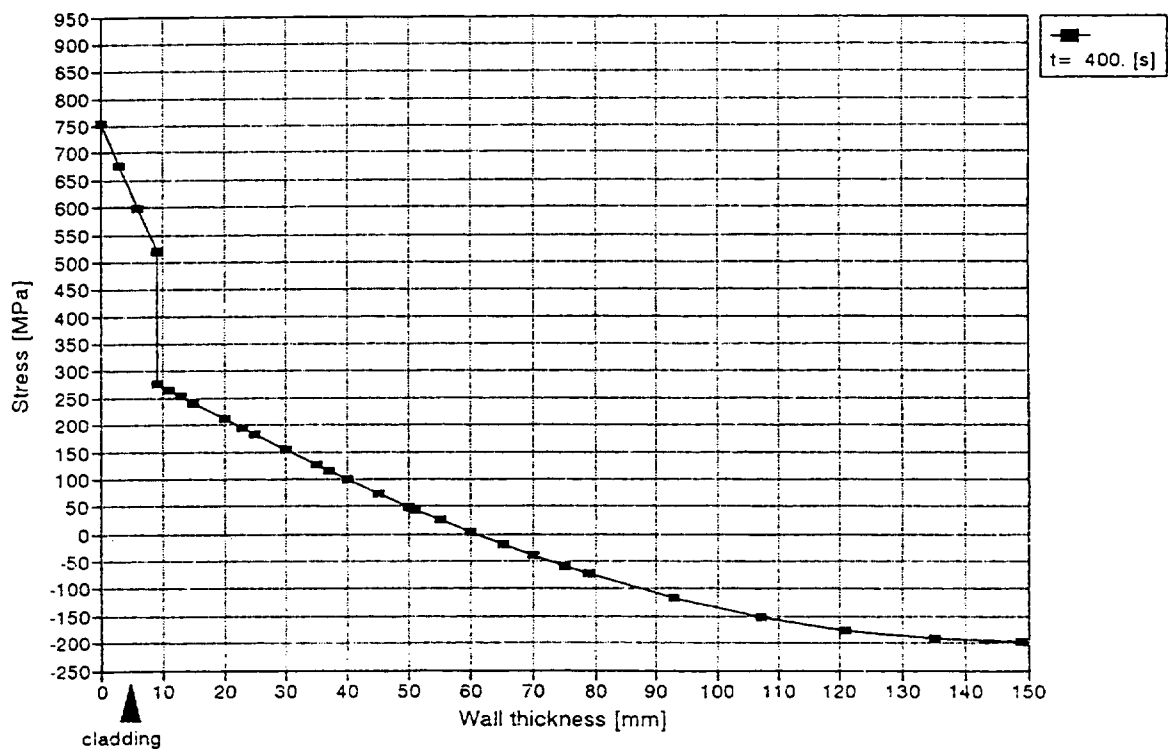


Fig. 20 KI-KIc Distributions in RPV Wall

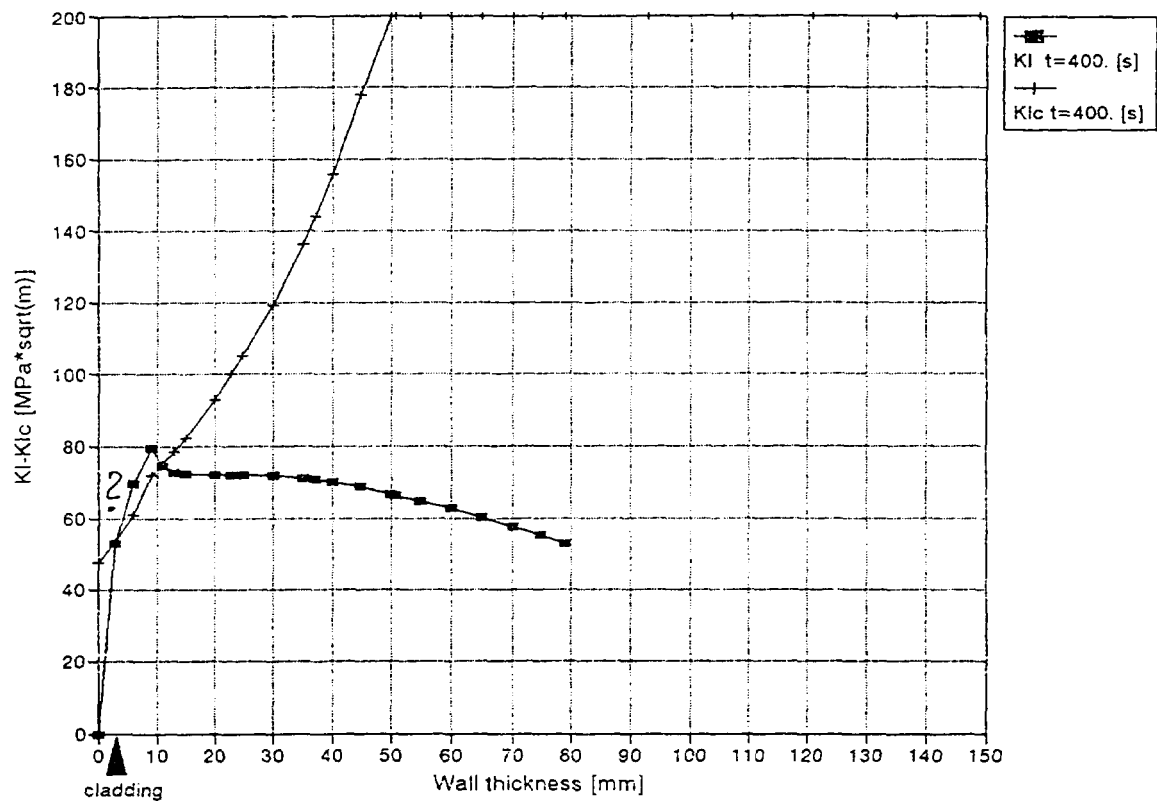
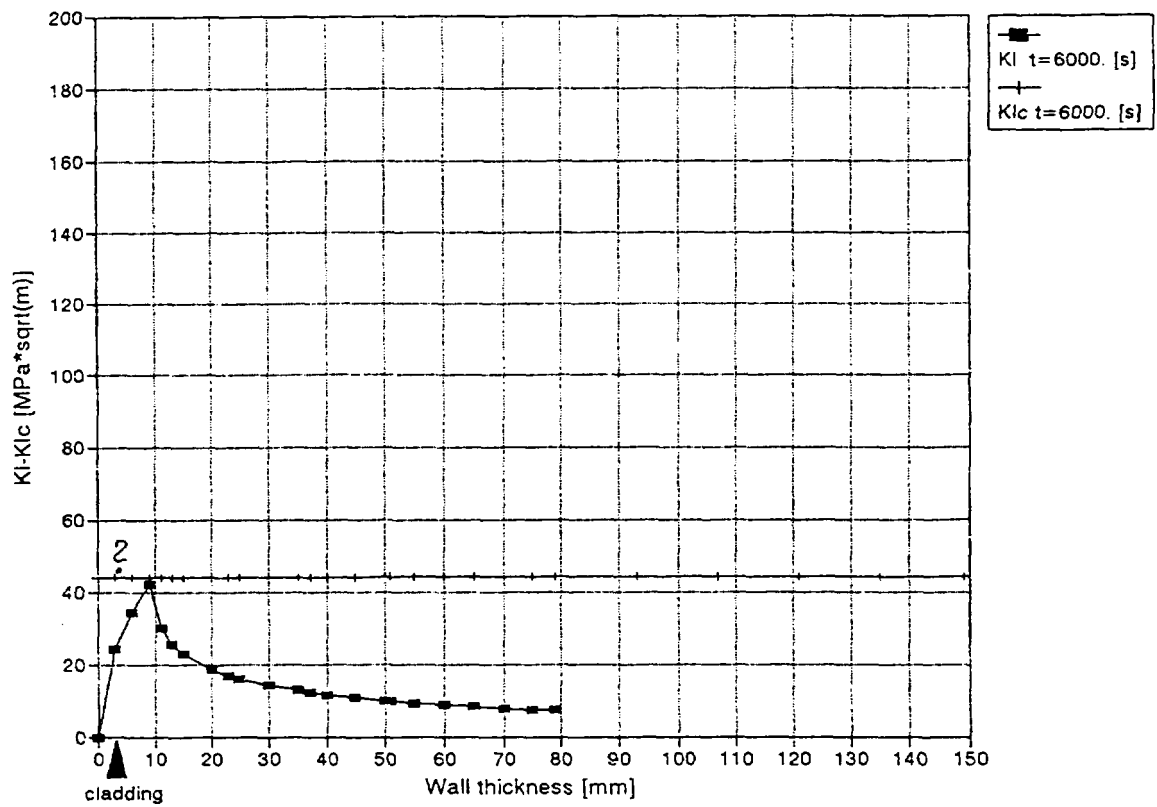


Fig. 19 KI-KIc Distributions in RPV Wall



Conclusions:

Further research is needed:

- a.) to verify recent material models
- b.) to develop more realistic material models, where it is needed.
- c.) study fracture properties of the cladding and crack arrest properties of the 15H2MFA material after irradiation, and enlarging the analysis with crack arrest considerations.

Appendices:

**Chairmen reports, conclusions and recommendations,
meeting programme, list of participants.**

Chairman's reports
Conclusions and recommendations
Meeting programme
List of participants

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Session 1.

5 May, Monday 11.00

International programmes

Chairmen: Carter, R.; Kiss, Z.

The paper **"IAEA Guidelines on the RPV PTS Analysis for WWER NPPs"** was presented by **Mr. Havel, R.** (IAEA);

In 1990 the IAEA initiated in 1990 a programme to assist the countries of central and eastern Europe and the former Soviet Union in evaluating the safety of their first generation WWER-440/230 nuclear power plants. Under this programme, a guidance document was developed to address the evaluation of PTS required to justify RPV integrity for nuclear power plants with WWER type reactors.

The objective of this work was to provide guidance for performing a reasonably bounding plant specific PTS analysis. The guideline provides advice on individual elements of the PTS analysis, such as acceptance criteria, selection and categorization of initiating events, thermal hydraulic analysis, fracture mechanics assessment, evaluation of material properties and neutron flux calculations.

The approach recommends a deterministic analysis of the limiting events using realistic modeling methods. Conservative assumptions are applied to the initial and boundary conditions. RPV integrity is determined based on a factors of safety between the maximum allowable value of critical brittle fracture temperature and its actual RPV material specific value.

The paper **Hurst R. C.; D.McGarry; Wintle J., B. Hemsworth "Status Report on the NESC I. Project"** was presented by Hurst R.C. (JRC/IAM)

In early 1997, a test was conducted at AEA Risley where a spinning cylinder, containing implanted defects, was subjected to a PTS event. The purpose of the project is the understanding and validation of fracture methodologies used by the nuclear industry to assess reactor pressure vessels against PTS. Additionally the experiment can yield valuable information concerning the effectiveness of current non-destructive practices used worldwide.

This paper summarized the current status of NESC 1 Project which simulated a PTS event using a large spinning cylinder. It described how the tests conditions were determined, the predictions of defect behavior by structural analyses. and the development of a method for detecting crack growth. A preliminary assessment of the success of the test was also presented.

It was stated that 1) crack initiation did occur and was detected via output of the strain gauges and 2) a post-test visual examination of the cylinder revealed that a small local rupture of the cladding emanating from an underclad defect. This was an unexpected event.

An evaluation of the results is planned. This will include as a minimum a structural analysis of the event, post-inspection and destructive examination.

Session 2.

5 May. Monday 14.00

Invited lectures 2.

Chairmen: Tendera, P; Czoboly, E.

Four papers were presented in this session. Two of them belongs in the area "research and development", two are rather "plant specific".

The paper **Blauel, J.; Nagel, G. "Pressurized Thermal Shock Evaluation of RPV Stade"** was presented by Mr. Blauel, J. (Germany)

The paper presented described practical example of PTS evaluation on the Stade RPV. The RPV analysis, including brittle fracture analysis, was made in accordance with the KTA rules in Germany.

The authors developed their own formula for K_I computations for different sub-clad and through-clad cracks and for different transitions. This formulae were validated by using FEM models with cracks. Influence of cladding yield stress was included. The depth of postulated cracks was based on in-service inspections nondestructive examinations. (i.e. sensitivity and reliability). It was stated that analyses are cost effective and possible for relevant spectra of defect postulates and LOCA transients including plume cooling.

The paper of **Bishop, B. A. Carter, R. G.; Gamble, R.M. Meyer, T. A. "Alternative Method for Performing PTS Analysis"** was presented by Mr. Carter, R. (USA)

The second paper of this session presented by Mr. Carter describe a new EPRI programme on an alternative method for performing PTS analysis.

The goal of the programme is to simplify probabilistic fracture mechanics analysis in connection with procedure described in US NRC Regulatory Guide 1.154 and to economize all analyses concerning PTS evaluation.

The basic idea of this new approach is, that there is relationship between calculated critical crack depth at which initiation would first occur on the flaw boundary during PTS event.

The alternative fracture mechanics procedure has been benchmarked to the NRC's risk studies. The results show that the technique is consistent and compatible with the results of the NRC studies.

It seems that this method is not generally accepted at present. The EPRI's intention is to qualify the benefit of this methodology.

The paper of **Hong, S. Y.; Jang, C.; Jeong I.S. Jin, T. E. "Plant Specific PTS Analysis of Kori Unit 1"** was presented by Mr. Hong, S. Y. (Korea)

The third paper described plant specific PTS analysis of KORI Unit 1 in Korea. For the operational life extension of NPP, and in the frame of residual life evaluation of the RPV, it was found from a screening analysis, that the reference PTS temperature would exceed screening criteria. The authors

described technical issues related to PTS events and their possible solution in the future. One of the main problems of Kori Unit 1 is radiation embrittlement due to the high content of Cu and Ni in weld.

The paper of **Moinereau, D.; Faidy C.; Valeta, M.P.; Bhandari, S. Guichard, D. "Evaluation of Fracture Mechanics Analyses Used in RPV Integrity Assessment Regarding Brittle Fracture"** was presented by Mr. Moinereau, D. (France)

The last paper of this session described research programmes on the evaluation of fracture mechanics analyses related to risk of brittle fracture within the frame of RPV structural integrity.

An experimental programme had been conducted on clad specimens with sub-clad flaws in order to evaluate different methods of fracture mechanics such as elastic analysis with specific correlation, elastic-plastic analysis and local approach. The final results are in agreement with the testing of specimens.

Within the framework of a cooperative programme (involving EDF, CEA, Framatome and AEA) numerous results obtained by different partners and related to PTS evaluation were compared with a good agreement on main parameters.

Session 3.

5 May, Monday 16.00

Invited lecturer 3.

Chairman : Tuomisto, H.; Fekete, T.

The paper "**Status of Pressure Vessel Embrittlement Study in Japan**" was presented by **Mr. Kataoka, S.** (Japan).

The number of nuclear power plants in service for more than 20 years is increasing in Japan, and for this reason the assurance of the safety and reliability of nuclear power plants is becoming more important. This report outlines the report "Specific Concepts in Dealing with Nuclear Power Plant High Ageing" issued by the Japanese Government in April 1996 and the past achievements and future plans of various verification tests related to irradiation embrittlement of nuclear reactor pressure vessels mainly related to PTS.

The paper "**On the Proper Fracture Toughness Properties to be Used for Pressurized Thermal Shock Evaluations**" was presented by **Mr. Server, W. L.** (USA)

Embrittlement of nuclear RPV steels has been monitored through extensive surveillance programmes conducted routinely for U.S. NPPs. The level of embrittlement is factored into normal operating procedures for heatup and cooldown of the vessel. Additionally, since the early 1980s, PTS has been a

non-design condition that all U.S. plants have had to prove adequate toughness relative to a severe overcooling and pressure-increasing transient event. The regulations in the U.S. were revised in 1985 to include a simple screening criterion which was called the PTS rule (10 CFR 50 rule). This screening criterion is for the value of RT_{pts} which cannot exceed 132 °C for base materials and axial welds and 149 °C for circumferential welds. RT_{pts} is the projected value of the reference temperature, RT_{NDT} , that corresponds to the end-of-license fluence of the vessel. Although these values are termed screening criteria, in practice they are really limits that require serious attention if they are going to be reached.

In the early 1990s, a plant in the U.S. was projected to exceed the screening limits even before the end-of-license based upon very conservative re-evaluation of the degree of toughness degradation. This plant was Yankee-Rowe, one of the earliest power plants built in the U.S. There were several other integrity issues for the Yankee Rowe vessel in addition to the level of the radiation embrittlement. Later studies indicated that the assumptions used by NRC in evaluating the Yankee Rowe embrittlement were very conservative.

The traditional approach in the U.S. for evaluating the PTS has relied upon probabilistic studies in which the toughness has been based upon the data used to generate the lower bound ASME code K_{IC} and K_{IR} curves. The Master Curve approach provides a unique alternative in providing a much better measure of real fracture toughness.

The paper **Popov, A. A.; Rogov, M. F.; Dragunov, U. G.; Parshutin, E. V.:** **"Improvement of Methods to Evaluate Brittle Failure Resistance of the WWER Reactor Pressure Vessels"** was presented by Mr. Popov, A. (Russia)

In the next 5-10 years a number of Russian NPP-s built with WWER RPV will complete their design lifetime. Normative methods to evaluate brittle failure resistance of the reactor pressure vessels used in Russia have been used at the design stage. The evaluation of RPV lifetime on operation stage demands new methods of calculation and new methods for experimental evaluation of brittle fracture resistance degradation. In the presentation an analysis of various methods to determine the critical brittle to ductile transition temperature was made. Critical parameters of toughness used in different countries were analysed. The principal differences of Russian, European Standards and ASTM Standards for determining static crack resistance values were examined and proposals to harmonise Russian and European Standards were presented.

Session 4.

6 May, Tuesday 9.00

PTS analysis

Chairman: Miller, A.; Jánosiné-Biró, Á.

The paper **Petkov, G.; Groudev, P.; Argilov, J. A; "Procedure for Temperature-Stress Fields Calculation of WWER-1000 Primary Circuit in PTS Events"** was presented by Mr. Petkov, G. (Bulgaria)

The first paper from Bulgaria describes a procedure for calculating the temperatures and stresses the WWER 1000 primary circuit, developed jointly by Bulgarian and Brookhaven National Laboratory staff. Initiating events addressed were SLB in the containment and stack open steam dump or SG relief valves. A RECAP/MOD3 model was used for the thermal hydraulic calculation. The RPV and primary circuit temperatures and stresses were calculated by a Fourier series method.

The paper **"Handling Large Primary to Secondary Leakage Opening of the Steam Generator Collector Cover"** was presented by **Mr. Perneczky, L.** (Hungary)

The second paper from Hungary analysed large primary to secondary leakage and including opening of the SG collector cover. The event occurred in the Rovno Unit I in 1982. The work was done for the Hungarian AGNES project and was the first PTS analysis in an Eastern European country. 18 cases were considered, which well covered the full range of PTS events. Three conservative postulated cracks were used. For the SG collector cover opening, the scenarios were a combination of transient and operator error. However, there are very many valves in the WWER-440 primary circuit and present operating procedures give absolute priority to the isolation at breaks. However, the PTS analyses show that this should not be given priority over RPV cold repressurisation. New guidance is needed for SG collector cover opening.

The paper **"Overcooling Transient Selection and Thermal Hydraulic Analysis of Loviisa Assessment"** was presented by **Mr. Tuomisto, H.** (Finland)

The third paper, from Finland, addressed transient selection and thermal hydraulic analyses at Loviisa. Deterministic analyses have been carried out for licensing purposes. An integrated probabilistic PTS study was carried out to give an overview of the severity of all PTS sequences, and to give a quantitative estimate of the importance of the PTS issue. Later sequences including extreme flooding were added to the PTS assessments. It is important to conserve the balance between preventing PTS and core cooling, which may have contradictory requirements. Most recently, in 1996, Loviisa I RPV lower core weld was annealed.

Session 5.

6 May Tuesday 11.00

PTS analysis 2.

Chairmen: Miannay, D.; Uri, G.

The paper **"PWG-3 Activities in the Field of PTS"** was presented by Mr. **Miller, A.**

Miller, A. secretary of the Principal Working Group 3, "Structural components" of the NEA/OECD described the general organisation of the Agency. Then he described the activities in the field of PTS. The main activity was the FALSIRE I and II programmes leaded by GRS and ORNL. In these programmes, simulated experiments were carried out and were analysed at first by several methods and then in phase II by finite element analysis only. The results of Phase I were very difficult to analyse but for Phase II a good structural representation was obtained. The shortcoming was on the material representation. Actually the ICAS programme with only a structural analysis is proceeding and there will be a final conclusion meeting in February, 1998 in Orlando, US.

The paper **Keim, E.; Schöpper, A.; Fricke, S.; "Fracture Mechanics Assessment of Surface and Sub-Surface Cracks in the RPV Under Non-Symmetric Loading"** was presented by Ms. Keim, E.

E. KEIM from SIEMENS described the mechanical analysis of a PTS event in a German Pressure Vessel. After presenting the possible cases of mixing in the cold leg and the formation of plume(s) with or without interaction, she gave the thermal aspect with the discontinuous variation of the heat transfer coefficient for calculating the thermal stresses. Then she presented the elastic and the elasto-plastic fracture analyses for a surface or subsurface semi-elliptical reference defect of 10 mm height. It is shown, that the subsurface flaw severity is covered by the surface flaw of same depth and that the elastic analysis is conservative compared to the elasto-plastic analysis. The analysis with plume formation gives result of magnitude half of magnitude of a symmetric analysis. However the mechanical resistance of the cladding, hard or soft, was not investigated.

Session 6.

6 May Tuesday 12.45

Database use.

Chairman: Pugh, C.; Elter, J.

The paper **Beghini, M. D'Auria, F.; Galassi G. M.; Vitale, E. "Database for Evaluating the PTS Potential in the WWER-1000 Reactor"** was presented by D' Auria, F. (Italy)

Dr. D'Auria illustrated PTS evaluations where thermal hydraulic results were employed as input for parametric fracture mechanics analyses.

The paper **Davies L. M.; Gillemot, F.; Yanko, L.; Lyssakov, V. "The IRPVM-Db database"** (UK, Russia, Hungary, IAEA) was presented by Mr. Yanko, L.;

Mr. Yanko described the evolving IAEA International RPV Database (IRPVMDb) relative to each of these aspects. The agency has established a common format for participating states to supply data and supporting information relative to irradiation effects on material properties. Currently, ten states have agreed to supply data and others are expected to join the programme soon. This database will be placed on CD-ROM and made available to participants

The paper **Davies, L. M. Gillemot, F. Lyssakov, V. "PTS and the IAEA Database"** (UK, Hungary, IAEA) was presented by Mr. Davies L.M.;

Mr. Davies emphasized the importance of selecting appropriate correlation parameters when developing trend curves from databases, e. g., irradiation effects. Benefits from the large IAEA database apply to utilities, design authorities, safety authorities, and researchers.

The paper **Gillemot, F. Davies, L.M. "Examples of the Use of the Database"** (Hungary, UK) was presented by Mr. Gillemot

Mr. Gillemot illustrated some ways that large databases (e.g. IRPVMDb) are useful, with a particular emphasis on how can one develop data trend curves for nationally-specific conditions. Additionally, large databases were illustrated as essential to performing meaningful probabilistic analyses and to assessing effects of parameters, such as inhomogeneity and thermal ageing. PTS analyses can be economised by using databases.

Session 7.

7 May Wednesday 9.00

Material degradation, PTS modelling.

Chairman Chomik, E.; Gillemot, F.

The paper **Nahm, S. H.; Kim; A.; Yu, K.M.; Kim, S.C ; “Toughness degradation evaluation of Low Alloyed Steels by Electrical Resistivity”** was presented by Mr. Nahm S. H. (Korea)

The paper introduced the results obtained from electrical resistivity measurement on Cr-Mo-V steels at different stages of ageing. The correlation between ageing time , FATT and electrical resistivity was presented. The electrical resistivity measurement seems a promising NDT method for evaluating toughness change.

The paper **Pirfo, S.; Martins P.G. Terra J.L. Lorenzo R.; Tanius R.M. (Brazil) “Elaboration and verification of a PTS model”** was presented by Ms. S. Pirfo (Brazil)

CDTN (Brazil) is designing a model for PTS research. The purpose of the model is the preparation of PTS analysis of Angra NPP, verification of the codes used, and an evaluation of the toughness degradation caused by severe thermal cycles. During the design stage the main requirements were: similarity to Angra II RPV, easily measurable temperature and stress distribution, and use of steel produced by a Brazilian foundry. The paper shows that the optimum model wall thickness is in the range of 80-100 mm.

The paper **Roos, E.; Eisele, U.; Strumpfrock, L.; “Transferability of Results of PTS Experiments to the Integrity Assessment of Reactor Pressure Vessels”** was donated by MPA Stuttgart.

Unfortunately the authors couldn't participate on the meeting, but they provided the paper for the meeting proceedings.

Session 8.

7 May Wednesday 11.00

WWER reactor analysis 2

Chairman: Ianko, L.; Oszwald, F.

The paper **Sievers, J.; Liu, X.; “Fracture Analysis of WWER Reactor Pressure Vessels”** was presented by Mr. Liu, X. (Germany).

The paper compared results of different methods of reactor pressure vessel integrity analysis.

The methodology of fracture assessment based on finite elements (FE) calculations was described, comparison with simplified methods and results of international comparative study FALSIRE shown.

Also, some results of fracture analysis of WWER RPV-s with postulated cracks under different loading transients were presented.

The paper **Elter, J.; Fekete, T. Gillemot, F.; Oszwald, F.; Maróti L.; Rátkay S. "PTS assessment - the basis of Life Time Evaluation of NPP Paks"** was presented by Mr. Gillemot, F. (Hungary).

Conservative PTS calculations were performed for NPP Paks . To evaluate real life time and to develop a life management programme further studies and research efforts are necessary.

The significance of some of the plant experience was stressed:

- material properties distribution in the RPV wall
- cladding effect
- use of extended results
- operational changes during lifetime

The paper **"Assessment of the Zaporozhye NPP Unit 1 Reactor Vessel Safety"** was presented by Mr. Popov V.; and Mr. Podkopaev V.:(Ukraine)

The paper discussed results from the study of Zaporozhye Unit 1 reactor pressure vessel integrity assessment and consequences of an accident which occurred in April 1995 and which involved a real severe transient.

The analysis was performed for postulated defects and the following methods were applied

- VISA-II (deterministic part)
- PNAEG -7-002-86
- FEM

The results confirmed that the transient had no significant impact on the reactor pressure vessel integrity.

Session 9.

7 May Wednesday 14.00

WWER reactor analysis 3

Chairman: Pirfo, S.; Gillemot, L.

The paper **"Russian practice of RPV integrity assessment under PTS conditions"** was presented by **Mr. V. Piminov** (Russia).

In studies on the Novovoronezh 3 & 4 vessels the calculations of the permissible ductile-to-brittle transition temperature of weld material were carried out assuming the worst conditions.

The primary leaks, during isolation need a special procedure. Special measures have to be taken to prevent cold overpressure of the reactor vessel.

To provide the safe operation of the reactor vessels during the designed service life it is necessary to non-destructively examine the reactor vessel for

detecting the small surface cracks and subsurface defects in the vessel cylinder as well as in the weldments.

The results of GIDROPRESS on the Kozloduy 1 vessel were compared to independent calculations using other approaches (carried out by Siemens and Westinghouse) and all are in good agreement.

The paper **Hrazsky, M.; Mikus, M.; Hermansky "WWER 440 integrity assessment with respect to PTS events"** was presented by Mr. Hrázský, M. (Slovak Republic).

This report summarizes the present state of the art in Slovakia in WWER 440 RPV integrity assessment with respect to PTS events. Several PTS analyses are reported briefly in the paper. They confirmed the necessity of developing new more sophisticated procedures again. Such methodology should be based on the IAEA Guidance.

The paper **"The cladding effect at PTS assessment of WWER-440 RPV-s"** was presented by **Mr. T. Fekete** (Hungary).

Analytical Calculation for Integrity of Beltline of Reactor Pressure Vessel (ACIB-RPV) computer programme was used for the analysis of the stability of defects in reactor vessel walls during PTS. To verify the use of the ACIB-RPV programme a comparison was made with the IVOFEM code and with the finite element calculation results made by ADINA. The results were in good agreement (the scatter is less than 4 %).

The paper analyses the effect of the cladding with different material properties.

Further activity is needed to verify the recent material models, to develop new ones and to analyse the effect of the cladding especially in the case of irradiated steels.

Conclusions and recommendations from discussion by participants

Chairmen: Server, W.; Miannay, D.; Blauel, J.G.; & Pugh, C.

The discussion was focused by the chairmen in two sessions under the heading. Measures to increase safety relative to PTS and the following specific topics were covered

On-line monitoring

Elimination of transients

Modification of plant/system

heat ECCS

replace high head (SI) pumps

change angle of injection

use plant experience

annealing of RPV

flux reduction

Improve analysis by better knowledge of

defect status

thermohydraulics

material data base

crack tip loading conditions

assessment methodologies

The key recommendations from the discussion were:

1. The ability to monitor PTS events through additional monitoring methods does not seem practical or necessary. Most plants have adequate instrumentation such that any PTS transient can be described in detail to perform necessary PTS evaluations.
2. The ability to eliminate transients seems to be a politically-charged issue, and only if a transient can be shown to have an extremely low probability of occurrence, it generally must be considered individually or preferably by a bounding transient. But all possible measures should be considered to minimize the severity and frequency of possible transients.
3. Many, if not all, of the plants with a real PTS concern are older plants; when considering changes or modifications of the plant or by systems reduce PTS susceptibility economic arguments can become crucial; for heating the ECCS water the costs of possible system reanalysis or other issues (such as corrosion) must be carefully considered.
4. Thermal annealing is a valid mitigating solution, but it should be considered as a last resort measure, even though a favorable cost-benefit can generally be shown.
5. Flux homogenisation and reduction, through core fuel management programmes

should be started early in the life of the vessel.

6. In-Service Inspection (ISI) could have a significant impact on PTS analyses if the results from ongoing inspection qualification processes are married with results from actual reactor pressure vessels (with confirmation of real cracks through destructive examination of the regions where NDE indications were found). The benefits can be significant for both deterministic and probabilistic analyses, fracture mechanics parameter studies may help to locate and establish what size of crack can become critical.
7. The development of improved and verified codes and modelling for thermohydraulic analyses (including 3D and two phase flow) is supported in order to provide input data for the fracture mechanics assessment which is adequate to its degree of sophistication.
8. Direct measurements of fracture toughness for the original material is preferred to any indirect method (based e.g. on Pellini and charpy tests); but this requires further development of small scale specimen methodologies. Master curve and local approach were judged to be promising approach.
9. In the case of small safety margins for a specific transient then refined (but more costly) analyses of the crack tip loading behavior may have to be undertaken and taking into account:
 - unloading after plastic deformation (WPS)
 - stress concentration areas such as nozzles
 - variation of constraint
 - influence of cladding
10. The role of cladding in evaluating PTS events has been debated for many years. It is generally agreed that cladding procedures high tensile stresses on the inside surface on the RPV during a PTS event. This stress must be considered in the fracture mechanics calculations when determining potential for crack initiation. There is some information which suggests that cladding can retard crack initiation while other data suggest that cladding contributes to crack initiation. There is limited information concerning the mechanical properties (e.g. Irradiated toughness) of cladding. Also the effect cladding with regard to crack initiation and propagation is not well understood. It is recommended that the following research be pursued for the purposes of better understanding the effects of cladding.
 - Irradiated toughness of the different types of cladding used in commercial RPV-S.
 - Crack initiation and propagation of surface, subsurface (Clad/base metal interface) and embedded flaws
 - Through wall stress distribution as a function of crack size
 - Determination of applied stress intensity as a function of crack size
11. PTS analyses are generally acceptable using initiation criteria only. However, as a second line of defense, initiation/arrest analyses can be performed.

12. Initiation of a postulated crack on the upper shelf for a low toughness material (e.g., one with an upper shelf Charpy toughness less than 68 J or a measured K_{Ic} of less than 120 MPam^{1/2}) is an important consideration for PTS that is often neglected. Linde 80 weld metals and some older A302B plates are materials known to have low upper shelf toughness, and others may exist. The transients that can be a concern in the upper shelf region are those that often are not important for transition temperature PTS calculations, such as a main steam line break (MSLB). Additionally, a relatively large flaw is required for initiation in the upper shelf region, and if adequate credit from ISI can be utilized, the problem is of no consequence. The upper shelf evaluations for low toughness materials may require an elastic-plastic J-R analysis to confirm acceptability.

Other recommendations

The following four recommendations were derived from the discussion of papers:

- I. The IAEA should consider detailing any significant transients on actual plant for provision to national representatives at the regular meeting of the IWG LMNPP for further distribution by them to interested parties in their own country - for use in 'case' calculations and as an adjunct for improved safety consideration on an international basis.
- II.) The NDT measurement of degradation is to be encouraged and should be included as a session in future Specialist's Meetings in this and allied areas.
- III.) Several model test and case studies are proceeding at present time and an allowance should be made for a session to present and discuss results at a future SM.
- IV.) Consideration should be given by the IWG-LMNPP for a future SM on PTS in 2-3 years time.

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IAEA Specialist's Meeting on
PRESSURIZED THERMAL SHOCK
Esztergom, Hungary, 5-8 May

Program

Meeting chairman: Davies L. M.
Scientific secretary: Lyssakov V.

Opening Section. 5 May, Monday 10.00 am.

Chairman: Davies, L. M.; Vöröss, L.

10.00 -10.40 Opening Ceremony (Gillemot F.; Davies M. L.; Vöröss L.; Kiss, Z.; Lyssakov, V.)

10.40-11.00 Break

Section 1. 5 May, Monday 11.00

International programmes

Chairman: Carter, R.; Kiss, Z.

- | | | | |
|-------|---|--------------|---|
| 11.00 | Havel, R. | IAEA | IAEA Guidelines on the RPV PTS Analysis for WWER NPPs |
| 11.45 | Hurst R. C.; McGarry, D ; Wintle, B.; Hemsworth, J. B.; | JRC/IAM (EC) | Status Report on the NESC I. Project |

12.30-14.00 Lunch Break

Section 2. 5 May. Monday 14.00

Invited lectures 1.

Chairman: Tendera, P.; Czoboly, E.

- | | | | |
|-------|--|---------|---|
| 14.00 | Blauel, J.; Nagel, G.; Hertlein, D.; Hodulak, L.; Siegele, D. | Germany | Pressurized Thermal Shock Evaluation of RPV Stade |
| 14.30 | Bishop, B.A.; Carter, R. G.; Gamble, R.M.; Meyer, T.A. | USA | Alternative Method for Performing PTS Analysis |
| 15.00 | Hong, S. Y.; Jang, C.; Jeong, I.S. Jin, T.E. | Korea | Plant Specific PTS Analysis of Kori Unit 1 |
| 15.30 | Moinereau, D.; Faigy, C.; Valeta, M.P.; Bhandari, S.; Guichard, D. | France | Evaluation of Fracture Mechanics Analyses Used in RPV Integrity Assessment Regarding Brittle Fracture |

16.00-16.30 Break

Section 3. 5 May, Monday 16.00

Invited lecturer 2.

Chairman : Tuomisto, H.; Fekete, T.

- | | | | |
|-------|---|--------|---|
| 16.30 | Kataoka, S. | Japan | Status of Pressure Vessel Embrittlement Study in Japanese LWR-s |
| 17.00 | Server, W. | USA | On the Proper Fracture Toughness Properties to be used for Pressurized Thermal Shock Evaluation |
| 17.30 | Popov, A. A.; Rogov, M.F.; Dragunov, Yu.; Parshutin E.; | Russia | Improvement of Assessment Methods of Brittle Fracture Resistance of WWER Structural Materials |

Reception on the behalf of the IAEA. 5 May, Monday 18.30

Session 4. 6 May Tuesday 9.00**PTS analysis 1.**

Chairman: Miller, A.; Jánosiné-Biró, Á.

9.00	Petkov, G.; Groudev, P.; Argilov, J.	Bulgaria	A Procedure for Temperature-Stress Fields Calculation of WWER-1000 Primary Circuit in PTS Event
9.30	Perneckzy, L.	Hungary	Handling Large Primary to Secondary Leakage: Opening of the Steam Generator Collector Cover
10.00	Tuomisto, H.	Finland	Overcooling Transient Selection and Thermal Hydraulic Analysis of Loviisa PTS Assessments

10.30-10.45 Break

Session 5. 6 May Tuesday 11.00**PTS analysis 2.**

Chairman: Miannay, D.; Uri, G.

10.45	Miller, A.	OECD/NEA	PWG-3 Activities in the Field of PTS
11.15	Keim, E.; Schöpper, A.; Fricke, S.;	Germany	Fracture Mechanics Assessment of Surface and Sub-Surface Cracks in the RPV Under Non-Symmetric Loading

11.45-12.30 Lunch break

Session 6. 6 May Tuesday 12.45**Database use**

Chairman: Pugh, C.; Elter, J.

12.45	Beghini, M.; D'Auria, F.; Galassi G. M.; Vitale, E.	Italy	Evaluation of the PTS Potential in a WWER-1000 Reactor, Following a Steam Line Break
13.15	Davies L. M.; Gillemot, F.; Yanko, L.; Lyssakov, V.	UK, Russia, Hungary, IAEA	The IRPVM-Db database
13.45	Davies, L. M.; Gillemot, F.; Lyssakov, V.	UK Hungary IAEA	PTS and the IAEA Database
14.15	Gillemot, F.; Davies, L. M.	Hungary	Examples of the Use of the Database

15.15 Visit the Esztergom Basilika and the Catholic Treasury.

Session 7. 7 May Wednesday 9.30**Material degradation, PTS modelling.**

Chairman Chomik, E.; Gillemot, F.

9.30	Nahm, S. H.; Kim, A.; Yu, K. M.; Kim, S. C.	Korea	Toughness Degradation Evaluation of Low Alloyed Steels by Electrical Resistivity
10.00	Pirfo, S.; Martins P. G.; Terra, J. L.; Lorenzo R.; Mansur, T. .M.	Brazil	Elaboration and Verification of a PTS model

10.30-11.00 Break

Session 8. 7 May Wednesday 11.00

WWER reactor analysis 1

Chairman: Ianko, L.; Oszwald, F.

11.00	Liu, X.; Sievers, J.	Germany	Fracture Analyses of WWER Reactor Pressure Vessels
11.30	Elter, J.; Fekete, T.; Gillemot, F.; Oszwald, F.; Maróthi L.; Rátkay S.	Hungary	PTS Assessment -the basis of Life Time Evaluation at NPP Paks
12.00	Podkopaev, V.; Popov, V.	Ukraine	Assessment of the Zaporozhye NPP Unit I Reactor Vessel Safety

Lunch break 12.30-14.00

Session 9. 7 May Wednesday 14.00

WWER reactor analysis 2.

Chairman: Pirfo, S.; Gillemot, L.

14.00	Piminov, V. ; Dragunov, Yu.; Kostyrkin, S.; Akhbachev, L.;	Russia	Russian Practice of RPV Integrity Assessment under PTS Conditions
14.30	Hrázský, M.; Mikula, M.; Hermanský, P.	Slovakia	WWER 440 Integrity Assessment with Respect to PTS Events
15.00	Fekete, T.	Hungary	The Cladding Effect at PTS Assessment of WWER-440 RPV-s

Break 15.30-16.00

Round table discussion. 7 May Wednesday 16.00-17.30

Chairman: Server, W.; Miannay, D.

PTS Monitoring systems, methods for safety level increase at PTS.

Farewell party 7. May 18.00

General discussion 8 May. Thursday. 9.00-10.00

Chairman: Blauel, J.; Pugh, C.

Break 10.00-10.30

Conclusions and Recommendations. 8 May. Thursday. 10.30-11.30

Chairman: Davies, L. M.

Adjourn. 8 May. Thursday. 11.30

12.00 Lunch

13.15 Bus to Budapest (arriving Ferihegy airport about 15.00)

Visit NPP Paks. 9. May. 7.30-17.00

(General visit, training centre, simulator, hot lab.)

Participans List

Title of meeting: **IAEA Specialist's Meeting on "Pressurized Thermal Shock"**
 Title of meeting: **5 - 8 May, 1997**
 Place of the meeting: **Esztergom, Hungary**

Scientific Secretary: **V. Lyssakov**

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