IAEA-TECDOC-1556

Assessment and Management of Ageing of Major Nuclear Power Plant Components Important to Safety:

PWR Pressure Vessels

2007 Update

IAEA
International Atomic Energy Agency

June 2007

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FOREWORD

At present, there are over four hundred operational nuclear power plants (NPPs) in IAEA Member States. Operating experience has shown that effective control of the ageing degradation of the major NPP components (e.g. caused by unanticipated phenomena and by operating, maintenance or manufacturing errors) is one of the most important issues for plant safety and also plant life. Ageing in these NPPs must be therefore effectively managed to ensure the availability of design functions throughout the plant service life. From the safety perspective, this means controlling within acceptable limits the ageing degradation and wear-out of plant components important to safety so that adequate safety margins remain, i.e. integrity and functional capability in excess of normal operating requirements.

IAEA-TECDOC-1120 documented ageing assessment and management practices for pressurized water reactor (PWR) reactor pressure vessels (RPVs) that were current at the time of its finalization in 1997–1998. Safety significant operating events have occurred since the finalization of the TECDOC, e.g. primary water stress corrosion cracking (PWSCC) of Alloy 600 control rod drive mechanism (CRDM) penetrations and boric acid corrosion/wastage of RPV heads, which threatened the integrity of the RPV heads. These events led to new ageing management actions by both NPP operators and regulators. Therefore it was recognized that IAEA-TECDOC-1120 should be updated by incorporating those new events and their countermeasures.

The objective of this report is to update IAEA-TECDOC-1120 in order to provide current ageing management guidance for PWR RPVs to all involved in the operation and regulation of PWRs and thus to help ensure PWR RPV integrity in IAEA Member States throughout their entire service life.

The IAEA officer responsible for this publication was T. Inagaki of the Division of Nuclear Installation Safety.

CONTENTS

1. INTRODUCTION

1.1. BACKGROUND

IAEA-TECDOC-1120 was published in 1998 for ageing assessment and management practice for PWR RPV materials. It was concluded that the safety significance of primary water stress corrosion cracking (PWSCC) is limited and leakage of the primary coolant from a through-wall crack is unlikely, as no crack had been found in the Alloy 80 and 182 welding material to date.

However a discovery was made of near through-wall corrosion of the RPV closure head at the Davis-Besse Nuclear Power Station, pressurized water reactor (PWR, thermal power output of 2772 Megawatts) in March 2002. Although no loss of coolant occurred and the reactor core always remained fully covered and cooled, this incident represented a significant degradation of the nuclear safety margin at the facility.

Based on data today available and information related to PWSCC of Alloy 600 control rod drive mechanisms and boric acid wastage of carbon steel RPV heads, the statements of IAEA-TECDOC-1120 are no longer valid.

Basic requirements on NPP activities relevant to the management of ageing (maintenance, testing, examination and inspection of system, structure and component (SSC)) have also been updated and included in the IAEA Safety Requirements on the Safety of Nuclear Power Plants: Operation [1] and associated Safety Guides on maintenance, surveillance and in-service inspection [2]. In addition the IAEA is preparing a new Safety Guide on ageing management which will provide key recommendations on managing ageing of SSCs important to safety.

1.2. OBJECTIVE

The objective of this report is to update IAEA-TECDOC-1120 in order to provide current ageing management guidance for PWR RPVs to all involved in the operation and regulation of PWRs.

IAEA-TECDOC-1120 is superseded and replaced with this report.

1.3. SCOPE

This report provides the technical basis for managing the ageing of the PWR and pressurized heavy water RPVs to ensure that the required safety and operational margins are maintained throughout the plant service life. The scope of the report includes the following RPV components: vessel shell and flanges, structural weldments, closure studs, nozzles, penetrations and top and bottom closure heads. The scope of this report does not treat RPV internals, the control rod drive mechanisms (CRDMs), or the primary boundary piping used in PWRs. All the various sizes and types of PWR pressure vessels are covered by this report including the WWER (Vodo-Vodianyi Energeticheskii Reactor) plants built in Russia and elsewhere. Boiling water reactor (BWR) pressure vessels and Canadian deuterium-uranium (CANDU) pressure tubes and calandria are covered in separate companion reports.

This scope is same as the previous IAEA-TECDOC-1120 published in 1998.

1.4. STRUCTURE

The designs, materials of construction and physical features of the various PWR pressure vessels are described in Section 2. The codes, regulations and guides used in a number of countries to design RPVs are summarized in Section 3. Section 4 identifies the dominant ageing mechanisms, sites, consequences and operating experience. Section 5 addresses the application of various inspection technologies to assess the condition of the RPV. Section 6 gives the current practices and data required in assessing degradation of an RPV. Section 7 describes operational methods used to manage ageing mechanisms (i.e. to minimize the rate of degradation) and maintenance methods used to manage ageing effects (i.e. to correct in a good time frame unacceptable degradation). Section 8 describes an RPV ageing management programme utilizing a systematic ageing management process.

This report retains the section numbering of IAEA-TECDOC-1120 in order to facilitate cross-referencing to the original publication.

2. DESCRIPTION OF REACTOR PRESSURE VESSEL

This section provides a description of the PWR RPVs and includes design features, applicable material specifications and differences amongst the various RPV components.

Western type PWR RPVs were designed by Babcock & Wilcox (B&W) Company, Combustion Engineering, Inc., Framatome, Mitsubishi Heavy Industries, Ltd, Siemens/KWU, Doo San Heavy Industries, and Westinghouse. The RPVs were fabricated by B&W Company, Chicago Bridge and Iron Company, Combustion Engineering, Inc., Creusot-Loire, Klockner, Rotterdam Dry Dock Company, MAN GHH, Mitsubishi Heavy Industries, Ltd, Doo San and Udcomb.

The water moderated, water cooled energy reactor (WWER) RPVs were designed by OKB Gidropress, the general designer for all NPPs in the former Soviet Union and the Community for Mutual Economical Assistance (CMEA) countries. Some small modifications were made in the Czech designs by SKODA Co. The WWER plants were built in two sizes: the WWER-440s which are 440 MWe plants and the WWER-1000s which are 1000 MWe plants. There are two different design types for each size: the WWER-440 Type V-230, the WWER-440 Type V-213, the WWER-1000 Type V-302, the WWER-1000 Type V-320 and WWER-1000 Type V-392. The Type V-230s were built first and the V-392s were built last. The WWER-440 RPVs are similar to the WWER-1000 RPVs; the differences in the two designs for the two plant sizes are mainly in the safety systems. There are only two WWER-1000 Type V-302 pressure vessels, so only WWER-1000 Type V-320 information is presented in this report. The WWER pressure vessels were manufactured at three plants, the Izhora Plant near Saint Petersburg (Russia), the Atommash Plant on the Volga (Russia) and the SKODA Nuclear Machinery Plant in the Czech Republic.

2.1. RPV DESIGN FEATURES

2.1.1. Western PWR pressure vessels

A Westinghouse designed RPV is shown in Fig. 1. This vessel is fairly typical of the reactor vessels used in all the so-called western designed RPVs. However, there are significant differences in size, nozzle designs, penetration designs and other details among the various suppliers. The RPV is cylindrical with a hemispherical bottom head and a flanged and gasketed upper head. The bottom head is welded to the cylindrical shell while the top head is bolted to the cylindrical shell via the flanges. The cylindrical shell course may or may not utilize longitudinal weld seams in addition to the girth (circumferential) weld seams. The body of the vessel is of low-alloy carbon steel. To minimize corrosion, the inside surfaces in contact with the coolant are clad with a minimum of some 3 to 10 mm of austenitic stainless steel.

Numerous inlet and outlet nozzles, as well as control rod drive tubes and instrumentation and safety injection nozzles penetrate the cylindrical shell. The number of inlet and outlet nozzles is a function of the number of loops or steam generators. For the majority of operating NPPs, the nozzles are set-in nozzles. However, there are a number of operating NPPs with RPVs with set-on nozzles. A set-in nozzle has the flange set into the vessel wall, a set-on nozzle has the flange placed on the vessel wall surface as shown in Fig. 2.

The PWR pressure vessel design pressure is 17.24 MPa (2,500 psi) and the operating pressure is 15.51 MPa (2,250 psi). The usual vessel preservice hydrostatic pressure is 21.55 MPa

(1.25 × design pressure). The PWR pressure vessel design temperature is 343°C (650°F) while the operating temperature is typically 280 to 325°C (540 to 620°F).

An ABB-CE (formally Combustion Engineering) designed RPV is shown in Fig. 3. The ABB-CE design is somewhat different from some other western designed RPVs in that there are a relatively large number of penetrations which are made from Alloy 600. As will be discussed in a later section, reactor penetrations fabricated from Alloy 600 can be of concern to ageing management of the RPV.

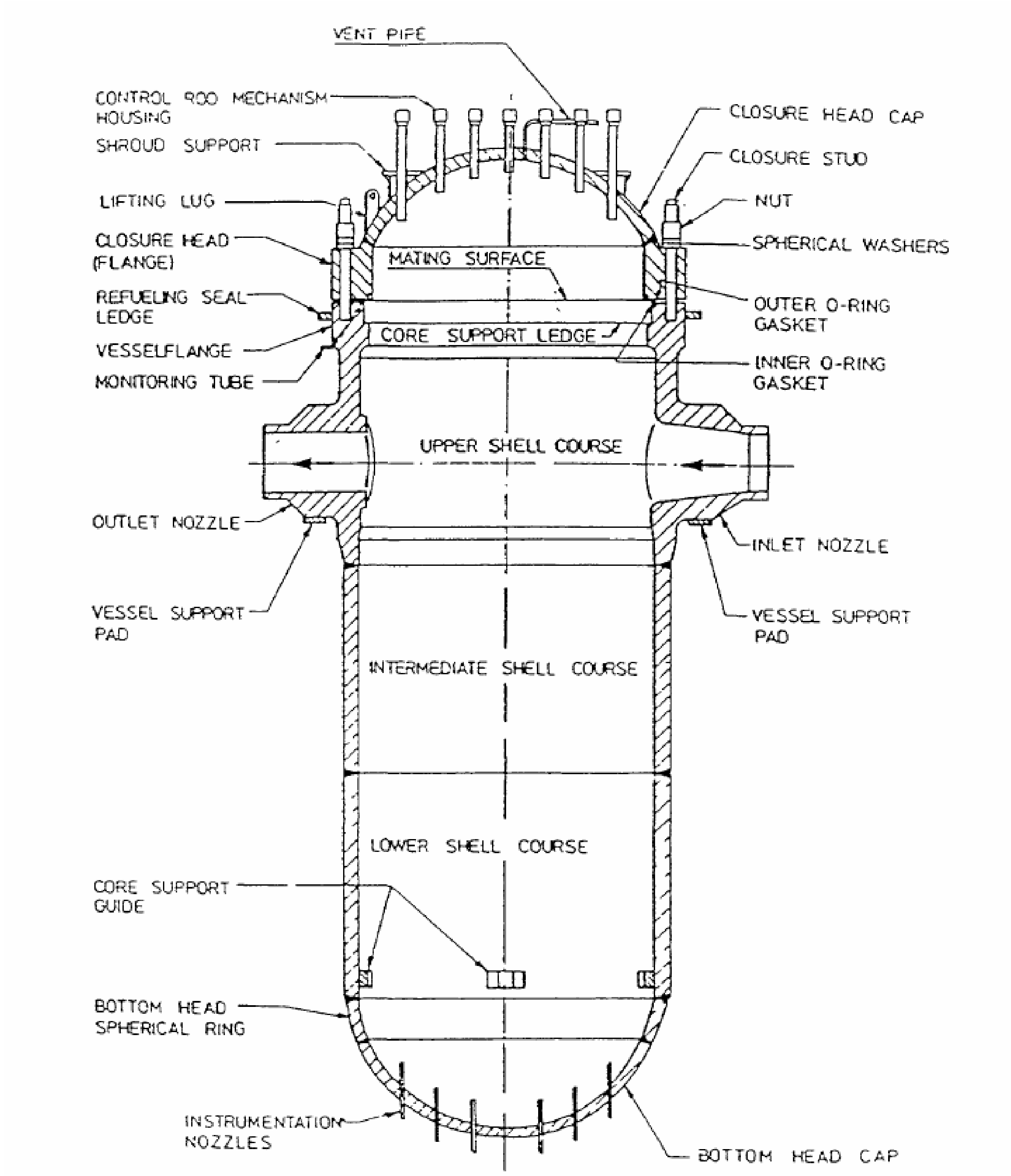


Fig. 1. A typical Westinghouse reactor pressure vessel.

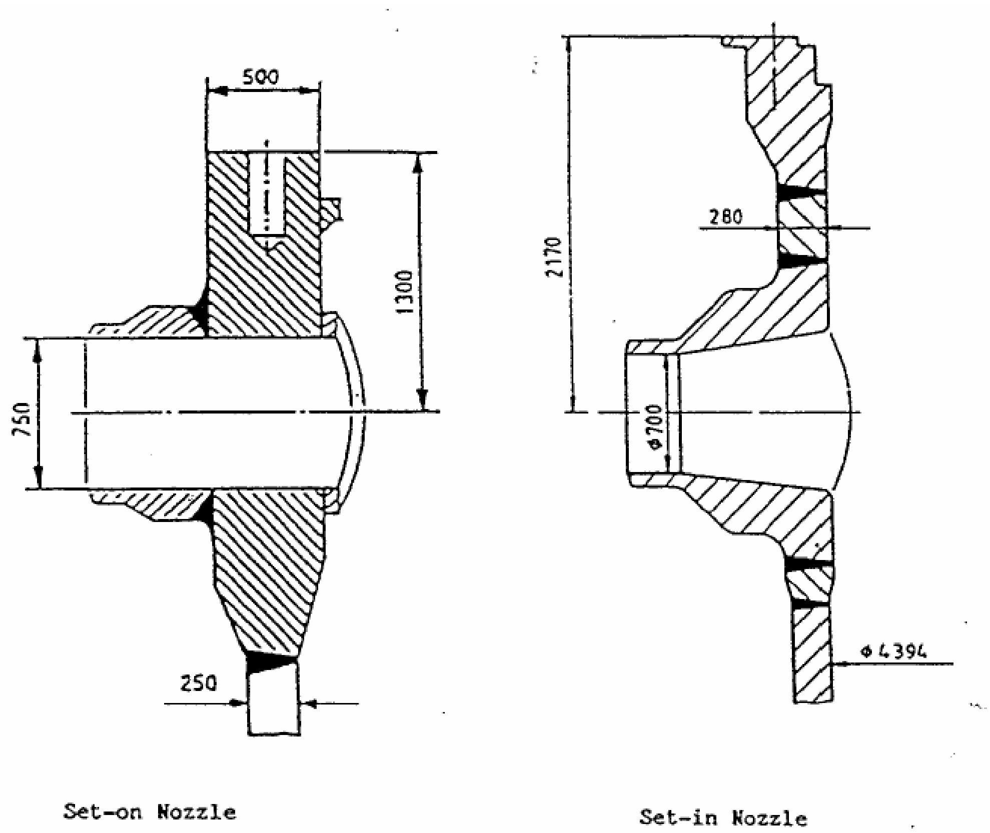


Fig. 2. Sketches of typical set-on and set-in nozzles used in reactor pressure vessels.

A Siemens (KWU) designed RPV is shown in Fig. 4. The features of the Siemens RPV which significantly differ from other western design are as follows:

- set-on inlet and outlet nozzles
- reinforcement of the flange portion
- no nozzles or guide tubes within the lower part of the RPV (no risk of breaks and leaks below the loops)
- one piece upper part section
- special screwed design for the control rod drive and instrumentation nozzle penetrations made from co-extruded pipe.

The French RPVs are designed by Framatome and manufactured by Creusot-Loire. Sketches of the French 3-loop (900 MWe) and 4-loop (1450 MWe) RPVs are presented in Fig. 5 and the major characteristics of the RPVs used for the 4-loop N4 plants are listed in Table 1. The French RPVs are constructed with ring sections and, therefore, there are no longitudinal (vertical) welds. Generally, the core beltline region consists of two parts, although the Sizewell B vessel has only one ring and some old vessels have three rings in the beltline region. Six or eight set-in nozzles are used along with stainless steel safe ends connected to the nozzles with dissimilar metal welds. The design pressure is 17.2 MPa, the operating pressure is 15.5 MPa, the initial pre-service hydrostatic pressure is 22.4 MPa (1.33 × design pressure) and the design life is 40 years.

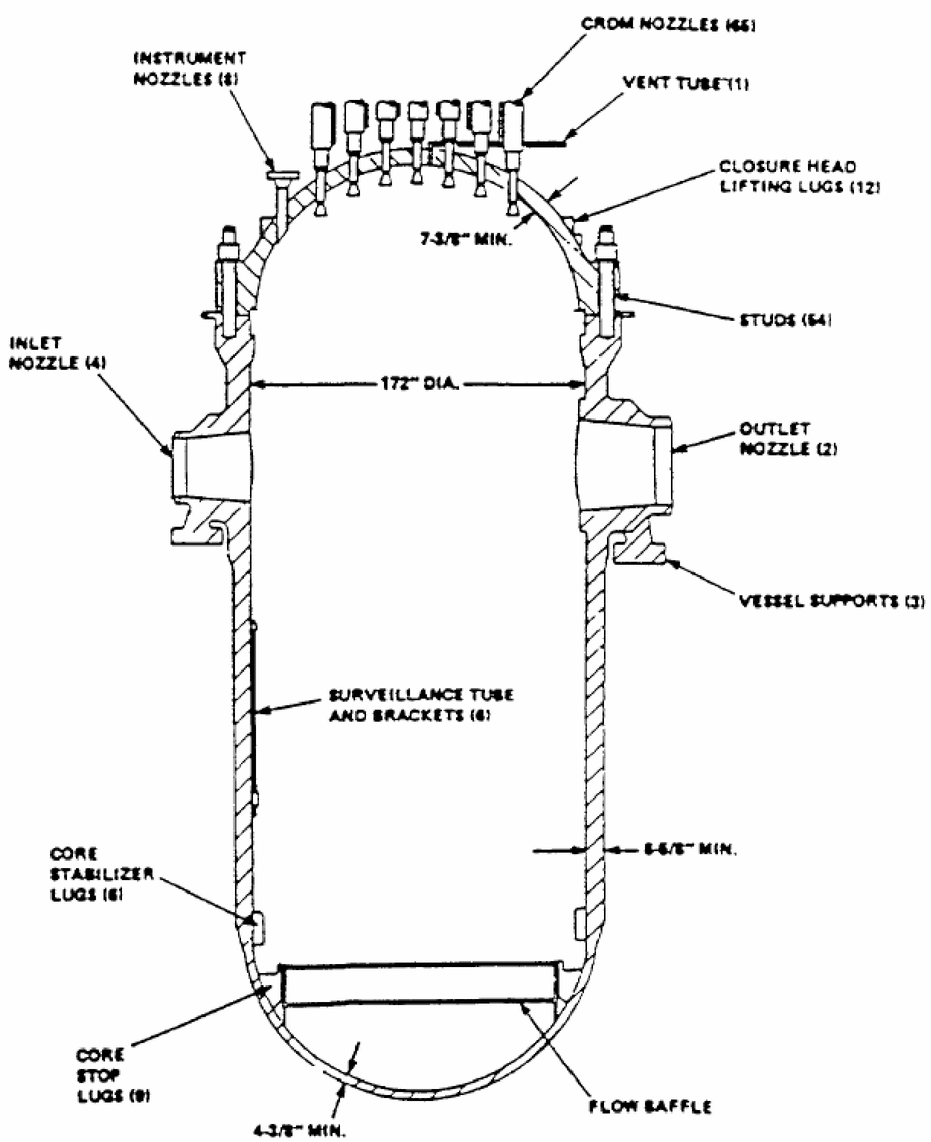


Fig. 3. A typical ABB-CE reactor pressure vessel.

2.1.2. WWER pressure vessels

The WWER pressure vessels consist of the vessel itself, vessel head, support ring, thrust ring, closure flange, sealing joint and surveillance specimens (the latter were not in the WWER/V-230 type of reactors). The RPVs belong to the "normal operation system", seismic Class I and are designed for:

- safe and reliable operation for over 40 years,
- operation without damage for not less than 24,000 hours (damage in this sense includes leaks in the bolted joints and the threaded control rod drive nozzle joints, thread surface damage, etc.),

- non-destructive testing (NDT) of the base and weld metal and decontamination of the internal surfaces,
- materials properties degradation due to radiation and thermal ageing monitoring (not in the case of WWER/V-230 type of reactors),
- and all operational, thermal and seismic loadings.

The WWER RPVs have some significant features that are different from the western designs. A sketch of a typical WWER pressure vessel is shown in Fig. 6 and the main design parameters and materials are listed in Tables 2a to 2c.

- The WWER RPVs (as well as all other components) must be transportable by land, i.e. by train and/or by road. This requirement has some very important consequences on vessel design, such as a smaller pressure vessel diameter, which results in a smaller water gap thickness and thus a higher neutron flux on the reactor vessel wall surrounding the core and, therefore, requirements for materials with high resistance against radiation embrittlement.

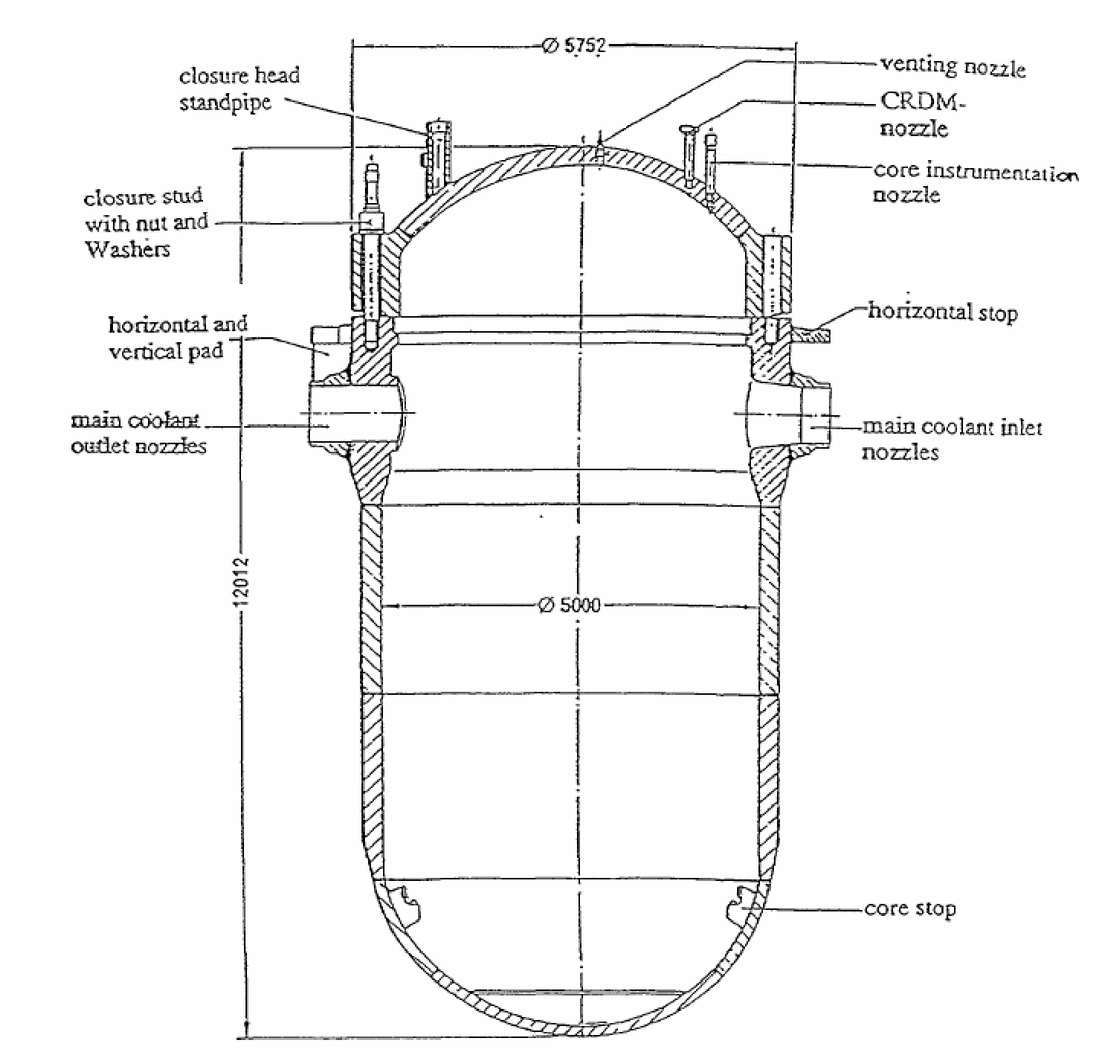


Fig. 4. A typical Siemens/KWU reactor pressure vessel for a 1300 MWe plant.

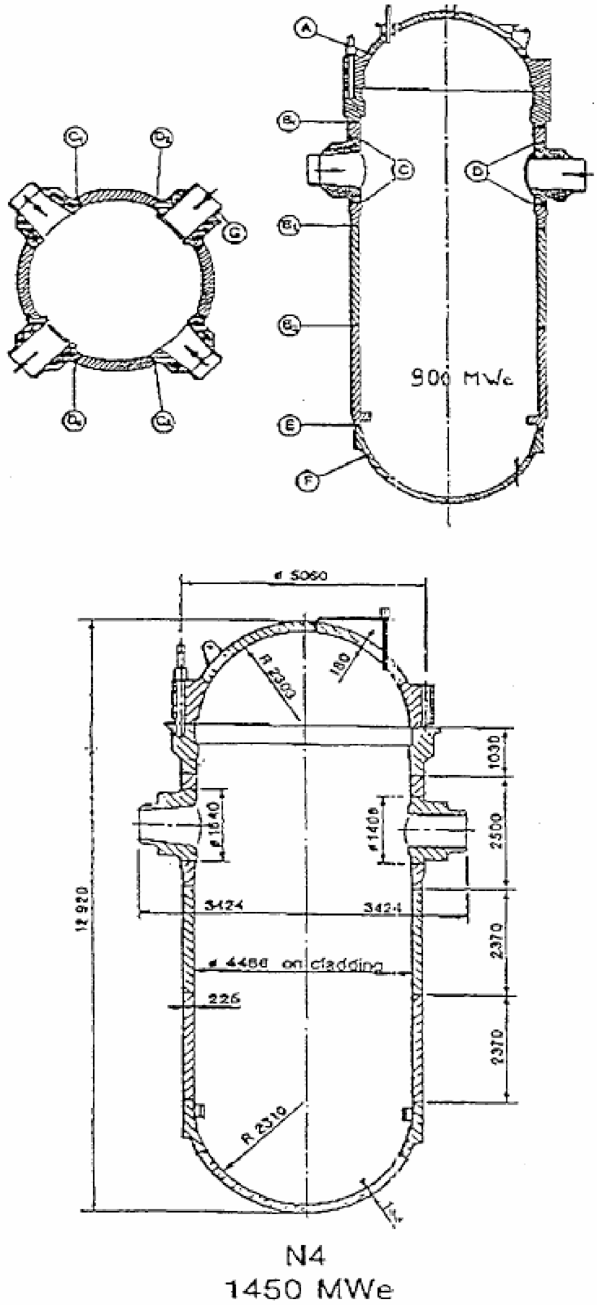


Fig. 5. Sketchs of French 3- and 4-loop RPVs; typical dimensions.

- Transport by land also results in a smaller vessel mass and, therefore, thinner walls which require higher strength materials.
- The upper part of the vessel consists of two nozzle rings, the upper one for the outlet nozzles and the lower one for the inlet nozzles. An austenitic stainless steel ring is welded to the inside surface of the vessel to separate the coolant entering the vessel through the inlet nozzles from the coolant exiting the vessel through the outlet nozzles. This design results in a rather abrupt change in the axial temperature distribution in the vessel, but uniform temperatures around the circumference.

TABLE 1. WESTERN REACTOR PRESSURE VESSEL DESIGN PARAMETERS

	French 4-loop N4 Type Plants	German Konvoi Design Values	Westinghouse 4-Loop Plant
Thermal power (MWth)	4,270	3,765	3,411
Electric output (MWe)	1,475	> 1,300	1,125
Number of loops	4	4	4
Type of fuel assembly	17 × 17	18 × 18 - 24	17 × 17
Active length (mm)	4,270	3,900	366
Core diameter (mm)	4,490	3,910	337
Water gap width* (mm)	424	545	51.2
Linear heating rate (W/cm)	179	166.7	183
Number of control rods	73	61	53
Total flow rate (m^3/hr)	98,000	67,680	86,800
Vessel outlet temperature (°C)	329.5	326.1	325.5
Outlet/inlet temperature difference (°C)	37.5	34.8	33.0
Specified RT_{NDT} at 30 L		-12°C	
ΔT_{41} at EOL (based on design values)	-	23°C	-

* distance from the outer fuel element and the RPV inner surface

- The WWER vessels are made only from forgings, i.e. from cylindrical rings and from plates forged into domes. The spherical parts of the vessels (the bottom and the head) are either stamped from one forged plate, or welded from two plates by electroslag welding, followed by stamping and a full heat treatment. There are no axial welds.
- The WWER inlet and outlet nozzles are not welded to the nozzle ring but they are either machined from a thicker forged ring, for the WWER-440 vessels, or forged in the hot stage from a thick forged ring for the WWER-1000 vessels. A typical WWER- 440 forged and machined nozzle is shown in Fig. 7.

2.2. RPV MATERIALS AND FABRICATION

2.2.1. Western PWR pressure vessel

Materials

The western PWR RPVs use different materials for the different components (shells, nozzles, flanges, studs, etc.). Moreover, the choices in the materials of construction changed as the PWR products evolved. For example, the Westinghouse designers specified American Society for Testing and Materials (ASTM) SA 302 Grade the shell plates of earlier vessels and ASTM SA 53 Grade B Class 1 for later vessels. [3, 4]

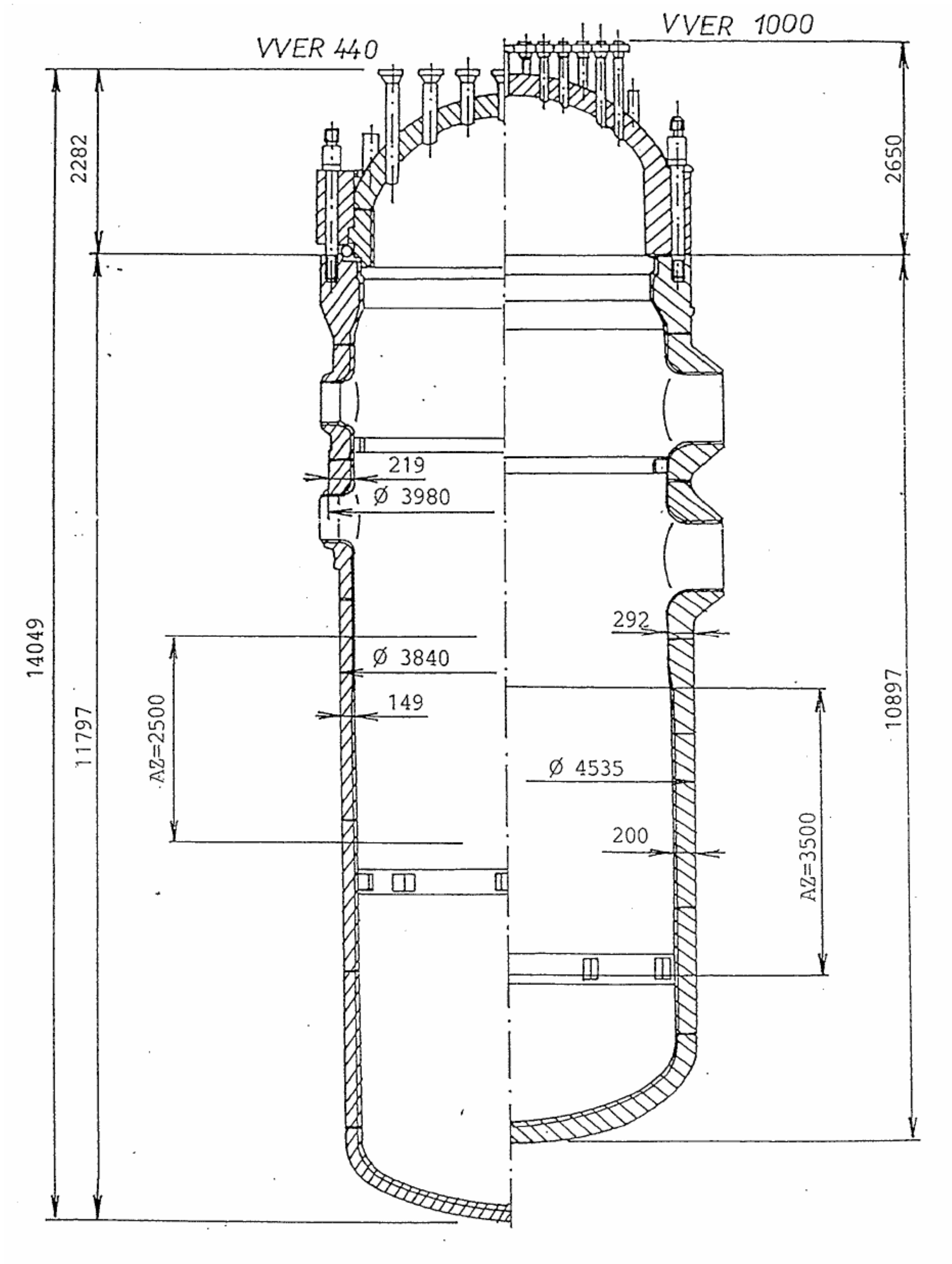


Fig. 6. WWER reactor pressure vessels (split diagram).

TABLE 2A. WWER REACTOR PRESSURE VESSEL DESIGN PARAMETERS

Reactor	WWER-440	
	V-230	V-213
mass [t]	215	
length [mm]	11,800	
outer diameter [mm]		
- in cylindrical part	3,840	
- in nozzle ring	3,980	
wall thickness (without cladding) [mm]		
- in cylindrical part	140	
- in nozzle ring	190	
number of nozzles	2 x 6[1]	2 x 6[1] + 2 x 3[2]
working pressure [MPa]	12.26	
design pressure [MPa]	13.7	
hydrotest pressure [MPa]	17.1	19.2[3]
operating wall temperature [°C]	265	
design wall temperature [°C]	325	
Vessel design lifetime [y]	30	40

1 Primary nozzle
2 Emergency Core Cooling System (ECCS) nozzle
3 Test pressure has been recently decreased to 17.2 MPa in Hungary, Czech Republic and Slovakia

TABLE 2B. EFFECTIVE FULL POWER YEARS (EFPY) FLUENCE FOR WWER-440 RPV

REACTOR TYPE	FLUX, $m^{-2}s^{-1}$ (E > 0.5MeV)	30 full-power effective years FLUENCE, m^{-2} (E > 0.5MeV)
WWER-440 core weld maximum	1.7×10^{15}	1.6×10^{24}
WWER-440 base metal maximum	2.5×10^{15}	2.4×10^{24}

Other vessel materials in common use include American Society of Mechanical Engineers (ASME) SA 508 Class 2 plate in the USA, 22NiMoCr37 and 20MnMoNi55 in Germany, and 16MnD5 in France. In addition to using plate products, all the NSSS vendors use forgings in the construction of the shell courses. Table III lists the main ferritic materials used for PWR vessel construction over the years and summarizes their chemical composition [5]. Table IV lists the individual vessel components and the various materials used for each component in the US and French N4 RPVs. These materials are discussed in somewhat more detail in the following paragraphs.

SA-302, Grade B is a manganese-molybdenum plate steel used for a number of vessels made through the mid-1960s. Its German designation is 20MnMo55. As commercial nuclear power evolved, the sizes of the vessels increased. For the greater wall thicknesses required, a material with greater hardening properties was necessary. The addition of nickel to SA-302, Grade B in amounts between 0.4 and 0.7 weight per cent provided the necessary increased hardening properties to achieve the desired yield strength and high fracture toughness across the entire wall thickness. This steel was initially known as SA-302, Grade B Ni Modified.

TABLE 2C. MATERIALS SPECIFIED FOR WWER PRESSURE VESSEL COMPONENTS

Reactor	WWER-440		WWER-1000
	V-230	V-213	V-320
Vessel components			
- cylindrical ring	15Kh2MFA	15Kh2MFA	15Kh2NMFAA
- other parts of vessel	15Kh2MFA	15Kh2MFA	15Kh2NMFA
- cover	18Kh2MFA	18Kh2MFA	15Kh2NMFA
- free flange	25Kh3MFA	25Kh3MFA	-
- stud bolts and nuts	25Kh1MF	38Kh3MFA	38Kh3MFA
Welding process			
- automatic submerged arc	Sv-10KhMFT +AN-42	Sv-10KhMFT +AN-42M	Sv-12Kh2N2MA +FC-16A
- electroslag	Sv-13Kh2MFT +OF-6	Sv-13Kh2MFT +OF-6	Sv-6Kh2NMFTA +OF-6

Forging steels have also evolved since the mid-1950s. The SA-182 F1 Modified material is a manganese-molybdenum-nickel steel used mostly for flanges and nozzles in the 1950s and 1960s. Another forging material used then was a carbon-manganese-molybdenum steel, SA-336 Fl. Large forgings of these materials had to undergo a cumbersome, expensive heat treatment to reduce hydrogen blistering. Eventually these steels were replaced with a steel, first described as ASTM A366 Code Case 1236 and is now known as SA-508 Class 2, that did not require this heat treatment [6]. This steel has been widely used in ring forgings, flanges and nozzles. It was introduced into Germany with the designation 22NiMoCr36 or 22NiMoCr37. With slight modifications, this steel became the most important material for German reactors for a long time. In addition, SA-508 Class 3 (20MnMoNi55 in Germany and 16 MnD5 and 18MnD5 in France) is used in the fabrication of western RPVs.

Although many materials are acceptable for reactor vessels according to Section III of the ASME Code [7], the special considerations pertaining to fracture toughness and radiation effects effectively limit the basic materials currently acceptable in the USA for most parts of vessels to SA-533 Grade B Class 1, SA-508 Class 2 and SA-508 Class 3 [8].

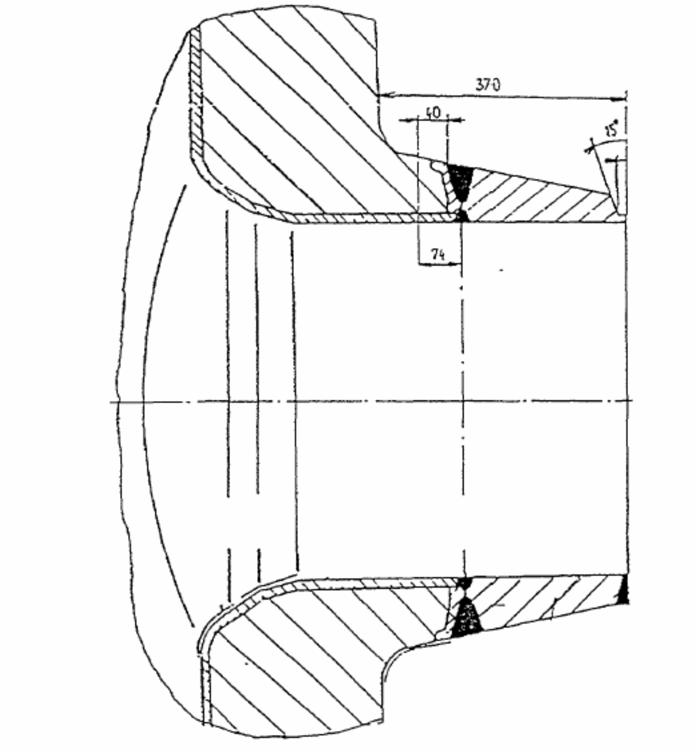


Fig. 7. Sketch of a typical forged and machined WWER-440 pressure vessel nozzle.

The part of the vessel of primary concern with regard to age related degradation is the core beltline — the region of shell material directly surrounding the effective height of the fuel element assemblies plus an additional volume of shell material, both below and above the active core, with an end-of-life fluence of more than 10^{21} n/m^2 (E >1 MeV) [6]. It is typically located in the intermediate and lower shells (Fig. 8). The low alloy steels making up the beltline are subject to irradiation embrittlement that can lead to loss of fracture toughness. When early vessels were designed and constructed, only limited data existed about changes in material properties caused by radiation damage. Now we know that the susceptibility of RPV steel is strongly affected by the presence of copper, nickel and phosphorus. Because operating vessels fabricated before 1972 contain relatively high levels of impurity copper and phosphorous, irradiation damage becomes a major consideration for their continued operation.

TABLE 3. CHEMICAL REQUIREMENTS (HEAT ANALYSIS) MAIN FERRITIC MATERIALS FOR REACTOR COMPONENTS IN WESTERN COUNTRIES

Designation	Elements (Weight %)													
	C	Si	Mn	P_{max}	S_{max}	Cr	Mo	Ni	V_{max}	Cu_{max}	Al	Sn	N2	As
ASTM A 302B		0.15	1.15				0.45							
	0.25	0.30	1.50	0.035	0.040		0.60							
ASTM A 336, Code Case 1236	0.19	0.15	1.10				0.50	0.40						
	0.25	0.35	1.30	0.035	0.035	0.35	0.60	0.50						
ASME A 508 cl 2 (1971)		0.15	0.50			0.25	0.55	0.50						
	0.27	0.35	0.90	0.025	0.025	0.45	0.70	0.90	0.05					
ASME A 533 gr B (1971)		0.15	1.15				0.45	0.40						
	0.25	0.30	1.50	0.035	0.040		0.60	0.70						
ASME A 508 cl 2 (1989)(1)		0.15	0.50			0.25	0.55	0.50						
	0.27	0.40	1.00	0..015	0.015	0.45	0.70	1.00	0.05	0.15				
ASME A 508 cl 3 (1989)(1)		0.15	1.20				0.45	0.40						
	0.25	0.40	1.50	0.015	0.015	0.25	0.60	1.00	0.05					
ASME A 533 gr B (1989)		0.15	1.15				0.45	0.40						
	0.25	0.40	1.50	0.035	0.040		0.60	0.70						
16 MnD5 RCC-M 2111(2)		0.10	1.15				0.43	0.50	0.01	0.20	0.040			
	0.22	0.30	1.60	0.02	0.012	0.25	0.57	0.80						
18 MnD5 RCC-M 2112 (1988)		0.10	1.15				0.45	0.50						
	0.20	0.30	1.55	0.015	0.012	0.25	0.55	0.80	0.01	0.20	0.040			
20 Mn Mo Ni 5 5 (1983, 1990)(3) (4)	0.17	0.15	1.20				0.40	0.50			0.010			
	0.23	0.30	1.50	0.012	0.008	0.20	0.55	0.80	0.02	0.12(5)	0.040	0.011	0.013	0.036
22 Ni Mo Cr 3 7 (1991)(6)	0.17	0.15	0.50			0.25		0.60			0.010			
	0.23	0.35	1.00	0.012	0.008	0.50	0.60	1.20(7)	0.02	0.12(5)	0.050	0.011	0.013	0.036

(1) Supplementary Requirement S 9.1(2) and S 9.2 for A 508 cl 2 and 508 cl 3.
(2) Forgings for reactor shells outside core region. Restrictions for Core Region (RCC-M 2111): S$\leq$0.008, P$\leq$0.008, Cu$\leq$0.08.
(3) VdTÜV Material Specification 401, Issue 1983.
(4) KTA 3201.1 Appendix A, Issue 6/90.
(5) Cu-Content for RPV (Core Region) shall be $\leq$0.10%.
(6) According *Siemens/KWU* under consideration of SR 10 (MPa Stuttgart).
(7) For flanges and tube sheets the Ni-content shall be $\leq$1.40%.

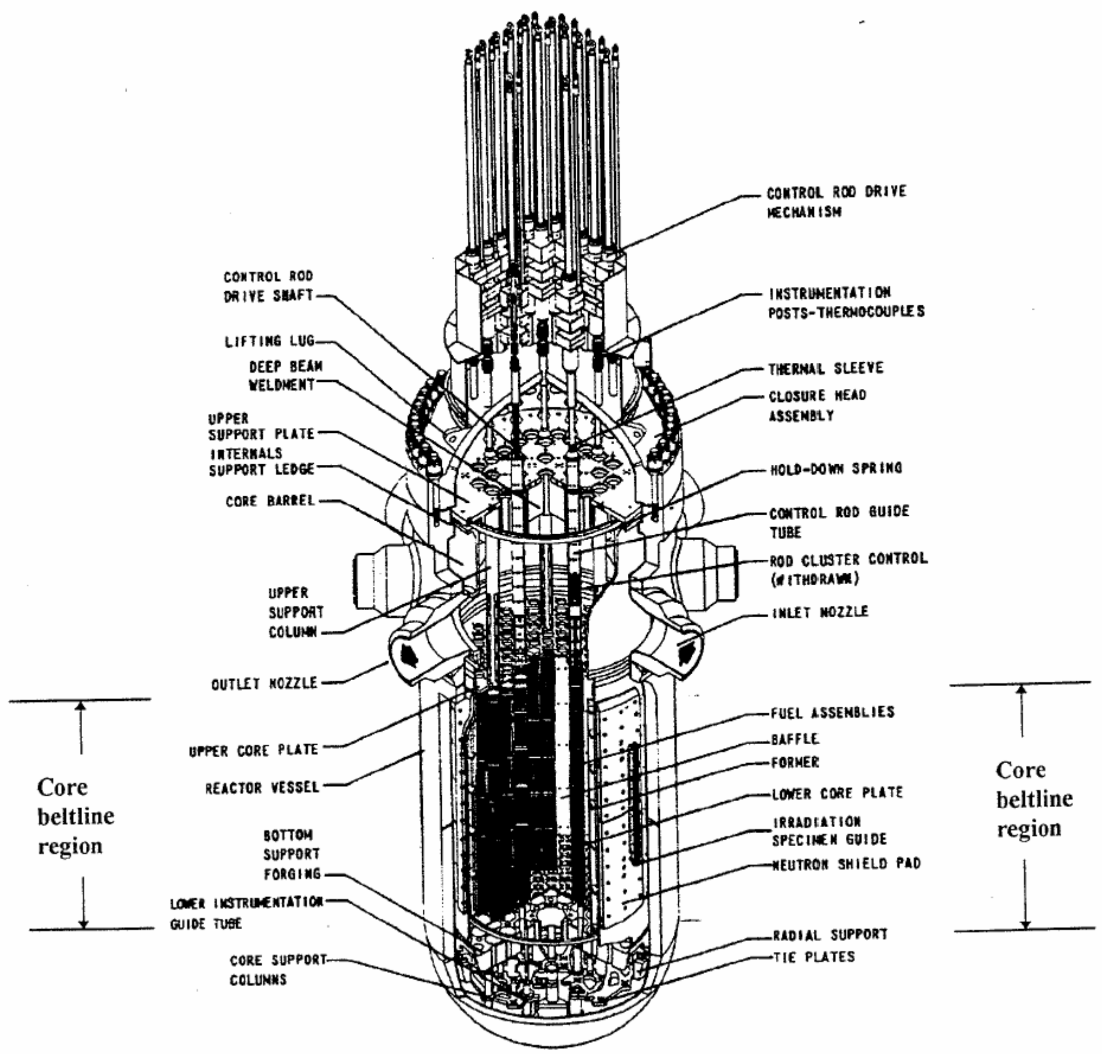


FIG. 8. Typical arrangement of reactor vessel plates and welds.

Other components of current concern with regard to ageing are certain CRDM nozzles. CRDM nozzles are made of stainless steel and Alloy 60 (ASME Code specification SB-166 bar or SB-167 pipe), a nickel base alloy. In Siemens RPVs, CRDM nozzles are made from ferritic steel, cladded with stainless steel (manufactured as co-extruded pipes), except the Obrigheim RPV, which is equipped with CRDM nozzles made from Alloy 600. Nozzles with Alloy 60 are of concern because some have experienced PWSCC. The composition of this alloy is about 75 weight per cent nickel, 15 weight per cent chromium and nine weight per cent iron, with trace amounts of carbon, manganese, sulphur, silicon, copper and cobalt.

The French have recently introduced the use of hollow ingots to make the beltline ring sections. The beltline material used in France is 16 MnD5. The chemical requirements for this material are listed in Table 3 along with the other western materials. The materials used for other N4 RPV components are listed in Table 4. The heat treatments and minimum material properties for 16MnD5 are listed in Table 5. The French practices in terms of K_{CV} and hot test

TABLE 4. MATERIALS SPECIFIED FOR PWR VESSEL COMPONENTS

U.S. Plants:

Closure Head Dome	Closure Head Flange	Lifting Lugs	Shroud Support Ring	Closure Head Stud Assembly	Vessel Flange	Shells	Bottom Head	Nozzles	CRDM Housings	Stainless Steel Cladding	Leakage Monitor-ing Tubes	Core Support Pads (Lugs)	Instru-menta-tion Tubes/ Pentra-tions	Refuelling Seal Ledge
SA302 GR B	SA336	SA302 GR B	SA212 GR B	SA320 L43	SA336	SA302 GR B	SA302 GR B	SA302 GR B	SA182 TYPES 304, 316	TYPE 308L, 309L	SA312 TYPE 316	SB166	SB166	SA212 GR B
SA533 GR B Class 1	SA508 Class 2 SA508 Class 3	SA533 GR B Class 1	SA516 GR 70	SA540 B23, B24	SA508	SA533 GR B Class 1	SA533 GR B Class 1	SA533 GR B Class 1	SB166	TYPE 304	SB166	SB167	SB167	SA516 GR 70
				SA320 L43 Class 3		SA336		SA336			SB167			SA533
						SA508 Class 2 SA508 Class 3		SA508 Class 2 SA508 Class 3						

French 4-loop N4 Plants:

Shells, flanges, heads, nozzles	16MnD5
Safe ends, Adapter flanges	Z2CND18-12
Adapter sleeves, instrumentation penetrations	NC15Fe/NC30Fe
Studs, nuts, washers	40NCDV7-03
Internal supports	NC15Fe

TABLE 5. HEAT TREATMENTS AND MINIMUM MATERIAL PROPERTIES FOR 16MND5

Austenisation	850-925°C	
Tempering	635-668°C	
Stress relieve	600-630°C	
Rp 0.2% at 20°C	> 400 MPa	
Rm at 20°C	550-670 MPa	
A% at 20°C	> 20	
Rp 0.2% at 350°C	> 300 MPa	
Charpy energy in J at 0°C	TL:	Ind. > 40[1] Mean > 56[2]
	L:	Ind. > 56 Mean > 72
at -20°C	TL:	Ind. > 28 Mean > 40
	L:	Ind. > 40 Mean > 56
at +20°C	TL:	Ind. > 72
	L:	Ind. > 88

(1) Measurement is from one individual specimen.
(2) Measurements from three specimens which are averaged.

requirements should be noted. As a general rule, material with a tensile strength at room temperature above 70 MPa cannot be used for pressure boundaries. The other western RPVs are designed with a minimum tensile strength of 350 MPa (50 Ksi).

Fabrication practice

Fabrication of RPVs has also been an evolving technology, and later vessels were fabricated using knowledge gained from the surveillance programmes and more modern methods such as the use of large forgings to reduce the number of welds in the beltline [6, 9].

Most RPVs in the USA were fabricated by either Combustion Engineering, Chicago Bridge and Iron, or Babcock and Wilcox. Westinghouse did not fabricate vessels but had them fabricated at another shop. Some vessels were fabricated in Europe by Rotterdam Drydock Company and by Creusot-Loire. In some cases, vessels were constructed by more than one fabricator because of scheduling problems in the shops.

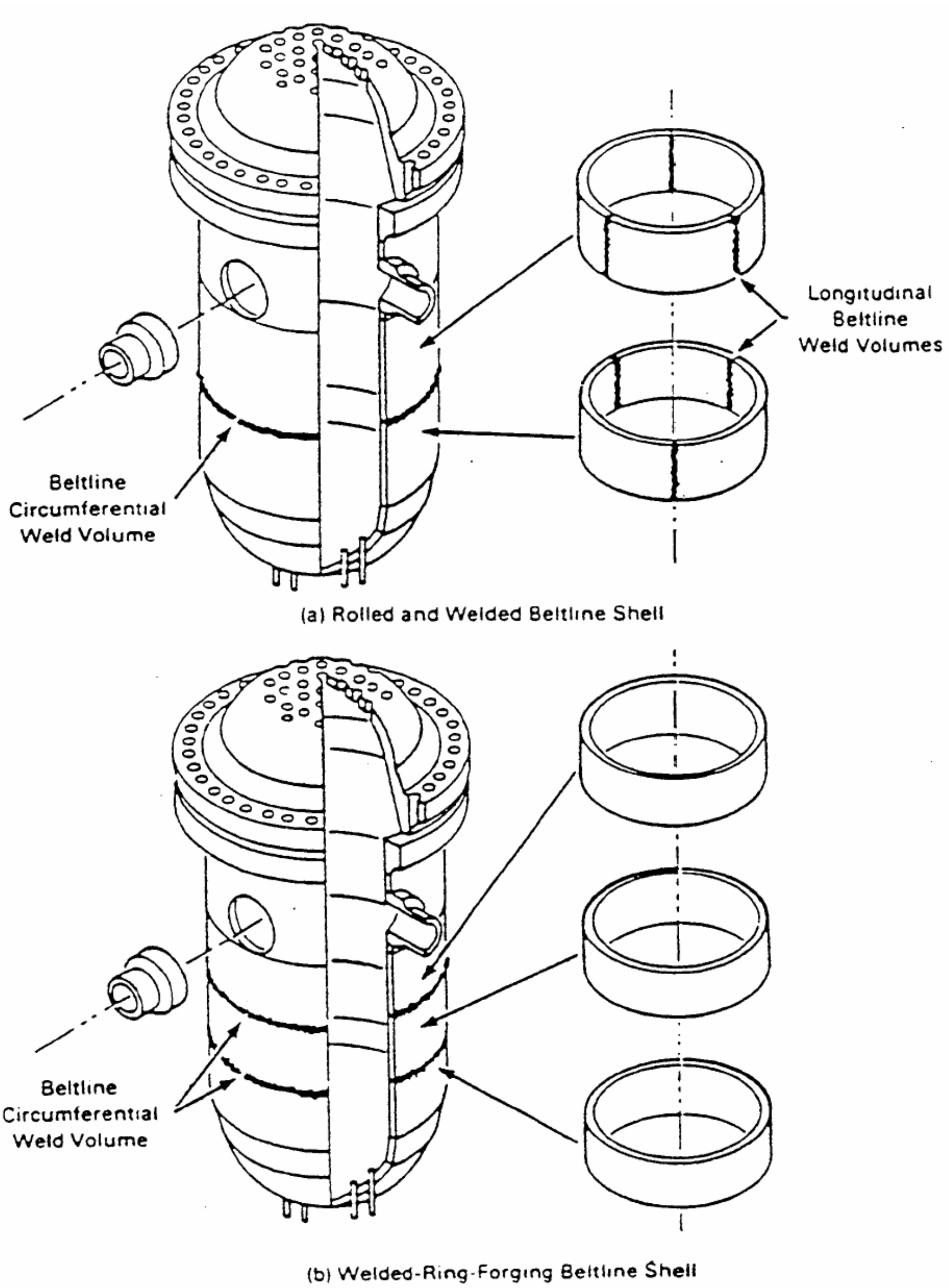


Fig. 9. Fabrication configuration of PWR beltline shells.

Large vessels are fabricated by two methods. In the first method, rolled and welded plates are used to form separate steel courses. Such a vessel has both longitudinal and circumferential weld seams (Fig. 9a). In some older vessels (before 1972), the longitudinal welds are of particular concern with regard to vessel integrity because they contain high levels of copper and phosphorous. In the second method, large ring forgings are used (Fig. 9b). This method improves component reliability because of the lack of longitudinal welds. Weld seams are located to avoid intersection with nozzle penetration weldments. Weldments within the beltline region were minimized once research showed that weld metal could be more sensitive to neutron radiation than base material. In general, parts of the longitudinal shell course welds are within the beltline region when the RPV is fabricated using plate material.

At least one circumferential weld is near, or marginally within, the beltline region when the RPVs are fabricated from either plates or ring forgings. Recently, NSSS vendors are designing the RPV such that the beltline region does not contain any weldments. This is accomplished by utilizing very large ring forgings to fabricate the shell course.

Western RPV heads may be fabricated by welding a central dished plate to multiple toroidal plates, sometimes called "orange peel" sections, forming a hemisphere. The lower head is welded to the lower shell course while the top head is joined to the shell course by a flanged and bolted joint. However, the modern French and German RPVs do not have welds in the heads except for the circumferential weld which connects the head to the flange (top) or shell (bottom).

The interior surfaces of the steel vessel, closure head and flange area are typically clad with stainless steel, usually Type 308 or 309. Cladding was used to prevent general corrosion by borated coolant and to minimize the buildup of corrosion products in the reactor coolant system. The cladding was applied in one or two layers by multiple-wire, single-wire, strip-cladding, or resistance welding processes. Some vessels have areas of Alloy 82 or 182 weld cladding where Alloy 600 components were welded to the vessel.

During the fabrication of some RPVs it was discovered that small cracks were present in the base metal beneath the cladding of the steel. The first incident of underclad cracking was discovered in the early 1970s in Europe and later in the USA. This cracking was defined as "reheat cracking" because the cracks appeared after the final stress relief heat treatment of the RPVs. Reheat cracking was limited to RPVs fabricated from A508 Class 2 forging steel or the equivalent European grades. Reheat cracking only occurred when the cladding was applied utilizing a high heat input welding procedure. During the cladding process, grain coarsing occurred due to the high heat input of the welding procedure, thus weakening the underclad grain boundaries. Then the subsequent post-weld stress relief heat treatment at elevated temperature resulted in decohension of the grain boundaries, e.g. small cracking occurred. Underclad reheat cracks are approximately 2 to 3 mm in depth and can be detected during the preservice NDE by using straight beam transducers. However, it is virtually impossible to size these cracks with NDT. Reheat cracking is, for the most part, confined to the cylindrical portion of the RPV. The beltline region can contain many millions of small reheat cracks.

The second incident of underclad cracking occurred in the late 1970s in Europe followed by discovery of cracks in the USA. The second incident of underclad cracking was identified as "cold cracking". Cold cracking only occurred during the cladding process of the RPV when the second layer of cladding was applied without preheat. Cold cracking was, for the most part, limited to the highly constrained nozzle regions in the RPV. The mechanism for cold cracking was hydrogen diffusion into the base metal during the application of the second layer of cladding The cracking occurred following cooldown of the component at locations where there was hydrogen and a high strain due to the RPV nozzle configuration The size of the cold crack beneath the cladding is of the order of 6 to 8 mm and these cracks are readily discovered during NDE. Unlike reheat cracking, the cracks that occurred due to cold cracking were removed by grinding prior to the vessel going into service. All RPV steels are susceptible to cold cracking if the cladding is applied without preheat in regions of high constraint. It is unlikely that cold cracking will occur at the beltline region of the RPV.

The USNRC reviewed the issue of reheat cracking and concluded that it was not a safety issue [10]. However, the USNRC also prohibited the use in USA of high heat input welding procedures for cladding of RPVs. To date there has not been any growth of the reheat cracks detected during the in-service inspections (ISIs). Cold cracking is not considered to be a significant issue because, for the most part, the cold cracks were removed prior to plant startup. Also any cold cracks that were inadvertently missed prior to startup would have been readily detected during the ISIs. Whitman et al. [11], Griesbach and Server [12], Griesbach [6] describe fabrication methods in detail, the Electric Power Research Institute (EPRI) [13] gives additional references. Kanninen and Chell [14] discussed the effect of the cladding on vessel integrity. Radiation embrittlement of beltline materials and the computer database containing data on beltline materials used in US reactors are covered in Ref [9].

Welding

The welding processes used were mostly submerged-arc and shielded-metal-arc. Before the early 1970s, copper-coated weld wire was used to improve the electrical contact in the welding process and to reduce corrosion during storage of the weld wire hence the generation of hydrogen. When it was discovered that copper and phosphorus increased the welds sensitivity to radiation embrittlement, RPV fabricators imposed strict limits on the percentage of copper and phosphorus in the welds as well as in plates [6, 11, 12]. The use of copper coated weld wire was eliminated due to the strict limits on the percentage of copper in the weld. The weld wire or stick electrodes were kept in storage in plastic bags and/or low temperature furnaces to eliminate the formation of moisture on the weld wire and electrodes.

For the circumferential welds, many beads of weld material and consequently a large volume of weld wire are needed. This becomes important when determining the properties of each individual weld in the beltline for sensitivity to neutron irradiation. For example, the chemistry of the weld (copper and nickel content) may vary through the thickness and around the circumference because of variations in the weld wire used in fabrication. Each weld in the vessel can be traced by the unique weld wire and flux lot combination used [9].

The sensitivity of welds to radiation can be inferred from the chemical composition. The degree of embrittlement [shift in transition temperature or decrease in upper shelf energy (USE)] is determined as a function of the chemical composition and the level of neutron exposure. Copper, nickel and possible phosphorus content in the weld are the most important elements from the standpoint of radiation damage. The embrittlement of high copper and high nickel welds plays a key role in the assessment of the significance of pressurized thermal shock (PTS) [9].

TABLE 6. NORMAL CHEMICAL COMPOSITION OF WWER REACTOR PRESSURE VESSEL MATERIALS (WEIGHT %)

MATERIAL	C	Mn	Si	P	S	Cr	Ni	Mo	V
WWER-440 15Kh2MFA	0.13 0.18	0.30 0.60	0.17 0.37	max 0.025	max 0.025	2.50 3.00	max 0.40	0.60 0.80	0.25 0.35
Submerged arc weldSv-10KhMFT + AN-42	0.040 .12	0.60 1.30	0.20 0.60	max. 0.042	max 0.035	1.20 1.80	max 0.30	0.35 0.70	0.10 0.35
Submerged arc weldSv-10KhMFT + AN-42M	0.04 0.12	0.60 1.30	0.20 0.60	max 0.012	max 0.015	1.20 1.80	max 0.30	0.35 0.70	0.10 0.35
Electroslag weld Sv-13Kh2MFT + OF-6	0.11 0.16	0.40 0.70	0.17 0.35	max 0.030	max 0.030	1.40 2.50	-	0.40 0.80	0.17 0.37

TABLE 7. REQUIREMENTS FOR AA RPV STEEL/WELD QUALITY (MAXIMUM ALLOWABLE CONTENT, MASS %)

Element	P	S	Cu	As	Sb	Sn	P+Sb+Sn	Co
AA-quality for beltline materials	0.012	0.015	0.08	0.010	0.005	0.005	0.015	0.020

TABLE 8A. GUARANTEED MECHANICAL PROPERTIES OF WWER VESSEL MATERIALS*

MATERIAL	20 °C				350 °C				$T_{k0}^{(1)}$ $RT_{NDT}^{(2)}$
	$R_{p0.2}$	R_m	A_5	Z	$R_{p0.2}$	R_m	A_5	Z	
	[Mpa]	[Mpa]	[%]	[%]	[Mpa]	[Mpa]	[%]	[%]	[^{O}C]
15Kh2MFA - base metal	431	519	14	50	392	490	14	50	$0^{(1)}$
A/S weld metal	392	539	14	50	373	490	12	45	$20^{(1)}$

*$R_{p0.2}$ is the 0.2 percent offset yield strength, R_m is the ultimate tensile strength, Z is the percent reduction in area at failure, and T_{k0} is the initial ductile-brittle transition temperature.

TABLE 8B. LIST OF ABBREVIATIONS USED IN WWER MATERIALS

Chemical elements			
A	high quality	**AA**	very high quality/purity
U	improved		
B	niobium	**F**	Vanadium
G	manganese	**Kh**	Chromium
M	molybdenum	**N**	Nickel
Sv	welding wire	**T**	Titanium
Beginning of the designation:			
0	lower than 0.1 mass % C	**08**	mean value 0.08 % C
15	mean value 0.15 % C		
Centre of the designation:			
Kh2	mean value 2 % Cr	**M**	lower than 1 % Mo

2.2.2. WWER pressure vessels

The WWER pressure vessel materials are listed in Table 2b. The normal chemical compositions of the various WWER materials are listed in Table 6, the allowable impurities in the beltline region are listed in Table 7 and the guaranteed mechanical properties are listed in Table 8a. Table 8b provides list of abbreviations used in WWER materials. As indicated by the information in these tables, the WWER pressure vessel materials are basically different from the western RPV materials. The Type 15Kh2MFA(A) material used for the WWER-440 pressure vessels contains 0.25 to 0.35 weight per cent vanadium and very little nickel (maximum of 0.40 weight per cent). The Type 15Kh2NMFA(A) material used for the WWER-1000 pressure vessels contains 1.0 to 1.5 weight per cent nickel and almost no vanadium. Material with vanadium alloying was first used in the Soviet naval RPVs because the vanadium carbides make the material relatively resistant to thermal ageing, fine grained (tempered banite) and strong. However, the Type 15Kh2MFA(A) material is harder to weld than nickel steels and requires very high preheating to avoid hot cracking. This became more of a problem for the large WWER-1000 pressure vessels and a material with nickel rather than vanadium alloying was chosen. The influence of vanadium on the susceptibility of those materials to radiation embrittlement was shown to be negligible.

Not all the WWER pressure vessels were covered by austenitic stainless steel cladding on their whole inner surface: only approximately half of the WWER-440/V-230 pressure vessels were clad. However, all of the WWER-440/V-213 and WWER-1000 pressure vessels were covered on the whole inner surface. The cladding was made by automatic strip welding under flux with two layers — the first layer is made of a Type 25 chromium/13 nickel unstabilized austenitic material (Sv 07Kh25N13), and the second layer is at least three beads made of Type 18 chromium/10 nickel stabilized austenitic stainless steel (Sv 08Khl8N10G2B) to achieve a required total thickness of cladding equal to ~ 8 mm. Therefore, all the austenitic steels which are in contact with water coolant are stabilized. The stabilized austenitic stainless steels contain an alloying element (usually titanium) which forms stable gain boundary carbides. This prevents chromium depletion along the grain boundaries and makes the material immune to stress corrosion cracking. Unstabilized material was used for the first layer because the thermal expansion coefficient of that material is closer to the thermal expansion coefficient of the low-alloy pressure vessel material.

The WWER vessel head contains penetrations with nozzles. The nozzles are welded to the vessel head from inside (buttering) and are protected by stainless steel sleeving (0Kh18N10T).

The WWER quality control and QA procedures are applied during manufacture, assembly and installation of the reactor in accordance with applicable standards. The required quality is ensured by:

- design by analysis,
- quality control of base and weld materials used,
- quality control during manufacture,
- acceptance testing prior to installation at the site.

Testing is performed using ultrasonic, radiographic, dye-penetrant and magnetic particle methods and includes hydrotests, if applicable. RPVs made in the Czech Republic were also monitored by acoustic emissions during the pressure hydrotests at the manufacturing site (ŠKODA), in Plzeň.

3. DESIGN BASIS: CODES, REGULATIONS AND GUIDES FOR REACTOR PRESSURE VESSELS

The load restrictions on as-fabricated RPVs in various national standards and codes are generally based on Section III of the ASME Boiler and Pressure Vessel Code [7]. The objective of designing and performing a stress analysis under the rules of Section III to the ASME Boiler and Pressure Vessel Code is to afford protection of life and property against ductile and brittle RPV failure. The ASME Section III requirements are discussed in the next section. Some important differences exist in the RPV design requirements of certain other countries (e g Germany, France and Russia) and these differences are discussed in Sections 3.3, 3.4 and 3.5.

3.1. DESIGN BASIS IN ASME SECTION III

The reactor vessel has been designated as Safety Class 1, which requires more detailed analyses than Class 2 or 3 components. The rules for Class 1 vessel design are contained in Article NB-3000 [7], which is divided into three sub-articles.

(a) NB-3100, General design rules

(b) NB-3200, Design by analysis

(c) NB-3300, Vessel design

Sub-article NB-3100 deals with loading conditions specified by the owner (or his agent) in the form of an equipment specification. The specification identifies the design conditions and operating conditions (normal conditions, upset conditions, emergency conditions, faulted conditions and testing conditions).

Sub-article NB-3200 deals with the stresses and stress limits which must be considered for the analysis of the component. The methods of analysis and stress limits depend upon the category of loading conditions, i.e. the requirement for normal conditions are considerably more stringent than those for faulted conditions.

Sub-article NB-3300 gives special requirements that have to be met by Class 1 vessels. This article gives tentative thickness requirements for shells, reinforcement requirements for nozzles and recommendations for welding nozzles, for example.

3.1.1. Transient specification

It is impossible to determine accurately the stresses in a component without a correct description of the loads applied to that component. The loads themselves are divided into two broad categories static and dynamic, the dynamic loads arising primarily from seismic conditions. The distinction between static and dynamic loads is based primarily on the comparison of the time span of the load variation to the response time of the structure.

The operating conditions themselves are divided into five categories depending on the seventy of the transient and the number of occurrences.

(a) Normal conditions

(b) Upset conditions

(c) Emergency conditions

(d) Faulted conditions

(e) Testing conditions.

Normal conditions are those which exist during normal running of the plant. Upset conditions are deviations from the normal conditions but are anticipated to occur often enough that provisions for them must be made in the analysis. These transients are those that do not result in forced outage, or if forced outage occurs, the restoration of power does not require mechanical repair. Emergency conditions are deviations from normal which require shutdown and may require repair and must be considered in order to assure no gross loss of structural integrity. Faulted conditions are deviations from normal, are extremely low probability, but may result in loss of integrity and operability of the system. Testing conditions are pressure overload tests, or other tests on the primary system.

For a PWR, the definitions of all operating transients are contained in the equipment specifications and are designed to represent the conditions under which a specific plant would operate. The interrelationship of the many groups within an organization needed to produce such a document is shown in Fig. 10. A listing of the transients, categories and number of occurrences contained in a typical specification is shown in Table 9.

3.1.2. Analysis of normal and upset conditions

Description of stress categories

The rules for design of Class 1 vessels make use of both realistic and accurate analysis techniques and failure criteria and therefore have relaxed overly restrictive safety factors used in the past. The calculated value of stress means little until it is associated with a location and distribution in the structure and with the type of loading which produced it. Different types of stress have different degrees of significance and must, therefore, be assigned different allowable values. For example, the average hoop stress through the thickness of the wall of a vessel due to internal pressure must be held to a lower value than the stress at the root of a notch in the wall.

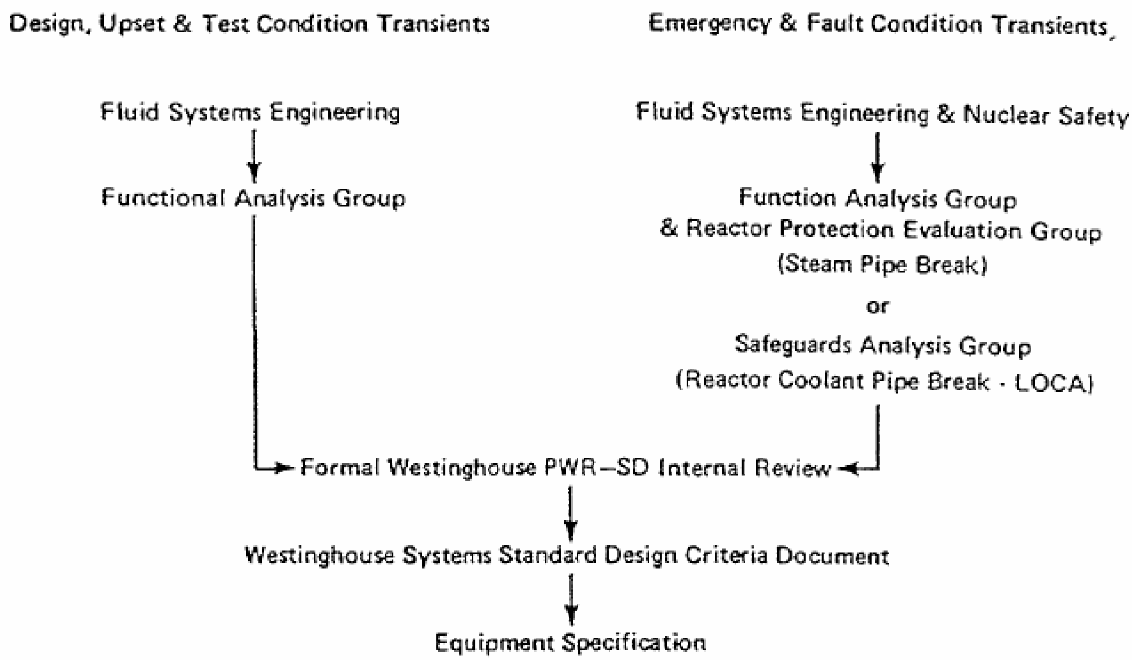


Fig. 10. Development of design transients.

TABLE 9. TYPE OF TRANSIENT, NUMBER OF OCCURRENCES, AND TRANSIENT CLASSIFICATION IN A TYPICAL PWR DESIGN SPECIFICATION

Title	Occurrences	Classification
Plant Heatup at 55°C (100°F)/Hr.	200	Normal
Plant Cooldown at 55°C (100°F)/Hr.	200	Normal
Plant Loading at 5% of Full Power per Minute	18,300	Normal
Plant Unloading at 5% of Full Power per Minute	18,300	Normal
Step Load Increase of 10% of Full Power	2,000	Normal
Step Load Decrease of 10% of Full Power	2,000	Normal
large Step Load Decrease (with Steam Dump)	200	Normal
Steady State Fluctuations	Infinite	Normal
Loss of Load (without Immediate Turbine or Reactor Trip)	80	Upset
Loss of Power (Blackout with Natural Circulation in Reactor Coolant System)	40	Upset
Loss of Flow (Partial Loss of Flow-One Pump Only)	80	Upset
Reactor Trip from Full Power	400	Upset
Inadvertent Auxiliary Spray	10	Upset
Turbine Roll Test	10	Test
Primary Side Hydrostatic Test Before Startup at 3105 psig (218.3 kg/cm^2)	5	Test
Primary Side Leak Test at 174.7 kg/cm^2 (2485 psig)	50	Test
Steam Pipe Break	1	Faulted
Reactor Coolant Pipe Break	1	Faulted

Likewise, a thermal stress can often be allowed to reach a higher value than one which is produced by dead weight or pressure. Therefore, a new, set of design criteria were developed which shifted the emphasis away from the use of standard configurations and toward the detailed analyses of stresses. The setting of allowable stress values required dividing stresses into categories and assigning different allowable values to different groups of categories. The failure theory used here is the maximum shear stress theory which has been found appropriate to reactor vessel applications and has the advantage of simplicity. Other criteria like the Mises criteria could be used as well. The maximum shear stress calculated from the failure theory defines stress intensities.

Different types of stress require different limits, and before establishing these limits, it was necessary to choose the stress categories to which limits should be applied. The categories and sub-categories chosen were as follows:

A. Primary stress

(1) General primary membrane stress

(2) Local primary membrane stress

(3) Primary bending stress.

B. Secondary stress

C. Peak stress.

The chief characteristics of these stresses may be described as follows:

(a) Primary stress is a stress developed by the imposed loading which is necessary to satisfy the laws of equilibrium between external and internal forces and moments. The basic characteristic of a primary stress is that it is not self-limiting. If a primary stress exceeds the yield strength of the material through the entire thickness, the prevention of failure is entirely dependent on the strain-hardening properties of the material.

(b) Secondary stress is a stress developed by the self-constraint of a structure. It must satisfy an internal strain pattern rather than equilibrium with an external load. The basic characteristic of a secondary stress is that it is self-limiting. These stresses are caused by thermal expansion or discontinuity conditions. The main concern with secondary stresses is that they may result in localized yielding or distortion.

(c) Peak stress is the highest stress in the region under consideration. The basic characteristic of a peak stress is that it causes no significant distortion and is objectionable mostly as a possible source of fatigue failure.

Stress intensity limits

The choice of the basic stress intensity limits for the stress categories described above was accomplished by the application of limit design theory tempered by some engineering judgment and some conservative simplifications. The principles of limit design which were used can be described briefly as follows.

The assumption is made of perfect plasticity with no strain-hardening. This means that an idealized stress-strain curve of the type shown in Fig. 11 is assumed. Allowable stresses, based on perfect plasticity and limit design theory, may be considered as a floor below which a vessel made of any sufficiently ductile material will be safe. The actual strain-hardening properties of specific materials will give them larger or smaller margins above this floor.

In a structure as simple as a straight bar in tension, a load producing yield stress, S_y results in "collapse". If the bar is loaded in bending, collapse does not occur until the load has been increased by a factor known as the "shape factor" of the cross section; at that time a "plastic hinge" is formed. The shape factor for a rectangular section in bending is 1.5. When the primary stress in a rectangular section consists of a combination of bending and axial tension, the value of the limit load depends on the ratio between the tensile and bending loads. Figure 12 shows the value of the maximum calculated stress at the outer fiber of a rectangular section which would be required to produce a plastic hinge, plotted against the average tensile stress across the section, both values expressed as multiples of the yield stress, S_y. When the average tensile stress, Pm is zero, the failure stress for bending is 1.5 S_y. When the average tensile stress is S_y no additional bending stress, P_b, may be applied.

Figure 12 was used to choose allowable values, in terms of the yield stress, for general primary membrane stress, P_m and primary membrane-plus-bending stress, $P_m + P_b$. It may be seen that limiting P_m to (2/3) Sy and $P_m + P_b$ to S_y provides adequate safety. The safety factor is not constant for all combinations of tension and bending, but a design rule to provide a uniform safety factor would be needlessly complicated.

In the study of allowable secondary stresses, a calculated elastic stress range equal to twice the yield stress has a very special significance. It determines the borderline between loads which, when repetitively applied, allow the structure to "shake down" to elastic action and loads which produce plastic action each time they are applied; 2 S_y is the maximum value of calculated secondary elastic stress which will "shake down" to purely elastic action.

We have now shown how the allowable stresses for the first four stress categories listed in the previous section should be related to the yield strength of the RPV material. The last category, peak stress, is related only to fatigue and will be discussed later. With the exception of some of the special stress limits, the allowables in Codes are not expressed in terms of the yield strength, but rather as multiples of the tabulated value S_m which is the allowable for general primary membrane stress. In assigning allowable stress values to a variety of materials with widely varying ductilities and widely varying strain-hardening properties, the yield strength alone is not a sufficient criterion. In order to prevent unsafe designs in materials with low ductility and in materials with high yield stress-to-tensile strength ratios, the Code has always considered both the yield strength and the ultimate tensile strength in assigning allowable stresses. The stress intensity limits for the various categories given are such that the multiples of yield strength described above are never exceeded.

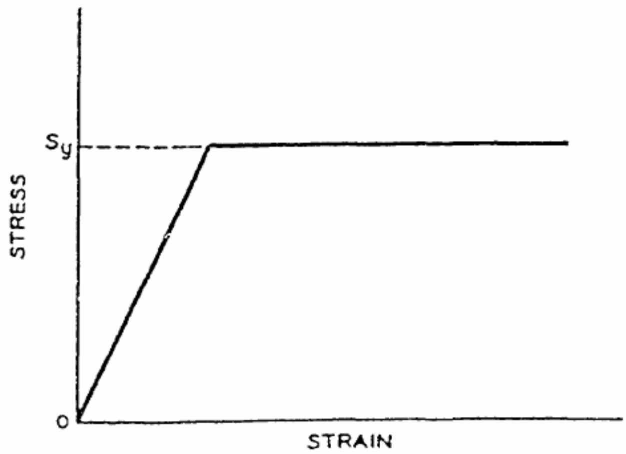


Fig. 11. Idealized stress-strain relationship.

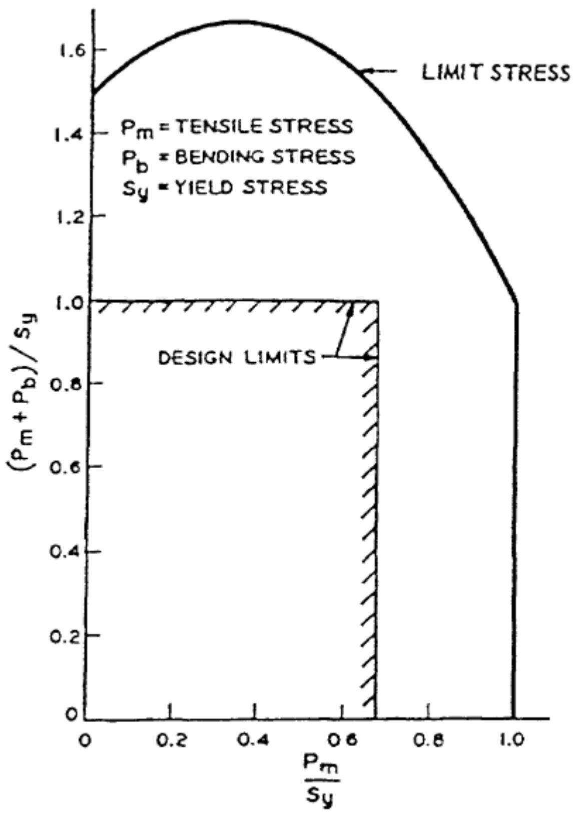


Fig. 12. Limit stress for combined tension and bending (rectangular section).

The allowable stress intensity for austenitic steels and some nonferrous materials, at temperatures above 38°C (100°F), may exceed (2/3) S_y and may reach 0.9 S_y at temperature.

Some explanation of the use of up to 0.9 S_y for these materials as a basis for S_m is needed in view of Fig 12 because this figure would imply that loads in excess of the limit load are permitted. The explanation lies in the different nature of these materials' stress strain diagram. These non-ferrous materials have no well-defined yield point but have strong strain-hardening capabilities so that their yield strength is effectively raised as they are high loaded. This means that some permanent deformation during the first loading cycle may occur, however, the basic structural integrity is comparable to that obtained with ferritic materials. This is equivalent to choosing a somewhat different definition of the "design yield strength" for those materials which have no sharply defined yield point and which have strong strain-hardening characteristics. Therefore, the Sm value in the code tables, regardless of material, can be thought of as being no less than 2/3 of the “design yield strength” for the material in evaluating the primary and secondary stresses.

The basic stress limits for each type of stress category are/is shown in Table 10. The basis for the allowable design stress intensity values (S_m) is shown in Table 11 for typical reactor vessel materials.

TABLE 10. ASME SECTION III STRESS LIMITS AND POTENTIAL FAILURE MODE FOR EACH TYPE OF STRESS CATEGORY

Stress intensity limit		Mode of failure
Primary stress		Burst and gross distortion
General membrane	S_m	
Local membrane + Primary bending	1.5 S_m	
Primary and secondary	3.0 S_m	Progressive distortion
Peak stresses	Design Fatigue Curve	Fatigue failure

TABLE 11. BASIS FOR THE ALLOWABLE DESIGN STRESS-INTENSITY VALUES (Sm) IN SECTION III OF THE ASME CODE

- Ferritic steels

  Design stress intensity value (S_m) is lowest of
  - 1/3 of the specified minimum tensile strength at room temperature
  - 1/3 of the tensile strength at temperature
  - 2/3 of the specified minimum yield strength at room temperature
  - 2/3 of the yield strength at temperature

- Austenitic steels, nickel-chromium-iron and Ni-Ch-Fe alloys

  Design stress intensity value (S_m) is lowest of
  - 1/3 of the specified minimum tensile strength at room temperature
  - 1/3 of the tensile strength at temperature
  - 2/3 of the specified minimum yield strength at room temperature
  - 90% of the yield strength at temperature, but not to exceed 2/3 of the specified minimum yield strength at room temperature

- Bolting materials

  Design stress intensity value (S_m) based on lowest of
  - 1/3 of minimum specified yield strength at room temperature
  - 1/3 of the yield strength at temperature up to a temperature of 426 7°C (800°F)

Fatigue evaluation

The last stress category to be examined is that of peak stresses. This category is only a concern in fatigue. The ASME Code gives specific rules for fatigue strength reduction factors and design curves for each type of material. For the component design to be acceptable, the cumulative usage factor at the end of life must be less than unity. Under some conditions outlined in the Code, a fatigue analysis is not necessary, however, conditions are then fairly restrictive.

Areas of the vessel analysed

The regions of the vessel which are examined in order to determine compliance with the ASME Code are shown in Figs 13-17. They are the areas which have potentially the highest stresses.

Stress analysis methods

Depending on the vendor, several different methods are used to determine the stresses in components. Two of the most popular are discontinuity analysis and finite element analysis as shown in Figs 18 and 19, respectively for the reactor vessel inlet nozzle.

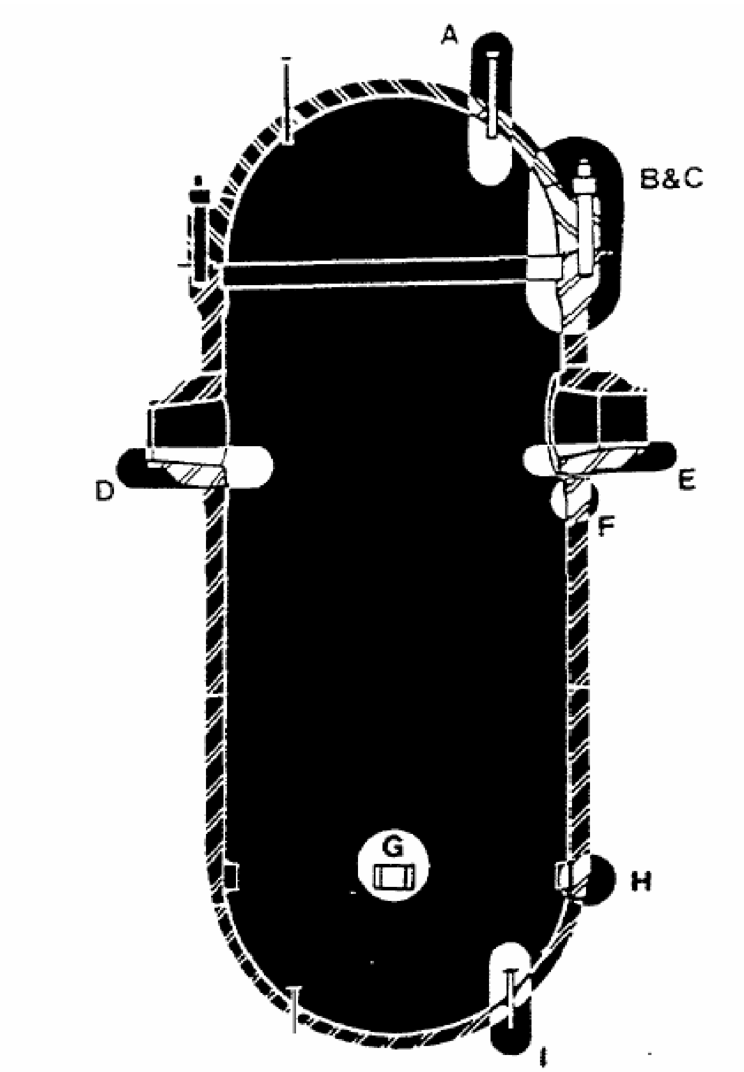


Fig. 13. Regions of a RPV to be analysed in order to determine compliance with the ASME Code.

Typical results of analysis for normal and upset conditions

For normal and upset conditions, Table 12 shows the maximum calculated primary stress intensities for the general membrane category and the local membrane plus bending category. Note, the stresses are all below the allowables for both categories.

Table 13 shows the maximum range of primary plus secondary stress intensities compared against the allowable limits; also the table shows the calculated usage factors in fatigue. Note, the Code requirements are satisfied in all cases.

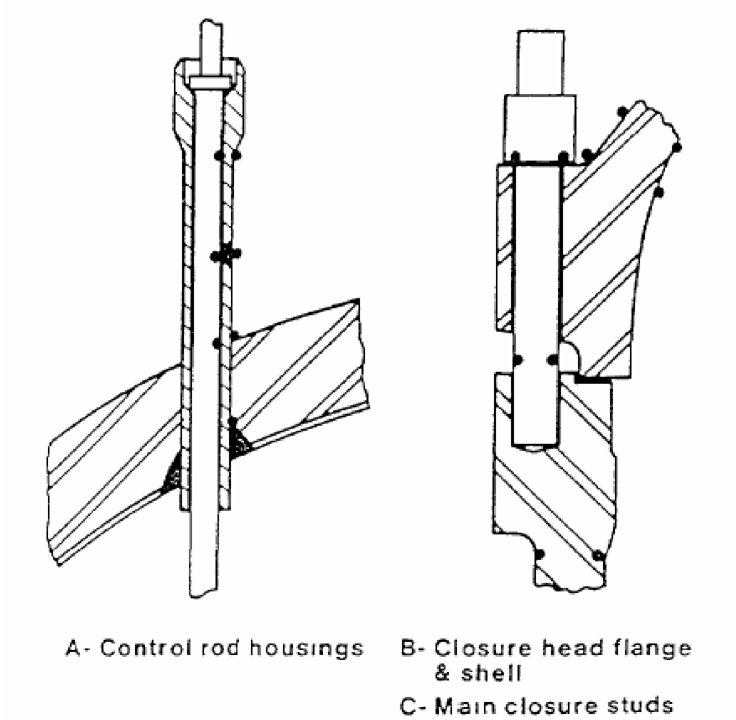


Fig. 14. Typical control rod housing and closure head flange, shell and studs locations to be evaluated in an ASME stress analysis.

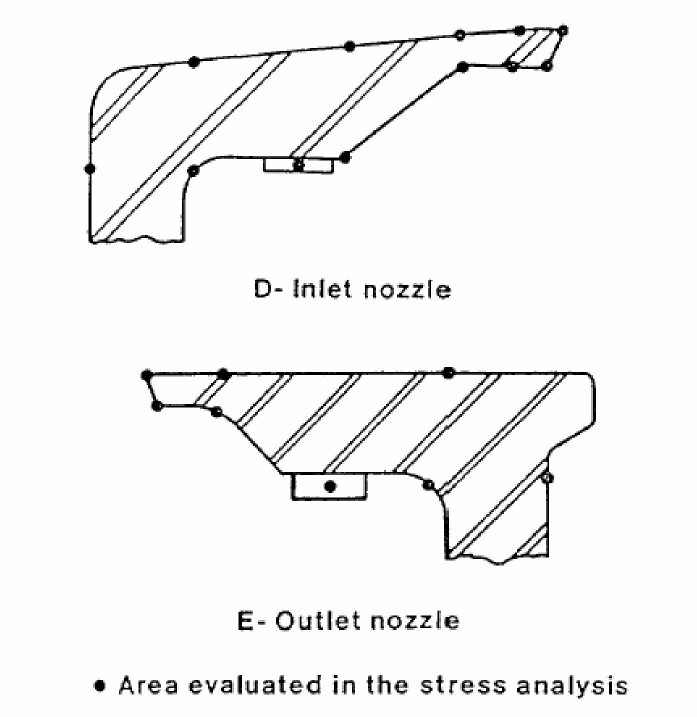


Fig. 15. Typical inlet and outlet nozzle locations to be analysed in order to determine compliance with the ASME Code

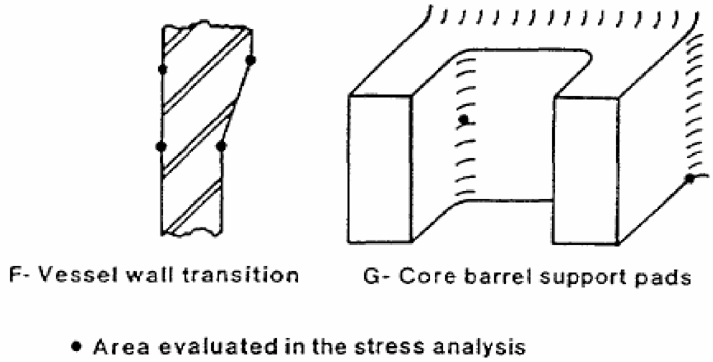


Fig. 16. A typical vessel wall transition and core barrel support pad locations to be analysed in order to determine compliance with the ASME Code.

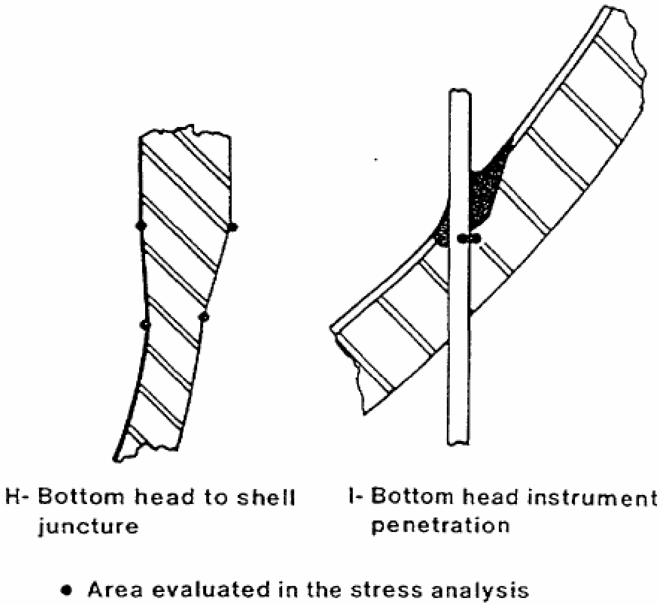


Fig. 17. Typical bottom head to shell juncture and bottom head instrument penetration locations to be analysed in order to determine compliance with the ASME Code.

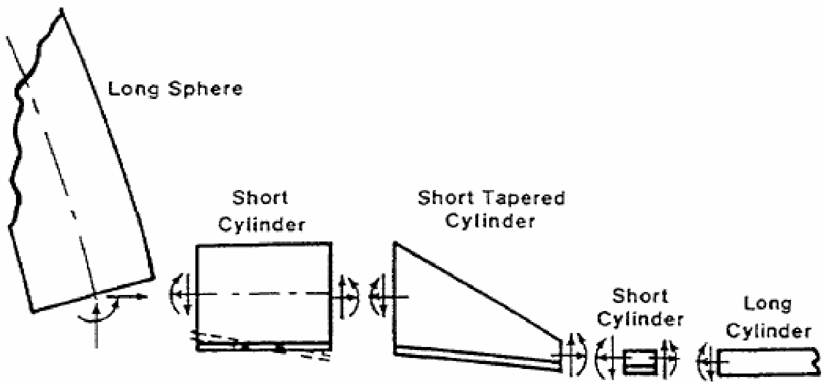


Fig. 18. Discontinuity analysis model of reactor vessel inlet nozzle.

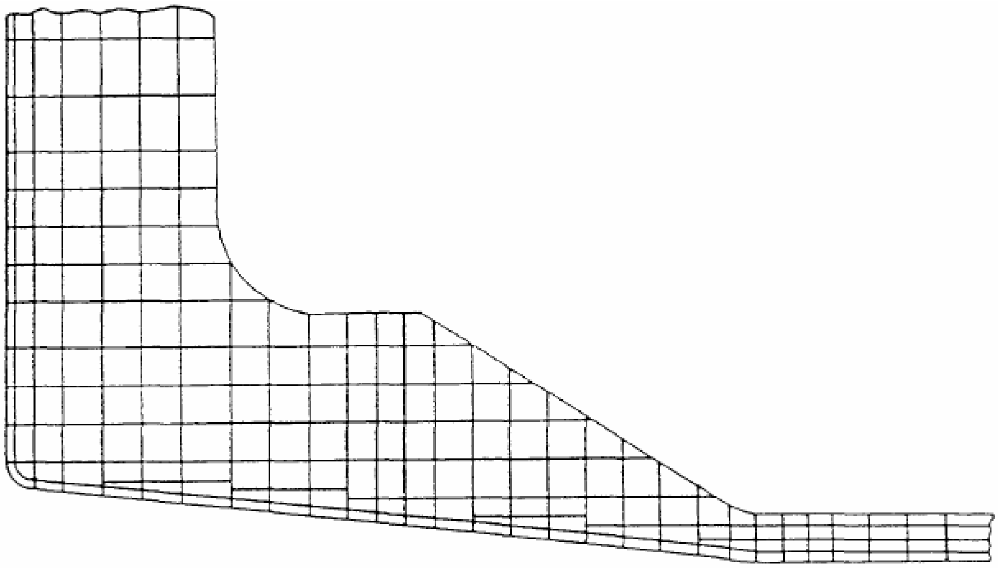


Fig. 19. Finite element analysis model of reactor vessel inlet nozzle.

TABLE 12. MAXIMUM CALCULATED PRIMARY STRESS INTENSITIES VERSUS ASME SECTION III ALLOWABLE LIMITS

NORMAL AND UPSET CONDITIONS				
	General membrane		Local membrane and bending	
Location	Calculated	Allowable (S_m)	Calculated	Allowable (1 5 S_m)
CRDM housings	24.3	26 7	16 9	35 0
Closure head-flange region	14 7	26 7	39 5	40 0
Vessel-flange region	19 9	26 7	29 7	40 0
Closure studs	34.5	34 8	47 6(1)	73 6
Inlet nozzle	15.8	26 7	37 4	40 0
Outlet nozzle	16 4	26 7	38 1	40 0
Vessel wall transition	26.3	26 7	24 3	40 0
Core support pads(2)			28 9	35 0
Bottom head to shell juncture	26 3	26 7	22 5	40 0
Bottom instrumentation tubes region	26.5	26 7	15 2	35 0

(1) Maximum average bolt service stress
(2) Not pressure retaining part

TABLE 13. MAXIMUM RANGE OF PRIMARY PLUS SECONDARY STRESS INTENSITIES COMPARED AGAINST THE ALLOWABLE LIMITS IN SECTION III OF THE ASME CODE

NORMAL AND UPSET CONDITIONS				
	Primary and secondary stress intensity		Usage factor	
Location	Calculated	Allowable ($3S_m$)	Calculated	Allowable
CRDM housings	54.6	69.9	0.09	1.0
Closure head-flange region	41.1	80.0	0.04	1.0
Vessel-flange region	58.2	80.0	0.02	1.0
Closure studs	91.5	110.4	0.57	1.0
Inlet nozzle	44.4	80.0	0.038	1.0
Outlet nozzle	48.0	80.0	0.06	1.0
Vessel wall transition	26.3	80.0	<0.01	1.0
Core support pads	46.2	69.9	0.37	1.0
Bottom head to shell juncture	27.1	80.0	0.01	1.0
Bottom instrumentation tubes region	53.2	69.9	0.13	1.0

TABLE 14. ALLOWABLE STRESS INTENSITY LIMITS IN SECTION III OF THE ASME CODE FOR EMERGENCY CONDITIONS

Primary stresses		Allowable limits
General membrane	(P_m)	Greater of 1.2 S_m or S_y for elastic analysis
Local membrane + Primary bending	($P_L + P_b$)	Greater of 1.8 S_m or 1.5 S_y for elastic analysis
		0.8 C_L for limit analysis (C_L denotes collapse load)

No evaluation of secondary stresses (including thermal stresses) is required since they are self-relieving.

These conditions need not be considered in the component fatigue evaluation since limited to a total of 25 occurrences.

TABLE 15. ALLOWABLE PRIMARY STRESS LIMITS FOR FAULTED CONDITIONS IN SECTION III OF THE ASME CODE

System or (Subsystem) Analysis	Components Analysis	Stress Limits for Components		Components Supports		
		P_m	P_1 or $[P_m(\text{or}P_1)+P_b]$	P_m	P_1 or $[P_m(\text{or}P_1)+P_b]$	Test
Elastic	Elastic	Smaller of $2.4S_m$&$0.70S_u$	Smaller of $3.6S_m$&$1.05S_u$ (4)	Larger of $1.5S_m$ or $1.2S_y$ (1)	Larger of $2.25S_m$ or $1.8S_y$ (4, 2)	
Elastic	Plastic	Larger of 0.70 S_u or S_y + 1/3 (S_u S_y) (5)	Larger of 0.70 S_u or S_y + 1/3 (S_{ut} S_y) (5)	S_y (1) L_2 (3, 5)	1.5 S_y (4, 2)	0.8 L_T
	Limit Analysis	0.9 L_1	(3)	0.9 L_1 (3)		L_2 (5)

S_u = Ultimate stress from engineering stress-strain curve at temperature
S_{ut} = Ultimate stress from true stress-strain curve at temperature
S_m = Stress intensity from ASME Section III at temperature
L_T = Test Load

(1) But not to exceed 0.70 S_u
(2) But not to exceed 1.05 S_u

(3) L_1 and L_2 = Lower bound limit load with an assumed yield point equal to 2.3. S_m and S_y (but not to exceed 0.70 S_u), respectively.
(4) These limits are based on a bending shape factor of 1.5. For simple bending cases with different shape factors, the limits will be changed proportionally.
(5) When elastic system analysis is performed, the effect of component deformation on the dynamic system response should be checked.

TABLE 16. GOVERNING MECHANICAL LOAD STRESS VERSUS ALLOWABLE FAULT CONDITION LIMITS

Loading	Stress category	Inlet nozzle S.I. (KSI)	Outlet nozzle S.I. (KSI)	Allowable limits Value	Allowable limits Limit
Reactor vessel nozzle safe ends					
Normal + DBE	P_m	17.65	16.14	40.08	2.4 S_m
	P_L+P_b	26.06	26.49	60.12	3.6 S_m
Normal + DBA	P_m	24.88	16.33	40.08	2.4 S_m
	P_L+P_b	34.28	26.07	60.12	3.6 S_m
Nor+DBE+DBA	P_m	30.55	17.69	40.08	2.4 S_m
	P_L+P_b	46.45	34.23	60.12	3.6 S_m
Reactor vessel nozzle to shell juncture					
Normal + DBE	P_L+P_b	35.82	36.46	74.92	1.8 S_y
Normal + DBA	P_L+P_b	41.58	49.32	74.52	1.8 S_y
Nor+DBE+DBA	P_L+P_b	45.64	53.36	74.52	1.8 S_y
Reactor vessel support pads					
Normal + DBE	Horiz.	47.71	29.37	56.68	1.2 S_y
	Vert.	7.83	9.29	56.68	1.2 S_y
Normal + DBA	Horiz.	59.94	76.43	109.14	0.8 test
	Vert.	15.45	14.71	56.58	1.2 S_y
Nor+DBE+DBA	Horiz.	107.66	105.80	108.14	0.8 test
	Vert.	21.12	21.85	56.58	1.2 S_y

3.1.3. Analysis of emergency and faulted conditions

Description of stress categories and analysis methods

For these types of operating conditions, the rate of occurrence is significantly less than normal and upset conditions and the primary concern is to prevent burst and gross distortion. For this reason limits are only placed upon the general membrane category and the local membrane plus primary bending category. Also, because inelastic analysis is often required, the stress limits are considerably more detailed. The system analysis used to determine the loads which act on the components is generally a dynamic analysis because of the nature of the events postulated (earthquakes/air crashes). This system analysis is generally elastic and the system design is modified by adding supports and stiffness to control structural resonance conditions. If significant inelastic response occurs within the component the original elastic system analysis requires modification. The stress intensity limits for emergency conditions are shown in Table 14. Depending upon the analysis method the applicable primary stress limits for faulted conditions are given in Table 15.

Typical results for faulted conditions

For the purposes of illustration, only the critical locations around the nozzles thought to be critical will be considered. The results of the analysis are shown in Table 16. In this table DBE is defined to be the Design Basis Earthquake and DBA is the Design Basis Accident.

3.1.4. Analysis of test conditions

The major interest for this transient is to prevent burst or permanent distortion. In the general primary membrane stress category, the stress intensity is limited to 0.9 of the tensile yield strength (σ_y) in the primary membrane plus primary bending stress category, the stress intensity is limited to 1 35 $\sigma_{y.}$ For the cold hydrotest transient the results of the analysis are shown in Table 17.

TABLE 17. MAXIMUM CALCULATED STRESS INTENSITIES DURING A COLD HYDRO TEST TRANSIENT COMPARED WITH THE ALLOWABLE LIMITS IN SECTION III OF THE ASME CODE

The reactor vessel hydro test initial pressure was 21 5 MPa (3125 psi)

Location	Calculated	Allowable (1 35σ_Y)
CRDM housing	23 8	47 25
Closure head - flange region	49 3	67 5
Vessel - flange region	37 2	67 5
Closure studs	85 0	130 0[1]
Inlet nozzle	45 6	67 5
Outlet nozzle	48 0	67 5
Vessel wall transition	31 9	67 5
Core support pads	38 8	47 25
Bottom head to shell juncture	34 1	67 5
Bottom instrumentation tubes	23 4	47 25

[1] Minimum bolt yield stress

3.1.5. Design and analysis against non-ductile failure (heatup and cooldown limit curves for normal operation)

At the recommendation of the Pressure Vessel Research Committee, the ASME Boiler and Pressure Vessel Code introduced criteria into Section III — Nuclear Power Plant Components — to provide assurance against brittle failure. The criteria required the component materials to satisfy certain fracture toughness requirements (NB-2330 of the Code). The criteria also introduced non-mandatory Appendix G, "Protection Against Non- Ductile Failure", into the ASME Code [15]. Appendix G of Section III presents a procedure for obtaining the allowable loading for ferritic pressure-retaining materials in Class 1 components. The procedure is based on the principles of linear elastic fracture mechanics (LEFM). Appendix G provides a reference critical stress intensity factor (K_I) curve as a function of temperature, a postulated flaw and a K_I expression.

The basic premise of LEFM is that unstable propagation of an existing flaw will occur when the value of K_I attains a critical value for the material designated as K_{IC}. K_{IC} is called the linear elastic fracture toughness of the material. In the case of ferritic materials, it has been found that the fracture toughness properties are dependent on temperature and on the loading rates imposed. Dynamic initiation fracture toughness obtained under fast or rapidly applied loading rates is designated K_{Id}. Further, in structural steels, a crack arrest fracture toughness is obtained under conditions where a propagating flaw is arrested within a test specimen. The crack arrest toughness is designated K_{Ia}. Appendix G to Section III presents a reference stress intensity factor [K_{IR}] as a function of temperature based on the lower bound of static K_{IC}, dynamic K_{Id} and crack arrest K_{Ia} fracture toughness values. The K_{IR} vs. temperature curve is shown in Fig. 20. No available data points for western-type ferritic RPV material yet tested for static, dynamic or arrest tests fall below the curve given. The value of K_{IR} represents a very conservative assumption as to the critical stress intensity vs. temperature properties of materials similar to those tested, as related to the measured nil-ductility temperature. The Code (NB-2331a) identifies a reference nil-ductility transition temperature (RT_{NDT}) to index the K_{IR} curve to the temperature scale. The reference temperature RT_{NDT} is defined (NB-2331) as the greater of the drop weight nil-ductility transition temperature or a temperature 33.3°C (60°F) less than the 68 J (50 ft-lb) [and 0.9 mm (35 mils) lateral expansion temperature] as determined from Charpy specimens oriented normal (NB-2322.2) to the rolling direction of the material (the T-L orientation). The requirements of Charpy tests at 33.3°C (60°F) above the nil-ductility temperature serve to sort out nontypical materials and provide assurance of adequate fracture toughness at "upper shelf" temperatures. In addition, the requirement of lateral expansion values provides some protection from variation in yield strength. Measurement of lateral expansion can also serve as an index of ductility.

G-2120 of Appendix G gives a postulated defect to be used in determining the allowable loading. As shown in Fig. 21, it consists of a sharp surface flaw, perpendicular to the direction of maximum stress, having a depth of 1/4 of the section thickness over most of the thickness range of interest. The assumed shape of the postulated flaw is semi-elliptic, with length six times its depth. In sizing the postulated flaw, it was assumed that (with the combination of examinations required by Section III and the volumetric examination required by ASME Section XI [17]) there is a very low probability that defects larger than four times the allowable size as defined in Section III will escape detection.

G-2200 outlines the recommended procedure for protection against nonductile failure for normal and upset operating conditions. Included in G-2200 is G-2214 which defines methods to calculate linear elastic stress intensity factors, K_I. G-2215 provides the bases for determining allowable pressure at any temperature at the depth of the postulated defect during normal, upset and operating conditions. The requirements to be satisfied and from which the allowable pressure for any assumed rate of temperature change can be determined are:

$$2K_{IM}+K_{IT}<K_{IR} \tag{1}$$

where

K_{IM} is the stress intensity factor for primary stresses, and
K_{IT} is the stress intensity factor for secondary stress.

This must be maintained throughout the life of the component at each temperature with K_{IM} from G-2214 1, K_{IT} from G-2214 2 and K_{IR} from G-2212. The recommended safety factor of 2 on K_{IM} adds to the conservatism of the assumptions. Due to its secondary and self-relieving nature, no safety factor is given for K_{IT}. G-2410 relaxes the conservatism by reducing the safety factor for K_{IM} to 1.5 during system hydrostatic testing.

Heatup and cooldown limit curves (P-T limit curves) are calculated using Appendix G and the most limiting value of the reference nil-ductility transition temperature (RT_{NDT}) for a given RPV. The most limiting RT_{NDT} of the material in the core region of the RPV is determined by using the preservice reactor vessel material fracture toughness properties and estimating the radiation-induced change in the reference nil-ductility transition temperature (ΔRT_{NDT}). RT_{NDT} is designated as the higher of either drop weight nil-ductility transition temperature (NDTT) or the temperature at which the material exhibits at least 50 ft-lb of impact energy and 0 9 mm (35-mil) lateral expansion (normal to the major working direction) minus 33°C (60°F).

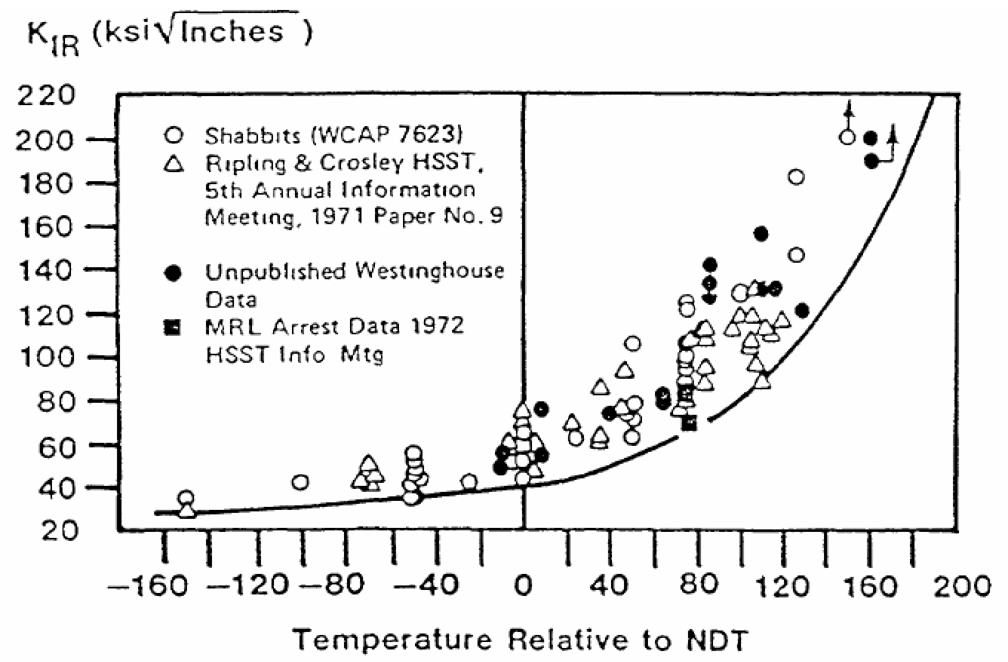


Fig. 20. Derivation of curve of reference stress intensity factor (K_{IR}).

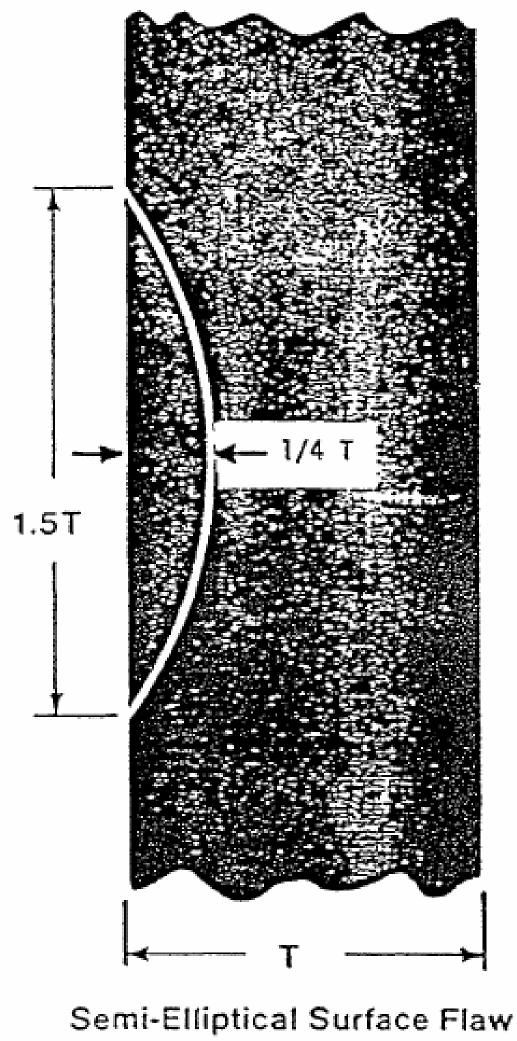


Fig. 21. ASME Section III, Appendix G Reference.

The fracture-toughness properties of the ferritic material in the reactor coolant pressure boundary are determined in accordance with the NRC Regulatory Standard Review Plan. Appendix G to the ASME Codes specifies that for calculating the allowable limit curves for various heatup and cooldown rates, the total stress intensity factor, K_I, for the combined thermal and pressure stresses at any time during heatup or cooldown cannot be greater than the reference stress intensity factor, K_{IR}, for the metal temperature at that time. K_{IR} is obtained from the reference fracture toughness curve, defined in Appendix G to the ASME Code. The K_{IR} curve is given by the following equation:

$$K_{IR} = 26.78 + 1.223 \exp [0.0145 (T-RT_{NDT} + 160)] \quad (2)$$

where

K_{IR} = reference stress intensity factor in British units (ksi -in) as a function of the metal temperature T (°F) and the metal reference nil-ductility temperature RT_{NDT}.

Therefore, the governing equation for the heatup-cooldown analysis is defined in Appendix G of Section III of the ASME Code [15] as follows:

$C\ K_{IM} + K_{IT} < K_{IR}$ (3)

where

K_{IM} = stress intensity factor caused by membrane (pressure) stress

K_{IT} = stress intensity factor caused by the thermal gradients

K_{IR} = function of temperature relative to the RT_{NDT} of the material

C = 2.0 for Level A and Level B service limits

C = 1.5 for hydrostatic and leak test conditions during which the reactor core is not critical.

At any time during the heatup or cooldown transient, K_{IR} is determined by the metal temperature at the tip of the postulated flaw, the appropriate value for RT_{NDT} and the reference fracture toughness curve. The thermal stresses resulting from the temperature gradients through the vessel wall are calculated and then the corresponding (thermal) stress intensity factors, K_{IT}, for the reference flaw are computed. From Equation (3), the pressure stress intensity factors, K_{IM}, are obtained and, from these, the allowable pressures are calculated.

For the calculation of the allowable pressure versus coolant temperature during cooldown, the reference flaw of Appendix G to the ASME Code is assumed to exist at the inside of the vessel wall. During cooldown, the controlling location of the flaw is always at the inside of the wall because the thermal gradients produce tensile stresses at the inside which increase with increasing cooldown rates. Allowable P-T relations are generated for both steady-state and finite cooldown rate situations. From these relations, composite limit curves are constructed for each cooldown rate of interest.

The use of the composite curve in the cooldown analysis is necessary because control of the cooldown procedure is based on the measurement of reactor coolant temperature whereas the limiting pressure is actually dependent on the material temperature at the tip of the assumed flaw.

During cooldown, the 1/4 wall thickness location is at a higher temperature than the fluid adjacent to the vessel inside diameter. This condition, of course, is not true for the steady-state situation. It follows that at any given reactor coolant temperature, the temperature change developed during cooldown results in a higher value of K_{IR} at the 1/4 wall thickness location for finite cooldown rates than for steady-state operation. Furthermore if conditions exist so that the increase in K_{IR} exceeds K_{IT}, the calculated allowable pressure during cooldown will be greater than the steady-state value.

The above procedures are needed because there is no direct control on temperature at the 1/4 wall thickness location and, therefore, allowable pressures may unknowingly be violated if the rate of cooling is decreased at various intervals along a cooldown ramp. The use of the composite curve eliminates this problem and ensures conservative operation of the system for the entire cooldown period.

Also, the 1993 Amendment to 10 CFR 50 has a rule which addresses the metal temperature of the closure head flange and vessel flange regions. This rule states that the metal temperature of the closure flange regions must exceed the material RT_{NDT} by at least 67°C (120°F) during normal operation when the pressure exceeds 20% of the preservice hydrostatic test pressure.

Vendors, owners and regulatory bodies can perform or require an ASME Section III Appendix G analysis for normal, upset and test conditions for all RPVs. Stresses are obtained from the pertinent stress report and the methods of ASME Appendix G are applied to four locations in the reactor vessel: closure head to flange region, nozzle to shell course region, beltline region and the bottom closure head to shell course region. Neutron radiation effects are factored into the analysis, where applicable. The analysis demonstrates the existence of adequate margins for continued operation over the life time of the plant in the presence of a flaw one quarter the vessel wall thickness in depth.

3.2. REGULATORY REQUIREMENTS FOR RPV DESIGN IN THE UNITED STATES OF AMERICA

Part 50 of the US Code of Federal Regulations, Title 10 (10 CFR 50) [16] regulates the construction of NPPs. Section 10 CFR 50.55(a) defines the reactor vessel to be part of the reactor coolant boundary and requires that the vessel meets the requirements for Class 1 vessels contained in the ASME Boiler and Pressure Vessel Code Sections III [7] and XI [17].

The pressure vessels in the USA were designed and fabricated in accordance with the version of Section III of the ASME Boiler and Pressure Vessel Code applicable at the time of fabrication, except for RPVs built before Section III existed (prior to 1963). Earlier plants, such as Yankee Rowe, Connecticut Yankee and a few others, were constructed to predecessors of Section III, such as Section I (power boilers) and Section VIII, Division 1 (unfired pressure vessels) [18, 19]. The allowable stress levels for pressure boundary materials were about 25% lower for Section VIII than those permitted by Section III for similar materials, which resulted in thicker walls and larger nozzle corner radii for Section VIII vessels. However, Section III requires a more limiting NDE of the welds, so the probability of having manufacturing defects in a Section III vessel is smaller than for a Section VIII vessel.

The US Code of Federal Regulations, Title 10, contains other regulations which are applicable to the vessel, such as 10 CFR 50.60, "Acceptance criteria for fracture prevention measures for light water nuclear power reactors for normal operation", 10 CFR 50.61, "Fracture toughness requirements for protection against PTS events", and Appendices A [20], G [21] and H [22] of 10 CFR 50. The quality, fracture prevention and inspection of the reactor coolant pressure boundary are addressed in General Design Criteria 30, 31 and 32 of Appendix A. Appendix G specifies fracture toughness requirements for ferritic RPV materials based on ASME Code, Section III. Requirements for the reactor vessel material surveillance programme are based on the ASTM requirements and are specified in Appendix H of Federal Regulation 10 CFR 50.

The following is a summary of the requirements of 10 CFR 50.60, 10 CFR 50.61, 10 CFR 50.66 and Appendices G and H to 10 CFR 50.

Under 10 CFR 50.60, "Acceptance criteria for fracture prevention measures for light water nuclear power reactors for normal operation", all nuclear power reactors must meet the fracture toughness and material surveillance programme requirements for the reactor coolant pressure boundary set forth in Appendices G and H to 10 CFR Part 50. The fracture toughness of the reactor coolant pressure boundary required by 10 CFR 50.60 is necessary to provide adequate margins of safety during any condition of normal plant operation. The required material surveillance programme monitors changes in the fracture toughness properties of ferritic materials in the beltline resulting from exposure to neutron irradiation and the thermal environment. Under the programme, fracture toughness test data are obtained from material specimens exposed in surveillance capsules, which are withdrawn periodically from the vessel.

Under CFR 50.61, "Fracture toughness requirements for protection against PTS events", the plant operators are required to assess the projected values of reference temperature. If the projected reference temperature exceeds the screening criteria in 10 CFR 50.61, the plant operator must submit an analysis and schedule for a flux reduction programme that is reasonably practicable and avoids exceeding the screening criteria. If no such flux reduction programme will avoid exceeding the screening criteria, the plant operator must submit a safety analysis to determine what actions are necessary to prevent potential failure of the reactor vessel if continued operation beyond the screening criteria is allowed. 10 CFR 50.61 has recently been modified to explicitly cite thermal annealing as a method for mitigating the effects of neutron irradiation, thereby reducing RT_{PTS}. PTS is discussed in more detail in Section 3.2.1 below.

Under 10 CFR 50.66, "Requirements for thermal annealing of the RPV", the nuclear plant operators in the USA are provided a consistent set of requirements for the use of thermal annealing to mitigate the effects of neutron irradiation. The thermal annealing rule impacts both 10 CFR 50.61 [pressure thermal shock (PTS) rule] and Appendix G of 10 CFR 50.

Appendix G to 10 CFR Part 50, "Fracture toughness requirements", requires that the beltline materials have Charpy upper shelf energies of no less than 68 J (50 ft-lb) throughout the life of the vessel. Otherwise, licensees must show equivalent margins of safety in accordance with Paragraph IV.A.1 or perform actions in accordance with Paragraph V.C of the Appendix.

Paragraph V.A of Appendix G to 10 CFR Part 50 requires a prediction of the effects of neutron irradiation on the reactor vessel materials. The extent of the neutron embrittlement depends on the material properties, thermal environment and results of the material surveillance programme. In Generic Letter 88-11, "NRC Position on Radiation Embrittlement of Reactor Vessel Materials and its Impact on Plant Operations", the USNRC stated that it will use the guidance in Regulatory Guide 1.99, Revision 2, "Radiation Embrittlement of Reactor Vessel Materials," in estimating the embrittlement of the materials in the vessel beltline. All the nuclear plant operators in the USA have responded to Generic Letter 88-11 and committed to use the methodology in Regulatory Guide 1.99, Revision 2 for predicting the effects of neutron irradiation. This methodology is also the basis in 10 CFR 50.61 for projecting the reference temperature.

Appendix H to 10 CFR Part 50, "Reactor Vessel Material Surveillance Programme Requirements", requires the surveillance programme to meet the ASTM Standard E 185. "Standard Practice for Conducting Surveillance Tests for Light-Water Cooled Nuclear Power Reactor Vessels", and specifies the applicable edition of ASTM E 185. Nuclear plant operators in the USA, especially those with reactor vessels purchased before ASTM issued the 1973 edition of ASTM E 185, may have surveillance programmes that do not meet the requirements of Appendix H to 10 CFR Part 50. They can use these alternative surveillance programmes if they have been granted an exemption. The plant operators must monitor the test results from the material surveillance programmes. According to Paragraph III.C of Appendix H to 10 CFR Part 50, the results may indicate that a plant Technical Specifications change is required, either in the pressure-temperature (P-T) limits or in the operating procedures required to meet the limits.

3.2.1. Pressurized thermal shock

The Pressurized Thermal Shock (PTS) rule is covered under 10 CFR 50.61 as part of the United States Nuclear Regulatory Commission's (US NRC) rule making responsibility. 10 CFR 50.61 provides a PTS screening criteria of 132^{o} C (270^{o} F) for plates, forgings, and axial weld materials, and 149^{o} C (300^{o} F) for circumferential weld materials. The screening criteria was developed using probabilistic risk assessment of PTS events. 10 CFR 50.61 provides guidelines for plants that exceed the screening criteria. Today, in the USA, 10 CFR 50.61 is the rule for assessment of a PTS event.

The US NRC in late September 2005 approved the proposed PTS Rulemaking. However, Regulatory Guide (R.G.) 1.99 Revision 3 will need to be completed prior to completion of the PTS Rulemaking process. Issuing of R.G. 1.99 Revision 3 is expected to take approximately one year. The Rulemaking could be completed sometime in October 2008. The October 2008 date includes a two year regulatory review process and a one year to complete R.G. 1.99 Revision 3.

The U.S. NRC has identified fourteen (14) technical questions that required resolution by February 2006, The EPRI / MRP (Material Reliability Programme) / ITG (Fatigue Issue Taskgroup) is interacting with the U.S. NRC to support the NRC Research Branch's response to these fourteen questions.

3.3. DESIGN BASIS IN GERMANY

The reactor vessel designs in Germany follow the German KTA standards for light water reactors, published by the NUSS Commission. The KTA requirements are very similar to those in the ASME Code, regarding the definition of stress intensities and allowable stresses. However, considerable differences exist in the design requirements for USE (upper shelf energy) and mid-thickness tensile and Charpy values, as well as for ISIs. Also, the German KTA has a limit on the allowable fluence whereas the ASME Code and the Codes in a number of other countries do not.

3.3.1. Non-ductile failure

To provide assurance against brittle failure, the KTA Standards require:

- an analysis of the brittle fracture transition temperature according to the Pellini/Porse methods and,
- a LEFM (linier elastic finite method) analysis (which is in accordance with Appendix G of Section III of the ASME Code).

(1) The brittle fracture transition temperature must be determined and shown to be well below the operating temperature range. However, the brittle fracture transition temperature concept is applied only to the core region, since that is where the maximum fast neutron fluence and the maximum primary stress occur.

(2) The allowance for detected flaw indications during ISIs is based on the principles of LEFM which are in accordance with Appendix A of Section XI of the ASME Code.

The acceptability of the observed flaws are met for all Service Limits if a safety factor of, at least, K_{IC}/K_I equal to 1.5 is shown. For locations other than the beltline region, a safety factor of 2 for the calculated membrane stress intensity factor K_I is, in contrast to ASME, not necessary for the level A and B Service Limits, also a surface flaw with a depth of 1/4 of the section thickness is not required if it can be justified.

For level C and level D Service Limits, assurance against brittle failure must be provided for the beltline region. KTA specifies that the critical flaw size which is still allowable must be twice as large as the flaw size which can reliably be detected by NDE. Crack instability is allowable if crack arrest can be proven within 3/4 of the section thickness.

3.3.2. Ductile failure and plastic collapse

This part of the design of the German RPVs follows the requirements of KTA 3201.2 [23]. In the main subjects, this part of KTA corresponds to the ASME Code, Section III, NB-3000. Load cases are given in a plant specification. The relation of the load cases to the service stress limits is done in the "design sheets" for the RPV for its whole or for parts of it. In addition, external loads, acting on nozzles or brackets, are also provided in the design sheets. The design stress intensity for low alloy ferritic RPV material is the smallest value of:

$$S_m = \left\{ \frac{R_{mRT}}{3}, \frac{R_{mT}}{2.7}, \frac{R_{p0.2T}}{1.5} \right\} \tag{4}$$

where

R_{mRT} is the minimum specified tensile strength at room temperature
R_{mT} is the minimum specified tensile strength at the design temperature
$R_{P0.2T}$ is the 0.2 per cent offset minimum specified yield strength at the design temperature.

In addition to the limitations on the loadings, the major RPV ferritic materials must initially have an USE of at least 100 J, measured with transverse Charpy V-notch specimens and the end-of-life USE must be at least 68J.

The stress limits of all service levels are given in Table XVIII. According to this table and the stress classifications, the number of calculations is fixed and corresponds to the requirements in the ASME Code.

Methods used to perform stress analyses are also given in KTA, especially:

- method of finite elements
- method of discontinuities.

Modelling of the RPV, or parts of it, allows the stress calculation to be performed everywhere in the component; but in general stresses are shown in sections or single points, covering the neighbourhood.

TABLE 18. GERMAN KTA STRESS LIMITS FOR THE VARIOUS SERVICE LEVELS

<table>
<tr><td colspan="4">Service Levels</td><td>Design Limits</td><td colspan="5">Service Limits</td></tr>
<tr><td colspan="4">Stress Category Prima</td><td>(Level 0)</td><td>Level A</td><td>Level B</td><td>Level p 2)</td><td>Level C 4)</td><td>Level D</td></tr>
<tr><td rowspan="6">Prima
ry
plus
secon
dary
plus
peak
stress
es</td><td rowspan="5">Prima
ry
plus
secon
dary
stress
es</td><td rowspan="3">ry
stress
es</td><td>P_m</td><td>S_m</td><td>-----</td><td>1.1 . S_m</td><td>0.9 . _$_{p0.2PT}$</td><td>_$_{p0.2T}$ 3)</td><td>0.7 . _$_{mT}$</td></tr>
<tr><td>P_1</td><td>1.5 S_m</td><td>-----</td><td>1.65 . S_m</td><td>1.35 . _$_{p0.2PT}$</td><td>1.5 . _$_{p0.2T}$ 3)</td><td>_$_{mT}$</td></tr>
<tr><td>$P_m + P_b$
or
$P_1 - P_b$</td><td>1.5 S_m</td><td>-----</td><td>1.65 . S_m</td><td>1.35 . _$_{p0.2PT}$</td><td>1.5 . $R_{p0.2T}$ 3)</td><td>_$_{mT}$</td></tr>
<tr><td colspan="2">P_e</td><td>-----</td><td>3 . S_m 1)</td><td>3 . S_m 1)5</td><td>-----</td><td>-----</td><td>-----</td></tr>
<tr><td colspan="2">$P_m + P_b + P_e + Q$
or
$P_l + P_b + P_e + Q$</td><td>-----</td><td>3 . S_m 1)</td><td>3 . S_m 1)5</td><td>-----</td><td>-----</td><td>-----</td></tr>
<tr><td colspan="3">$P_m + P_b + P_e + Q + F$
or
$P_l + P_b + P_e + Q + F$</td><td>-----</td><td>D $\leq$ 1.0;
2 . S_a</td><td>D $\leq$ 1; 6)
2 . S_a</td><td>-----</td><td>-----</td><td></td></tr>
</table>

(1) When the 3 . S_m stress intensity limit is exceeded, an elastic-plastic analysis shall be performed taking the stress cycles into account. Provided the applicable requisites are fulfilled, this may take the form of simplified elastic-plastic analysis.

(2) If the total of stress cycles is greater than 10, the number of stress cycles in excess of 10 shall be included in the fatigue analysis as for Level A and B Service Limits.

(3) But not more than 90% of the value for Level D Service Limits.

(4) If the total number of stress cycles is greater than 25, the number of stress cycles in excess of 25 shall be included in the fatigue analysis as for Level A and B Service Limits.

(5) These verifications are not mandatory in those cases in which stresses and strains of emergency and faulted service conditions are assigned to these Service Limits for reasons of operability or for any other reasons.

(6) Fatigue analysis is not mandatory in those cases in which stresses and strains of the emergency and faulted service conditions are assigned to these Service Limits for reasons of operability or for any other reasons and in which these service conditions are part of the group of 25 stress cycles for Level C Service Limits for which fatigue analysis is not required.

3.3.3. Heatup and cooldown limit curves for normal operation

In general, the same procedure as specified in the ASME Code and described in Section 3.1.5 above is used in Germany and defined as the "fracture mechanics approach" in KTA 3201.2 [23]. Alternatively, the KTA accepts the use of a modified Porse-diagram as the so called "RT_{NDT} approach", according to which the stress limits are calculated as a function of the minimum RPV-wall temperature according to the Pellini/Porse method.

3.4. DESIGN BASIS IN FRANCE

3.4.1. Code rules

The oldest 3-loop plants in France were designed under ASME Section III. Appendix G. The newer 4-loop plants are being designed under RCC-M B 3200. Appendix ZG [24].

The RCC-M B 3200 rules are similar to the rules in ASME Section III (however, the fabrication, welding, examination and QA rules are different) [25. 26]. The allowable stress, Sm, is equal to the minimum of:

$R_m/3$, $S_u/3$, $2R_e/3$, or $2S_y/3$ for ferritic steels
$R_m/3$, $S_u/3$, $2R_e/3$, or $0.9S_y$ for austenitic steels.

where R_m is the specified tensile strength at room temperature, S_u is the minimum tensile strength at temperature and S_y is the minimum yield limit at temperature. A value of 1.8 S_m is used for the Level C criteria rather than 2.25 S_m. Also, specific fatigue analysis requirements and specific methods for brittle and ductile fracture protection are included.

3.4.2. Brittle and ductile fracture assessment

Two methods for assessing the fracture toughness of the RPV steel are presented in RCC-M:

Method 1: similar to ASME Section III, Appendix G

- 1/4 thickness defect
- Level A: $2K_{Im} + K_{Ith} < K_{IR}$
- Level C & D 1 .5 $K_{Im} + K_{Ith}, < K_{IR}$

The K_{IR} curve is the same function of T-RT_{NDT} as in the ASME Code.

Method 2: An initial 15 mm crack is postulated, the end of life size is then evaluated using the Level A transient fatigue crack growth, the end of life K_j (based on J estimation scheme) is evaluated and the various criteria presented in Table 19 are used.

TABLE 19. RCC-M APPENDIX ZG CRITERIA

Level A criteria:	$T < RT_{NDT} + 50°C$	$K_{cp} < \min(0.4\ K_{IC};\ 0.5\ K_{Ia})$
	$T > RT_{NDT} + 50°C$	$K_{cp} < \min(0.7\ K_{Ia};\ 0.7\ K_{JC})$
Level C criteria:	$T < RT_{NDT} + 50°C$	$K_{cp} < \min(0.5\ K_{IC};\ 0.85\ K_{Ia})$
	$T > RT_{NDT} + 50°C$	$K_{cp} < \min(0.85\ K_{Ia};\ 0.85\ K_{JC})$
Level D criteria:	$T < RT_{NDT} + 100°C$	$K_{cp} < \min(0.8\ K_{IC};\ 0.9\ K_{JC})$ and crack arrest before 75% of the thickness
	$T > RT_{NDT} + 100°C$	$K_{cp} < 0.9\ K_{JC}$ and limited stable crack growth

where RT_{NDT} is the material nil ductility transition temperature,

K_{IC}, K_{Ia}, K_{JC} are the material static, arrest and ductile initiation toughness, and

K_{cp} is the elastic stress intensity factor with a plastic zone correction.

3.4.3. Heatup and cooldown limit curves for normal operation

The governing equation for this analysis is defined in RCC-M [27] Appendix ZG Method 1, which is similar to ASME III Appendix G and based on ¼ t depth crack and corresponding safety factors:

$$2\ K_{IM} + K_{It} < K_{IR}$$

The RSE-M gives in B2140, a figure for 2 hydro-proof test pressure, one for 1.2 time the design pressure (RT_{NDT} + 12°C) and one for 1.33 time the design pressure (RT_{NDT} + 18°C) and a specific curve for a cooling rate in operation of 20°C /hour [28 29]. These values are based on a 20 mm crack size, K_{IC} instead of K_{IR} and partial safety factors [30].

The fitness for service criteria are developed on the basis of the ISI performance for the more sensitive location. An underclad crack of 6 mm is used as a basic ISI performance demonstration and specific partial safety factors are used in accordance with French regulatory requirements (on load, on toughness and on crack size). A complementary probabilistic approach is proposed to highlight the results and the consequences of uncertainties in the data used in the analysis. [31].

3.5. WWER DESIGN BASIS

All the WWER RPVs were designed according to the Soviet (Russian) Codes in effect at the time of their design and manufacturing. Requirements for assuring general safety and design life were summarized in *Rules for Design and Safe Operation of Components of NPPs, Test and Research Reactors and Stations* [32] issued in 1973: these rules were updated in 1990 as *Rules for Design and Safe Operation of Components and Pipings of NPPs* [33] and in 2000 as *Rules for Design and Safe Operation of Components and Pipings of NPPs* [34]. The design itself (including the necessary stress analysis and the design lifetime calculations) was carried out mostly according to the *Code for Strength Calculations of Components of Reactors, Steam-Generators and Pipings of NPPs, Test and Research Reactors and Stations* [35] issued in 1973, which was updated in 1989 as the *Code for Strength Calculations of Components and Piping of Nuclear Power Plants, Moscow, 1989* [36]. The former Code was used for the design and analysis in the Pre-operational Safety Reports and the Supplementary Manufacturing Reports, the newer one is now also used for calculations within the Operational Safety Reports and other assessments. All these Soviet Codes were accepted also by all the national regulatory bodies of the countries operating these reactors.

3.5.1. Code requirements in the Russian Federation

The RPVs and primary system piping at all the major nuclear facilities, i.e. the PWRs, nuclear heating centres, as well as research and test reactors with operating temperatures over 600°C (i.e. with gas or liquid metal coolants) are safety related components and must be evaluated according to the Codes and Rules [32–36]. With respect to the WWER RPVs, special analysis requirements are also provided for radiation embrittlement.

The Code [33] is divided into 5 parts:

(1) General Statements deal with the area of Code application and basic principles used in the Code.

(2) Definitions gives full description of the most important operational parameters as well as parameters of calculations.

(3) Allowable stresses, strength and stability conditions.

(4) Calculation of basic dimensions deals with the procedure for choosing the component wall thickness, provides strength decrease coefficients and hole reinforcement values. Further, formulas for analysis of flange and bolting joints are also given.

(5) Validating calculations are the most important part of the Code. These detailed calculations contain rules for the classification of stresses as well as steps for stress determination.

Further, detailed calculations for different possible failure mechanisms are required and their procedures and criteria are given:

- calculation of static strength,
- calculation of stability,
- calculation of cyclic strength (fatigue),
- calculation of long-term cyclic strength (creep-fatigue) [not applicable for WWER RPV],
- calculation of resistance against brittle fracture,
- calculation of long-term static strength (creep) [not applicable for WWER RPV],
- calculation of progressive form change [not applicable for WWER RPV],
- calculation of seismic effects,
- calculation of vibration strength (ultra-high frequency fatigue).

TABLE 20. TYPICAL LIST OF WWER TRANSIENTS USED FOR DESIGN OF THE RPVS

CLASSIFICATION	TITLE	OCCURENCE
NORMAL	Primary side pressure test	100
	Primary side leak test	30
	Plant heat-up (20 °C)	130
	Plat loading at 1% of full power per minute	5600
	Plant unloading at 1 % of full power per minute	5000
	Change in 30-100% of full power	10000
	Lost of load (without immediate reactor trip)	150
	Step load decrease of 20% of full power	150
	Step load increase of 20% of full power	150
	Steady state fluctuation (+/- 5%)	Not limited
	Lost of power (blackout with natural circulation in reactor coolant system)	120
	Fault reactor trip	150
	Plant cool down (Max. 30 °C)	70
UPSET	Loss of flow (partial loss of flow-one pump only)	30
	Inadvertent auxiliary spray into steam generator	10
	Tube failure of steam generator	30
	Fast plant cool down (60 °C)	30
EMERGENCY	Small break loss of coolant accident (inside diameter less than 100 mm)	15
	Loss of coolant accident	1
	Non-closure of safety valve in pressurizer	1
	Non-closure of safety valve in steam generator	1
	Steam pipe break	1

A mandatory part of this Code is also a list of the materials (and their guaranteed properties) to be used for manufacturing the components of the nuclear steam supply system (NSSS), including the RPVs. These appendices also contain methods for the determination of the mechanical properties of these materials and some formulas for designing certain structural features (e.g. nozzles, closures, etc.) of the vessel, as well as typical equipment units strength calculations.

3.5.2. Transient specification

In accordance with the NPP elements and systems classification as described in the General Provisions on NPP Safety Assurance [37], the WWER pressure vessel belongs to the 1st class of safety. Therefore, appropriately more rigid requirements are placed on the quality of the design, as well as the fabrication and operation of the RPV.

In accordance with Ref. [33] there are operation modes for equipment and pipings (including RPVs) which are defined as follows:

Normal mode of operation

- working conditions in normal operation

Violation of normal mode of operation

- any deviation from the normal mode of operation (as to pressure, temperature, loads, etc.), requiring a shutdown of the reactor to eliminate these deviations but without actuating the ECCS.

Emergency situation

- any deviation from the normal mode of operation which could result in poor core cooling and actuation of the ECCS.

Additionally, the normal mode of operation is subdivided into the following categories: steady mode, start up, CPS work, reactor power change, shutdown, as well as pressure hydro tests for strength and tightness testing.

A list of the expected operational modes is prepared when the RPV lifetime is calculated. Faulted conditions, like earthquakes, are analysed in a special part of the validating calculations. Definitions of these conditions are similar to those in the ASME Code or other Rules. For a given type of reactor, these conditions are specified in the design specification as well as in the Pre-operational Safety Report and are plant specific mostly only in the definitions of seismic events and conditions. A typical list of transients for the WWER-1000 reactor type with their categorization and design number of occurrences is given in Table 20.

3.5.3. Stress analysis

The validating calculations require a detailed stress analysis to determine the different types of stresses and classify of them so as to be able to apply prescribed stress limits and safety coefficients. Detailed analysis of various failure mechanisms are also required.

Categories of stresses

In principle, the stresses are divided into the following categories:

σ_m general membrane stresses,

σ_M local membrane stresses,

σ_b general bending stresses,

σ_{Bl} local bending stresses,

σ_T general temperature stresses,

σ_{TL} local temperature stresses,

σk compensation stresses,

σ_{mw} mean tensile stresses in bolted sections, created by mechanical loading.

Checking calculations are carried out, applying to all existing loadings (including temperature effects) and all operating regimes.

Stress intensities, which are compared with allowable ones, are determined using the theory of maximum shear stresses with the exception of calculations of resistance against brittle failure, in which the theory of maximum normal stresses is applied.

Linear-elastic analysis techniques are used to calculate stresses in locations without stress concentrations. For fatigue calculations in the elastic-plastic region of loading, so-called pseudo-elastic stresses are used. These stresses are obtained by multiplication of the elastic-plastic strains in a given location by the Young's modulus.

Stress intensities are divided into four groups, according to their type:

$(\sigma)_1$ stress intensities calculated from the general membrane stress components,

$(\sigma)_2$ stress intensities calculated from the sum of the general or local membrane and bending stress components,

$(\sigma)_{3w}$ stress intensities calculated from the sum of the mean tensile stresses in a bolted section, including the tightening loads and the effects of temperature,

$(\sigma)_{4w}$ stress intensities caused by mechanical and temperature effects, including tensioned bolt loadings and calculated from stress components of tension, bending and twisting in bolts,

while the stress intensity ranges for RPVs are defined as:

$(\sigma)_{RV}$ the maximum stress intensity range calculated from the sum of the general and local stress components, the general and local bending stresses, the general temperature stresses and the compensation stresses.

Stress intensity limits

The WWER Codes [35, 36] do not contain allowable stress intensity values (i.e. stress intensity limits), therefore these values must be calculated using:

(a) guaranteed mechanical properties, given in the Code,

(b) safety coefficients, also given in the Code.

Nominal allowable stresses [σ] caused by internal pressure, are defined as a minimum value:

$$[\sigma] = \min \{ R_{mT}/ n_m; R_{p0.2T}/ n_{0.2} \} \tag{6}$$

where safety factors for vessels loaded by internal pressure are defined as:

$n_m = 2.6$ with respect to ultimate tensile strength, $R_{m,}$

$n_{0.2} = 1.5$ with respect to yield strength, $R_{p0.2.}$

The nominal allowable stresses in bolting materials, as a result of pressure and bolt tightening, are given as:

$$[\sigma] = R_{p0.2T}/ n_{0.2}, \tag{7}$$

where the safety factor is given by:

$n_{0.2} = 2.$

The allowable stresses in the WWER pressure vessel components are governed by the values calculated from the ultimate tensile strength of the material. These allowable stresses reach a value slightly less than 0.5 $R_{p0.2T}$, similar as for bolted joints, i.e. even somewhat lower than that according to the ASME Code.

The validating calculation for static strength serves to control the strength requirements taking into account pressure, weight, additional loading, reaction loading and temperature effects in all operational regimes. All stresses obtained during these calculations must not exceed the values given in Table 21. Mean bearing stresses must not exceed 1.5 $R_{p0.2T}$. At the same time, mean shear stresses, as a result of mechanical loadings, must not be larger than 0.5 [σ] (and, in bolt threads, no more than 0.25 Rpo2i). Mean shear stresses, as a result of mechanical loadings and temperature effects, must not be larger than 0.65 [σ] (and, in bolt threads, no more than 0.32 $R_{p0.2T}$). The general membrane stresses during hydraulic (or pneumatic) pressure tests must not be larger than 1.35 $[\sigma]_{Th}$ and the total stresses, determined as a sum of general and local membrane and general bending stresses must not be larger than 1.7 $[\sigma]_{Th}$, where $[\sigma]_{Th}$ is the allowable stress at the temperature of the pressure test. The maximum allowable stresses in the bolts during the pressure tests must not be larger than $0.7R_{p0.2Th}$. In calculations of static strength using stress range $(\sigma)_R$, the maximum or minimum absolute values of stresses, put into calculations of $(\sigma)_R$, must not be larger than R_{mT}. Supplementary requirements for these stresses are also given in Table 21.

TABLE 21. ALLOWABLE STRESS INTENSITY LIMITS FOR WWER RPVS

Components	REGIMES	$(\sigma)_1$	$(\sigma)_2$	$(\sigma)_{3W}$	$(\sigma)_{4W}$	$(\sigma)_{RV}$
REACTOR PRESSURE VESSEL	NORMAL OPEARATING CONDITIONS	$[\sigma]$	1.3 $[\sigma]$	-	-	$(2.5-R_{p0.2T}/R_{mT})Rp_{0.2T}$
	UPSET CONDITIONS	1.2 $[\sigma]$	1.6 $[\sigma]$	-	-	-
	EMERGENCY CONDITIONS	1.4 $[\sigma]$	1.8 $[\sigma]$	-	-	-
BOLTING JOINTS	NORMAL OPEARATING CONDITIONS	$[\sigma]_w$	-	1.3 $[\sigma]_w$	1.7 $[\sigma]_w$	-
	UPSET CONDITIONS	1.2 $[\sigma]_w$	-	1.6$[\sigma]_w$	2.0 $[\sigma]_w$	-
	EMERGENCY CONDITIONS	1.4 $[\sigma]_w$	-	1.8 $[\sigma]_w$	2.4 $[\sigma]_w$	-

TABLE 22. ALLOWABLE STRESSES FOR WWER PRESSURE VESSELS SUBJECTED TO SEISMIC EVENTS

LOADINGS	GROUP OF STRESSES	ALLOWABLE STRESSES	GROUP OF STRESSES	ALLOWABLE STRESSES
ALLOWABLEW STRESSES				
NORMAL + DBE	$(\sigma_s)_1$ $(\sigma_s)_2$	1.4 $[\sigma]$ 1.8 $[\sigma]$	$(\sigma_s)_{mw}$ $(\sigma_s)_{4w}$	1.4 $[\sigma]_w$ 2.2 $[\sigma]_w$
NORMAL + DBA	$(\sigma_s)_1$ $(\sigma_s)_2$	1.2 $[\sigma]$ 1.6 $[\sigma]$	$(\sigma_s)_{mw}$ $(\sigma_s)_{4w}$	1.2 $[\sigma]_w$ 2.0 $[\sigma]_w$
ALLOWABLE BEARING STRESSES				
NORMAL + DBE	$(\sigma_s)_s$	2.7 $[\sigma]$	-	-
NORMAL + DBA		2.5 $[\sigma]$	-	-
ALLOWABLE SHEAR STRESSES				
NORMAL + DBE	$(\tau_s)_s$	0.7 $[\sigma]$	$(\tau_s)_s$	0.7 $[\sigma]_w$
NORMAL + DBA		0.6 $[\sigma]$		0.6 $[\sigma]_w$

Calculation for earthquake effects must be performed for all sites characterized by MSK-64 grade 5 and more. In this case, new categories of stresses are defined:

$(\sigma_S)_s$ bearing stress including seismic loading,

$(\tau_S)_s$ shear stress including seismic loading.

Requirements for these stresses are given in Table 22, separately for the pressure vessel and the bolting joints. (In this table DBE is defined to be the design basis earthquake [with frequency 1/10,000 year] and DBA is the design basis accident [with frequency 1/100 year]).

Areas of the vessel analyzed and examined

Stress analysis of the vessel is carried out for the whole RPV volume; however emphasis is placed on those regions with stress concentrations. Therefore the ISIs concentrate on regions with:

- the highest stress levels,
- potential sources of defects (welding joints, cladding, etc.).

Special attention should be also paid to the cylindrical part of the vessel during the emergency situations like PTS events, loss of coolant accident, LOCA, etc.

Stress analysis methods

The Code provides unified methods for calculated and experimental determination of stresses, strains, displacements and loads. These methods are taken as recommended, other more precise methods can be also used. In this case, the organization performing this calculation is fully responsible for the results. Only computing programmes which have been approved by the regulatory body can be used for WWER stress analysis.

3.5.4. Design and analysis against non-ductile failure

All necessary requirements and analysis procedures as well as material data are given in the new version of the Code [35] (only the temperature approach was given in the previous version of the Code [32]). The whole procedure is summarized in the Chapter *"Calculation of Resistance Against Brittle Fracture"*. The Code can also be used for components manufactured before the Code was issued, which are now in operation, or under completion, if the procedure has been approved by the regulatory body. The procedures in the Code are based on the principles of LEFM with the use of static plain strain fracture toughness, K_{IC}, only. The Code provides allowable stress intensity factor curves (defined also by formulas) as a function of reference temperature, a postulated flaw and a K_I expression for normal operating conditions, pressure tests and upset conditions and emergency conditions. In principle, the procedure is very similar to the one from the ASME Code, some differences result from the different materials and reactor designs used.

A new regulatory Code Procedure for WWER reactor pressure vessel lifetime assessment during operation", МРК-СХР-2000 [38] was developed in Russia. This Code is establishes the rules for determination of the reactor pressure vessel residual lifetime. and is applied to the cylindrical part of reactor pressure vessel, exposed to neutron irradiation.

This Code was accepted by the Russian regulatory bodies.

Allowable stress intensity factors

The Code [34] gives as the main condition for fulfillment of component resistance against brittle failure the following formula:

$$K_I \leq [K_I]i \qquad (8)$$

where

$[K_I]i$ is the allowable stress intensity factor for regime i

i = 1 normal operating conditions
i = 2 pressure hydrotest (pneumatic pressure test) or upset conditions
i = 3 emergency conditions.

According to Code [38] the strength criteria for any point at the crack front in the base metal, can be written down as follows:

$$K_I = n_k K_{IP} + K_{IS} \leq [K_{IC}(T-T_k)] \qquad (9)$$

$$K_I = K_{IP} + K_{IS} \leq [K_{IC}(T-T_k-\Delta T)] \qquad (10)$$

where

K_{IP} is the stress intensity factor due to primary stresses (MPa $m^{1/2}$),K_{IS} is the stress intensity factor due to secondary stresses (MPa $m^{1/2}$).

Strength criteria for any point of the crack front in the cladding or contacting with cladding can be written as follows:

$$(K_I)_i = n_i \cdot K_{IP} + K_{IS} \leq K_{JC} \qquad (11)$$

where

$K_{JC} = \sqrt{\frac{J_C E}{1-\nu^2}}$, J_C is the critical value of the J-integral under brittle failure (if the crack ductile growth before brittle failure was less than 0.2 mm) or the J-integral value, determined on cladding J_R-curve for the crack ductile growth 0.2 mm.

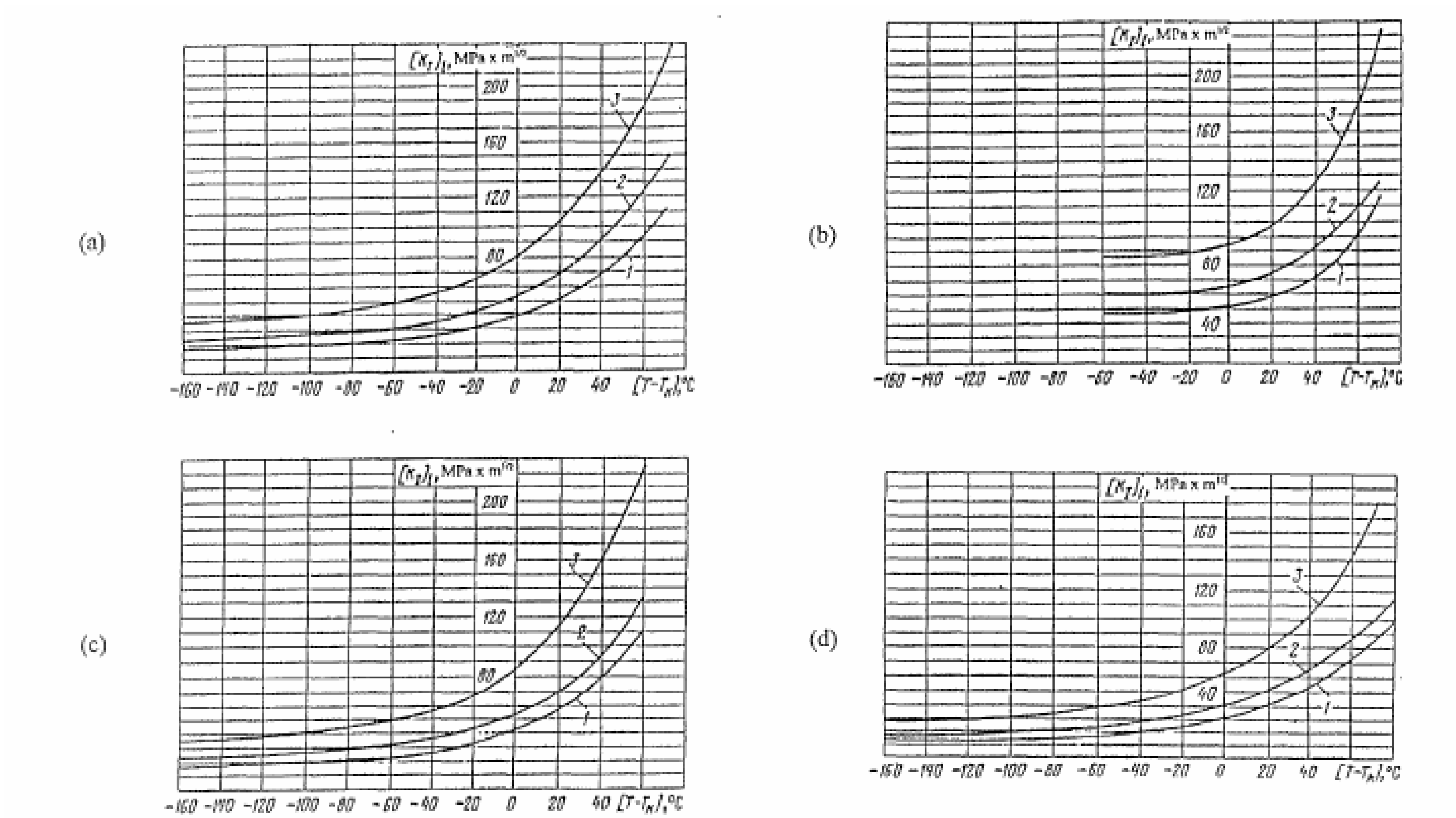
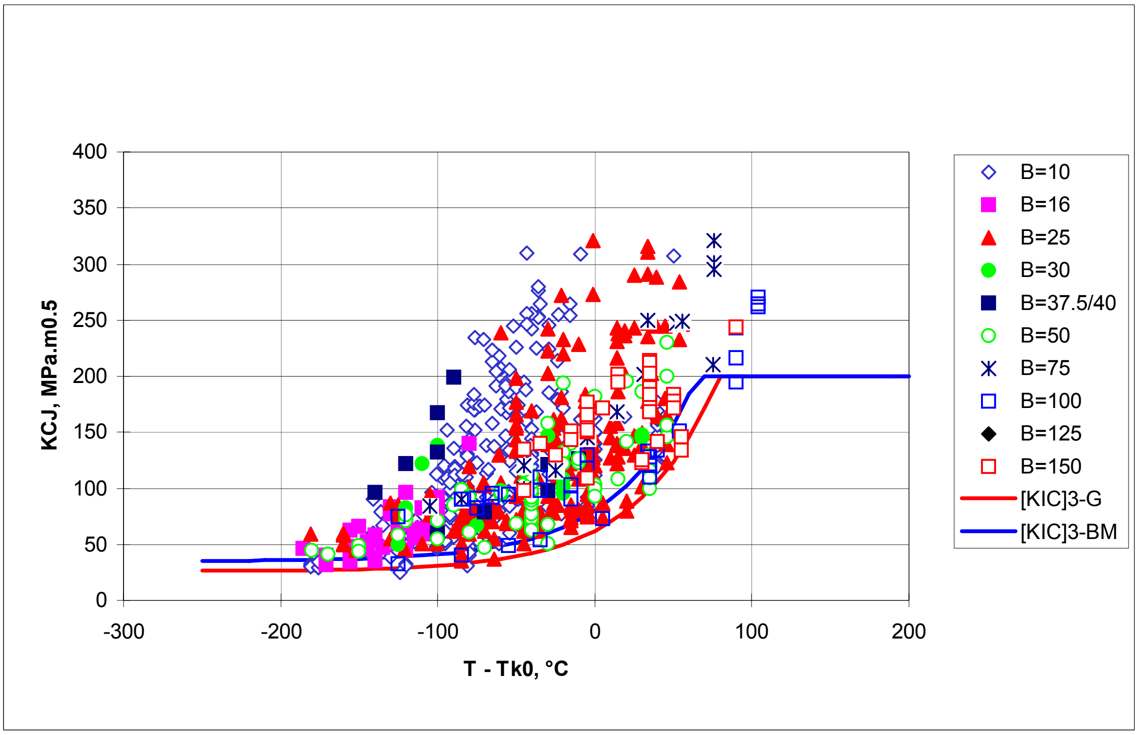
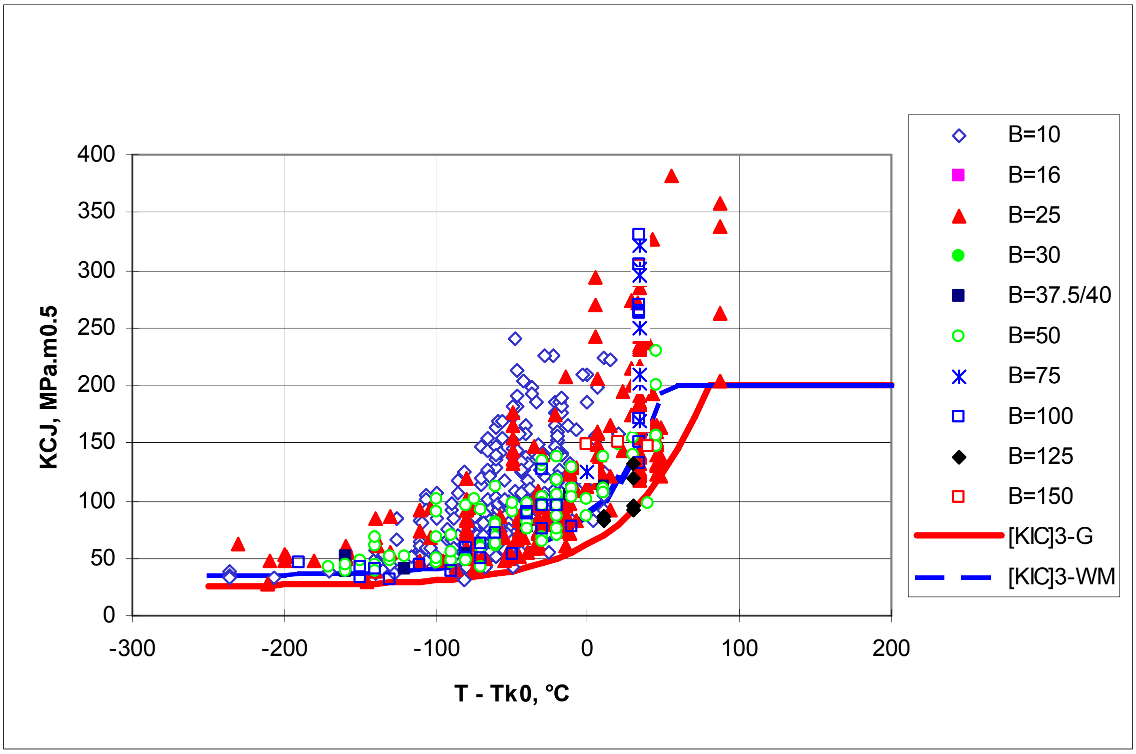


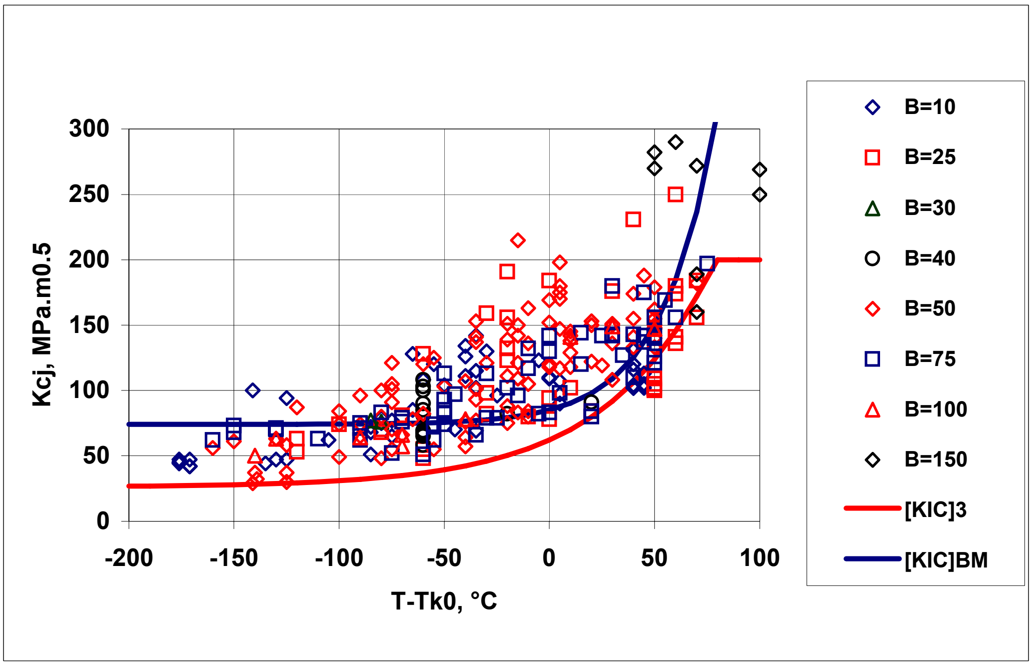
Fig. 22. The allowable fracture toughness curves in the Russian Code for three material types and for other low-Alloy steels. The curves labeled 1 are used for normal operating conditions, the curves labeled 2 are for pressure testing and upset conditions, and the curves labeled 3 are for emergency conditions, (a) Types 12Kh2MFA, 15Kh2MFA and 15Kh2MFAA steel, (b) Types 15Kh2NMFA and 15Kh2NMFAA steel (c) 15Kh2MFA, l5Kh2MFAA, 15Kh2NMFA and 15Kh2NMFAA weld metal, (d) Other low-alloy steels.



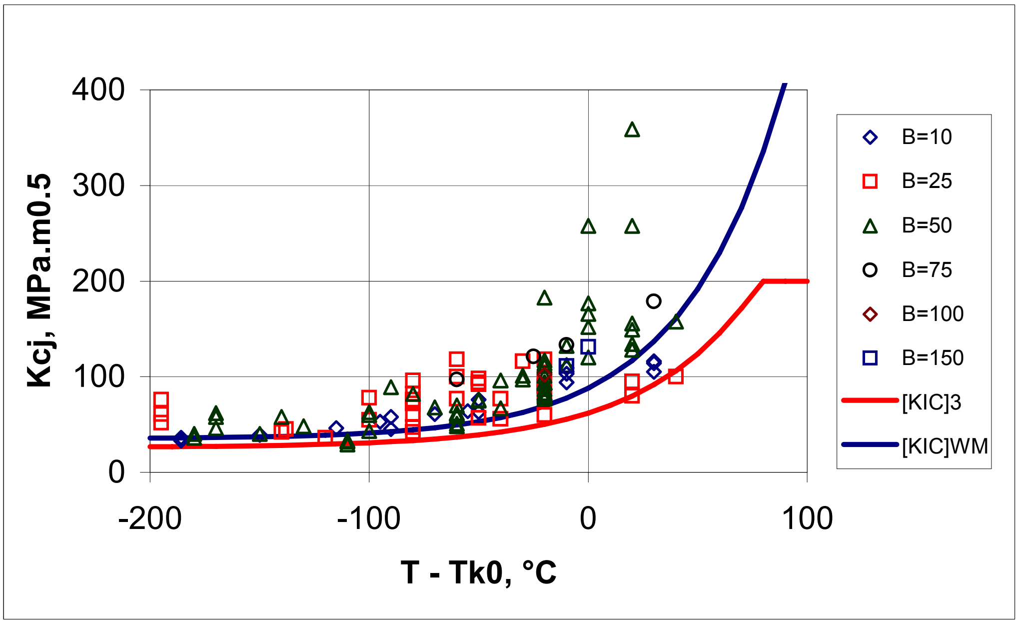
(a) Type 15Kh2MFA and 15Kh2MFAA base metals.



(b) Type 15Kh2MFA(A) weld metals.



(c) Type 15Kh2NMFA and 15Kh2NMFAA base metals.



(d) Type 15Kh2NMFA(A) weld metals.

Fig. 23. Fracture toughness (K_{CJ}) data plotted versus reference temperature (a) Type 15Kh2MFA and 15Kh2MFAA base metals, (b) Type 15Kh2MFA(A) weld metals, (c) Type 15Kh2NMFA and 15Kh2NMFAA base metals, (d) Type 15Kh2NMFA(A) weld metals. B is the specimen thickness, $[K_{IC}]_3$ is the generic curve of allowable stress intensity factors, $[K_{IC}]$-BM and $[K_{IC}]$-WM are specific curves for base metals and weld metals, resp.

The safety margins for different categories of conditions are as follows:

Normal operating conditions, i=1; n_k=2; ΔT =30 °C

Hydrotests, i=2; n_k=1.5; ΔT =30 °C

Upset conditions, i=3; n_k=1.25; ΔT =30 °C

Emergency conditions, i=4; n_k =1.1; ΔT =0 °C

A precise analysis (see Fig. 24) can also be performed to obtain more accurate results. The criterion for precise analysis can be written down as follows:

$$\frac{1}{B}\int_B \frac{(K_I(\varphi)-K_{\min})^4}{(K_{IC}(\varphi)-K_{\min})^4}\,dB<1 \tag{12}$$

where

$K_{IC}(\varphi)$ - fracture toughness distribution through the crack front (MPa $m^{1/2}$),

$K_I(\varphi)$ - stress intensity factor distribution through the crack front (MPa $m^{1/2}$).

$K_{\min}$ = 20 MPa $m^{1/2}$.

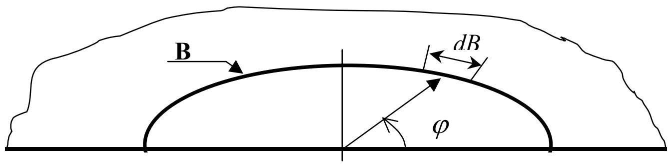


Fig. 24 Precise analysis.

The allowable stress intensity factor curves for three material groups and a set of general allowable stress intensity factor curves are plotted in Fig. 22. The curve labeled 1 on each plot is the K_{IC} curve for normal operating conditions, the curve labeled 2 is the K_{IC} curve for pressure testing and upset conditions and the curve labeled 3 is the K_{IC} curve for emergency conditions. The curves in Fig. 22a are for Types 12Kh2MFA, 15Kh2MFA and 15Kh2MFAA steels. The curves in Fig. 22b are for Types 15Kh2NMFA and 15Kh2NMFAA steels. The curves in Fig. 22c are for Type 15Kh2MFA, 15Kh2MFAA, 15Kh2NMFA and 15Kh2NMFAA weld metal.

In addition, the curves in Fig. 22d are general formulas for use with other low alloy steels. These curves were constructed from lower bound curves of all the relevant experimental data for each material type; almost no available data fall under the curves. The fracture toughness data (K_{IC} versus reference temperature) for the Type 15Kh2MFA and 15Kh2MFAA steel are plotted in Fig. 23a and 23b. The fracture toughness data for the Type 15Kh2NMFA and 15Kh2NMFAA steels are plotted in Fig. 23c and 23d. Then two curves of allowable stress intensity factors were constructed for each of the three operating conditions using two types of safety factors:

n_K: a safety factor applied to the stress intensity
n_T: a safety factor applied to the temperature.

One curve was obtained from the initial lower bound data curve by dividing K_{IC} by the safety factor n_K. The other curve was obtained by shifting the temperature scale by n_T. The values of n_K and n_T used for the three operating conditions were:

Operating condition	n_K	n_T
Normal operating conditions	2.0	+30°C
Pressure tests and upset conditions	1.5	+30°C
Emergency conditions	1.0	0°C

The final allowable stress intensity factor curves shown in Fig. 22 were then constructed by fitting a lower bound curve to the curves adjusted by n_K and n_T for each operating condition. The result was allowable stress intensity factor curves ($[K_{IC}]$i curves) as a function of reference temperature, defined as $[T-T_K]$, where T_K is the ductile to brittle transition temperature. As mentioned, allowable stress intensity factor curves were developed for three specific material types, as well as general ones. Fig. 25 shows a comparison of the general curves for the three operating conditions with the ASME K_{IC} and K_{IR} curves. The equations which describe the curves shown in Fig. 22 are listed in Table 23.

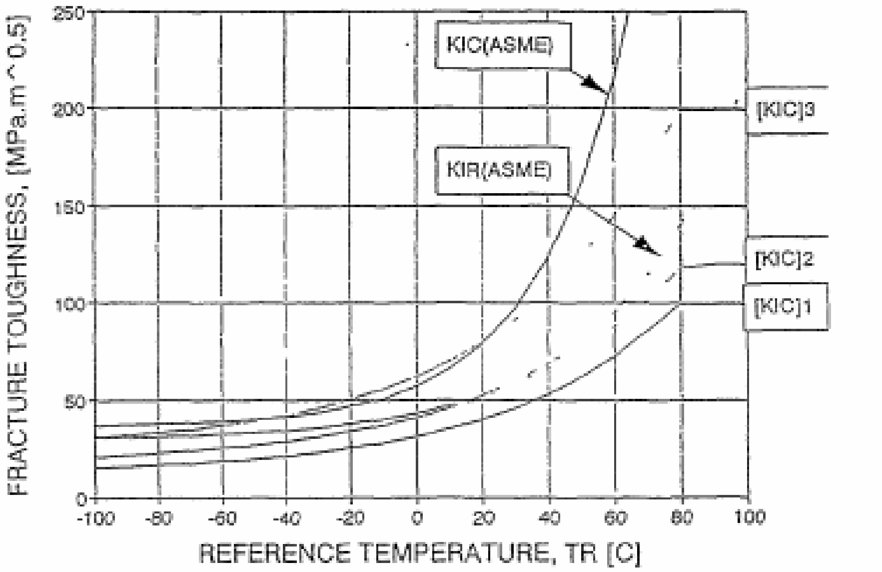


Fig. 25. Comparison of the allowable WWER fracture toughness curves for low-alloy steels with the K_{IC} and K_{IR} reference fracture toughness curves in the ASME Code $[K_{IC}]1$ is the allowable fracture toughness for normal operating conditions, $[K_{IC}]2$ is the allowable fracture toughness for hydraulic testing, and $[K_{IC}]$ 3 is the allowable fracture toughness for emergency conditions.

According to Code [38] the initial information on the fracture toughness is the base curve $K_{IC}(T-T_K)$ for the specimen thickness B=150 mm that corresponds to probability Pf=0.05 of finding of fracture toughness value being less than K_{IC} . (see Fig. 26, 27) One base curve for all WWER pressure vessels materials is proposed:K_{IC} = 23 + 48 exp [0.019(T-$T_к$)] (13)

Allowable values of the stress intensity factor for cracks with a crack front length B_i can be determined from the base curve by the following formula:

$$[K_I] = \left(\frac{\overline{B}}{B_i}\right)^{1/4}(K_{IC} - K_{\min}) + K_{\min} \tag{14}$$

where

$\overline{B}$ =150 mm,

$K_{\min}$ = 20 MPa $m^{1/2}$.

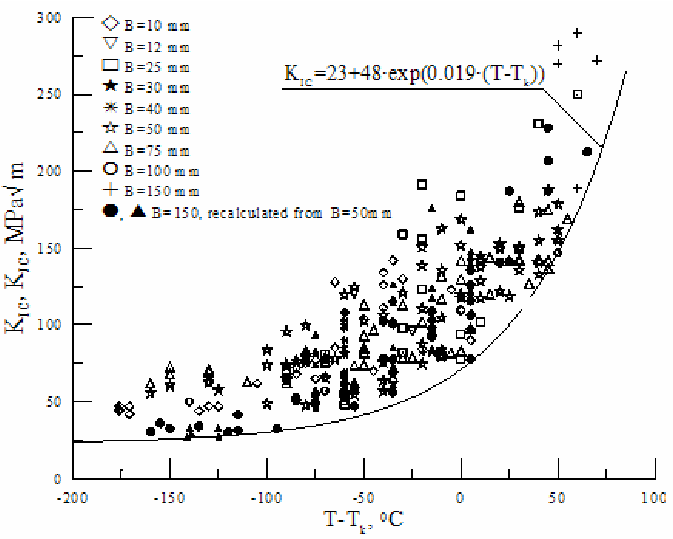


Fig. 26. Fracture toughness for 2Cr-Ni-Mo-V steel (initial state).

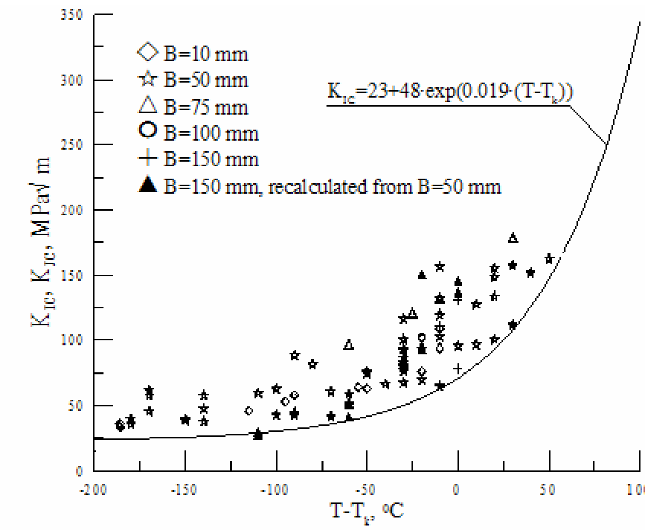


Fig. 27. Fracture toughness for welds of 2Cr-Ni-Mo-V steel (initial state).

It is necessary to mention that specific curves for allowable stress intensity factors, as described by formulas given in Table 23, are not fully conservative, i.e. they do not represent a lower bound curves of all experimental data, as it is seen in Fig.23. Mostly, they represent only 90% or even less probability curves and thus they can not be recommended for RPV integrity assessments especially during PTS regimes. Thus, only the generic curves are recommended in the VERLIFE procedure [39].

In practice, fracture toughness data for WWER materials in Russia are used from the material certification reports on materials which are more updated rather than outdated curves from the normative document PNAE-G-7-002-86 [40].

TABLE 23. METHODS FOR CALCULATING THE ALLOWABLE FRACTURE TOUGHNESS OF VARIOUS WWER MATERIALS

MATERIAL	$[K_{IC}]_1$	$[K_{IC}]_2$	$[K_{IC}]_3$
STEELS 12Kh2MFA, 15Kh2MFA, 15Kh2MFA-A	$17.5 + 22.5 \exp(0.02\ T_R)$	$23.5 + 30.0 \exp(0.02\ T_R)$	$35.0 + 45.0 \exp(0.02\ T_R)$
STEELS 15Kh2NMFA, 15Kh2NMFA-A	$37.0 + 5.5 \exp(0.0385\ T_R)$	$50.0 + 5.1 \exp(0.0041\ T_R)$	$74.0 + 11.0 \exp(0.0385\ T_R)$
WELDING MATERIALS FOR STEELS 15Kh2MFA, 5Kh2MFAA, 15Kh2NMFA, 15Kh2NMFA-A	$17.5 + 26.5 \exp(0.0217\ T_R)$	$25.0 + 27.0 \exp(0.00235\ T_R)$	$35.0 + 53.0 \exp(0.0217\ T_R)$
GENERAL – ALL MATERIALS	$13.0 + 18.0 \exp(0.02\ T_R)$	$17.0 + 24.0 \exp(0.018\ T_R)$	$26.0 + 36.0 \exp(0.02\ T_R)$

Calculated (postulated) defect

A postulated defect according to Code for Strength Calculations of Components and Piping of Nuclear Power Plants [36], i.e. for RPV design, was chosen to be much larger than any defect that could be missed during the pre-service or in-service non-destructive inspections. The postulated defect is defined as a semi-elliptical fatigue type crack with a depth (a) equal to 25% of the component thickness (S) without cladding and a crack shape equal to a/c = 2/3 where c is the crack length. These dimensions are independent of the vessel thickness and are applicable if the vessel thickness S fulfils the requirement

$$S \leq 8 \times 10^3 \left([K_I]_1 / R^T{}_{P0.2}\right)^2 \qquad (15)$$

This postulated defect is put into the calculations for the normal and upset conditions. For emergency conditions, defects which range in size from a = 0 to a = 0.25 S must be taken into account.

According to the procedure for determination of the operating life time of nuclear reactor vessels [38] for the unclad vessels the surface circumferential and axial semi-elliptical cracks with initial depth a_0=0.07S and length $2c_0$=$6a_0$ are considered (S – RPV wall thickness).

For vessels with cladding there are the following defects (depending on the presence and results of ISI of the metal state of corrosion-resistant cladding by non-destructive methods):

- surface semi-elliptical crack with initial depth a_0=SC+0.07S and length $2c_0$=$6a_0$ are considered (SC –cladding thickness, S – RPV wall thickness);
- subsurface elliptic crack, which is located in the base metal (or in the welded joint), a small axis of which is perpendicular to the vessel surface and a crack contour is in contact with interface "base metal (or welded joint) – corrosion-resistant cladding". Initial height of a crack (small axis) is equal to $2a_0$=0.07S, and length (large axis) is equal to $2c_0$=$6a_0$.

Cyclic growth for the considered period of operation is determined and finally the maximum calculated dimensions of $a_p=a_0+D_a$ and $c_p=c_0+D_c$ are determined. The calculation is carried out for crack depths (from 0 to a_p) at constant value c_p.

Stress intensity factors

The Code allows the analyst to determine the stress intensity factors using analytical, numerical, or experimental methods, but all must be approved by the Regulatory Body. The Codes also give formulas for cylindrical, spherical, conical, elliptical as well as flat elements, loaded by inner pressure and temperature effects. In these formulas, stresses are divided into membrane and bending components using an integral type of mean stress determination. For elements with concentrators (due to thickness changes, holes, or nozzles) special correcting coefficients are provided. All these formulas are supposed to be as conservative as possible. At the same time, finite element method (FEM) computer codes are widely used and allowed if they are sufficiently qualified and certified. These code results not only in temperature and stress field determination but also give stress intensity values for chosen postulated defects if a suitably fine FEM mesh is used.

Transition temperature shifts

Fracture toughness is a temperature dependent mechanical property of a material (fracture toughness depends also on load rate, but in the Code, only static fracture toughness, i.e. failure initiation, is taken into account). Therefore, reference fracture toughness curves are constructed using so-called reference temperatures. In the Russian Codes, the so-called *critical temperature of brittleness* is a basis for an assessment of resistance against brittle failure. This critical temperature of brittleness, T_K, is determined using notch toughness testing of Charpy-V type specimens, only. In principle, this temperature is defined as a temperature, at which the mean value from three notch toughness tests is equal to a critical value $(KCV)_c$ which is dependent on the yield strength ($R_{p0.2}$) of the material:

$R_{p0.2}$[MPa]	$(KCV)_c$[J.cm-2]	$(KV)_c$ [J]
less than 300	30	24
300-400	40	32
400-550	50	40
550-700	60	48

At the same time, at a temperature equal to $T_K + 30°C$ the following supplementary requirements must be fulfilled:

$$KCV > 1.5(KCV)_c \quad (16)$$

$$(KCV)_{min} > 0.7 \times 1.5\ (KCV)_c = 1.05\ (KCV)_c$$

$$(\text{fracture appearance})_{min} > 50\ \%\ (\text{fibrous fracture, }\%)$$

Differences between these critical temperatures, as determined experimentally for Types 15Kh2MFA and 15Kh2NMFA steel and ASTM A 533-B steel are:

$$\delta T = RT_{NDT} - T_K = \pm\ 10°C \quad (17)$$

Evaluation of the brittle resistance of the RPV at the design state is performed in accordance with the former Soviet "Code for Strength Calculation..." [36]. The evaluation is performed using linear elastic fracture mechanics techniques. Temperature and stress fields in the vessel during a PTS sequence are calculated for the whole vessel wall thickness, i.e., the austenitic cladding is taken into account, if it exists. A finite element method is usually used for the calculation of the temperature distribution in the wall, as well as the stress calculation.

The stress intensity factors, K_I, are determined only for the deepest part of each postulated defect and are calculated for the entire loading path and for a whole set of postulated defects with depths ranging from 0 to 25% of the wall thickness. These calculated stress intensity factors are then compared with the allowable stress intensity factors for emergency conditions, $[K_I]_3$ taking into account the temperature dependence of these factors. From those comparisons, the maximum allowable critical brittle fracture temperatures, $T_k^a(j)$, for the analysed PTS sequences are obtained. In other words, the K_I values are plotted versus the temperature at the deepest point in the crack during the whole PTS sequence. Then the $[K_I]_3$ curve is shifted to a higher temperature, up to the point where it contacts the K_I curve. The value of the shift determines the maximum allowable critical temperature, $T_k^a(j)$ for the j event which fulfils the requirement that K_I is lower than $[K_I]_3$. The lowest of these temperatures for the whole set of analysed PTS sequences is taken as the maximum allowable critical brittle fracture temperature, T_k^a. This temperature is material independent and depends only on the RPV and reactor design, especially on the PTS sequences. This temperature is then compared with the critical brittle fracture temperature T_k of the analysed vessel. Decisions on further operation can be made based on this comparison.

Transition temperature shifts

The brittle to ductile transition temperature (critical temperature of brittleness) of the WWER pressure vessel materials is time or use dependent, since many damaging mechanisms can affect it, and can be expressed in the form:

$$T_k = T_{k0} + \Delta T_F + \Delta T_T + \Delta T_N \quad (18)$$

where

T_K is the instant critical temperature of brittleness
T_{k0} is the initial critical temperature of brittleness
ΔT_F is the shift of critical temperature due to radiation embrittlement
ΔT_T is the shift of critical temperature due to thermal ageing
ΔT_N is the shift of critical temperature due to cyclic damage

Individual component shifts are also defined in the Code where formulas for their determination are also given.

The transition temperature shift due to radiation embrittlement (ΔT_F) can be expressed as:

$$\Delta T_F = A_F\,(F \times 10^{-22})^{1/3} \tag{19}$$

where

A_F is the radiation embrittlement coefficient
F is the neutron fluence with energies greater than 0.5 MeV.

Fluences with energies higher than 0.5 MeV are used in the Soviet Code, as it is suggested that this criterion better describes the damaging part of the fluence. The ratio between fluences with energies higher than 0.5 and energies higher than 1.0 MeV depends on the place in the reactor where it is determined — for PWR types, it mainly depends on the reflector thickness and the surveillance position, inner or outer vessel wall. For the inner surface of a WWER pressure vessel this ratio is approximately:

$$F(E_n \geq 1.0\text{ MeV}) / F(E_n \geq 0.5\text{ MeV}) \propto 0.6 \tag{20}$$

The coefficient A_F depends not only on the radiation temperature but also on material composition, mainly on the phosphorus, copper and nickel contents (for 15Kh2NMFA). The Code provides specific values or formulas for the AF coefficients which are necessary to put into the calculations. These values have been obtained as upper bound values from experimental data. All the necessary data are summarized in Table 24.

Thermal ageing should also be taken into account, and for the WWER-440 and WWER-1000 RPV materials, this shift is given as:

$$\Delta T_T = 0°\text{C for Type 15Kh2MFAA steel} \tag{21}$$

$$= +\,5°\text{C for Type 15Kh2NMFAA of steel (after the first 50,000 h of operation)}$$

The shift ΔT_N represents the changes in the material properties caused by low-cycle fatigue damage. All transients are considered, including heatup and cooldown, pressure testing, scram, etc. For WWER pressure vessel materials, the Code provides the following formula to be used in the calculations:

$$\Delta T_N = 20\,A\ [°\text{C}] \tag{22}$$

where A is the usage factor from the fatigue calculations, which means that the maximum shift due to cyclic damage is equal to + 20°C. This shift is, of course, only taken into account in locations with high stress concentrators, where a high usage factor is obtained - i.e. mostly for nozzles.

However, it must be mentioned that both of the WWER pressure vessel materials are cyclically softened and thus this formula gives very conservative values. In fact, some negative shift of the transition temperature is usually found during the early part of the fatigue life.

The Code strictly requires a material surveillance programme for all reactor vessels. Requirements for the type of specimens and the time schedule for their withdrawal are also presented.

TABLE 24. WWER RADIATION EMBRITTLEMENT COEFFICIENTS

MATERIAL		IRRADIATION TEMPERATURE T_{irr} [°C]	IRRADIATION EMBRITTLEMENT COEFFICIENT A_F [°C]
15Kh2MFA	BASE METAL	250	22
		270	18
		290	14
	A/S WELD METAL	250	800(P+0.07 Cu)+8
		270	800(P+0.07 Cu)
15Kh2MFA-A	BASE METAL	270	12
		290	9
	A/S WELD METAL	270	15
		290	12
15Kh2NMFA	BASE METAL	290	29
15Kh2NMFA-A	BASE METAL	290	23
	A/S WELD METAL	290	20

3.5.5. WWER heatup and cooldown limit curves for normal operation

Heatup and cooldown limit curves (P-T limit curves) are calculated using a linear elastic fracture mechanics approach and a reference critical (brittle) temperature, T_k, defined on the basis of Charpy V-notch impact tests, only, but taking into account the potential effects of degrading mechanisms such as radiation embrittlement, thermal ageing and fatigue damage.

The allowable stress intensity factors values are shown in Table XXIII. They were constructed from the lower bound fracture toughness values for the listed materials and certain prescribed safety factors. Then, allowable P-T limit curves are obtained when:

$$K_I(T) < [K_I]i \qquad (23)$$

where

i = 1 for normal operating conditions, and

i = 2 for hydrostatic testing.

The stress intensity factors, $K_I(T)$, are calculated for the "postulated defect" discussed in Section 3.5.4 above, which is assumed to be at a surface without cladding and semi-elliptical in shape with a depth equal to 25% of the wall thickness and an aspect ratio, a/c. equal to 2/3. The defect is assumed to be perpendicular to the principal stresses. Only the deepest point of the defect is considered when calculating the stress intensity factors. The following formula is recommended:

$$K_I = \eta(M_m\sigma_m + M_b\sigma_b)(\pi a)^{1/2}Q^{-1} \quad (24)$$

where

η is a correction to the stress concentration (= 1 for a cylindrical part)

σ_m is the membrane stress

σ_b is the bending stress

M_m is a membrane correction factor

M_b is a bending correction factor

a is a crack depth (m)

Q is a shape factor.

The mechanical, as well as the thermal stress components, are added together, and the membrane and bending stress components are then derived using summary stress integration through the vessel wall. The following type of equation is obtained when the required dimensions and aspect ratio of the postulated defect are put into Equation (24):

$$K_I = \eta(0.7\sigma_m + 0.4\sigma_b)(S)^{1/2} \quad (25)$$

where S is the RPV wall thickness (m).

This formula is then used for calculation of the P-T limit curves. It must be also mentioned that the maximum allowable heatup and cooldown rates are 30 K/h, only.

The analyst can determine the stress intensity factors using analytical, numerical, or experimental methods, but all methods must be approved by the Regulatory Body.

3.5.6. IAEA Guidelines for PTS evaluation

The need for detailed guidance for the PTS analysis for WWER plants has been identified through the IAEA activities. The guidelines provide advice to justify RPV integrity for nuclear power plants with WWER type reactors. The guidelines provide advice on individual elements of the PTS analysis, such as acceptance criteria, selection and categorization of initiating events to be considered, thermal hydraulic analysis, structural analysis including fracture mechanics assessment, evaluation of material properties and neutron field calculations. [39]

The most important changes in comparison with original Russian Code [36] are related to:

- recent development in the field of emergency operation procedures (EOPs) was taken into account,
- design K_{IC} curve,
- postulated defects size and shape,
- use of safety factors,
- introduction of the "Master Curve" approach.

Stress intensity factors

The stress intensity factor K_I should be evaluated for the crack front with the highest crack loading and subsequently compared allowable stress intensity factors, K_{IC}. Usually, it is sufficient to evaluate K_I for the deepest point of the crack front and for the intersection of the crack front with the boundary between cladding and base or weld metal (for cladded RPV).

Postulated defects

The postulated defects are surface or subsurface cracks, located in the limiting areas of the vessel. In selection of the limiting areas of the vessel, consideration should be given to the stress level, to the material degradation and to the results of the non-destructive testing. The orientation of the postulated defect should be considered axial and circumferential depending on the direction of the maximal principal stress.

The postulated defect should be defined in the following way:

- For uncladded vessels the postulated defect is a surface semi-elliptical crack with depth up to ¼ of the RPV wall thickness and with aspect ratio a/c of 0.3,

- For cladded vessels, cladding integrity of which is verified by non-destructive testing and its mechanical properties are known, the postulated defects are undercladding elliptical as well as semi-elliptical cracks with depth up to ¼ of the RPV wall thickness, and with aspect ratio a/c, resp. 2a/c of 0.3,

- For cladded vessels, where limited or no information on cladding exists, the postulated defect is surface through cladding semi-elliptical crack with depth up to ¼ of the RPV wall thickness and with aspect ratio a/c of 0.3.

Usually, the analyses of cracks with aspect ratio of 0.3 and 0.7 are sufficient.

Defect sizes smaller than ¼ of the wall thickness could be used for the RPV integrity assessment under PTS loading of plants under operation if it is possible to demonstrate the required non-destructive testing reliability and if permitted by the national regulatory requirements. The size of the postulated defect could be selected with respect to the size of realistic manufacturing defect probable to exist in the vessel. Determination of postulated defects' sizes should take into account international practices, i.e. application of safety factor $n_a = 2$.

Safety factors

To demonstrate the RPV Integrity for a specified transient, two following conditions must be met simultaneously for the postulated crack with depth a:

$$n_K \cdot K_I (T,a) \leq [K_{IC}(T)] \quad (26)$$

$$K_I (T,a) \leq [K_{IC}(T-\Delta T)] \quad (27)$$

The parameters n_K and ΔT are safety factors with respect to the origin of uncertainties in the overall PTS analyses. In Table 25, the recommended values of safety factors are given. Other values could be also used, if justified.

TABLE 25. SAFETY FACTORS

SAFETY FACTOR	ANTICIPATED TRANSIENT	POSTULATED ACCIDENT
n_K	$\sqrt{2}$	$1^{x)}$
ΔT (°C)	30	0

$^{x)}$ In case of postulated defect size smaller than 1/8 S, $n_K = 1.1$ is recommended

"Master Curve" Approach

RPV integrity assessment can be also performed using the "Master Curve" approach. In such a case, allowable stress intensity factor values are determined with the use of an experimentally determined transition temperature T_0 (instead of critical brittle fracture temperature T_k from Charpy-V notch impact specimens) obtained from testing static fracture toughness of surveillance specimens and/or specimens from template cut from RPV wall. Neutron fluence of these specimens should be close to the analysed state of the RPV; in this case no initial values of any transition temperature (neither T_{k0} nor T_0^{ini}) of tested material are necessary. Temperature T_0 for the analysed state of the RPV is determined using single or multiple temperature method in accordance with the ASTM standard E 1921 [41].

Allowable stress intensity factors are then given as a 5% lower tolerance bound by the equation

$$[K_{IC}]_{25mm} = 25.4 + 37.8 \exp [0.019 (T-T_0)] \quad (28)$$

which is valid for the specimen thickness/crack length equal to 25 mm.

For cases when crack front length B is larger than 25 mm, the following re-evaluation of the aforementioned dependence is recommended:

$$[K_{IC}]_{Bi} = (B_{25}/B_i)^{1/4} ([K_{IC}]_{25mm} - K_{min}) + K_{min} \quad (29)$$

where K_{min} is a minimum value of fracture toughness of the material and is usually taken equal to 20 MPa.m$^{0.5}$.

The RPV integrity is assured if the following equation is fulfilled:

$$K_I(T, a, B_i) < [K_{IC}(T)]_{Bi} \quad (30)$$

3.5.7. VERLIFE procedure

WWER operating countries – Czech Republic, Slovak Republic, Hungary, Finland and Bulgaria co-operated in preparation of the VERLIFE – "Unified Procedure for Lifetime Assessment of Components and Piping in WWER NPPs". This procedure was completed in 2003 within the EU 5th Framework Programme and now is under acceptance procedures by regulatory bodies of aforementioned countries. [42]

This procedure considered Russian codes and rules applied for the design and manufacturing of WWER components and incorporated also some approaches used in assessment of PWR reactors. Last developments in the field of material science and fracture mechanics, like "Master Curve" approach is also included.

Main difference from the aforementioned procedures are as follows:

- two equivalent approaches are given, either "Master Curve" approach or transition temperature T_k approach based on Charpy V-notch impact tests,
- crack arrest approach is also allowed for specific cases.

Allowable stress intensity factors

- for "Master Curve" approach:

$$K_{JC}^{5\%}(T) = \min\{25.2 + 36.6 \cdot \exp[0.019 \cdot (T-RT_0)]; 200\} \quad (31)$$

$$[K_{IA}]_3(T) = \min\{25.2 + 36.6 \cdot \exp[0.019 \cdot (T-RT_0-60)]; 200\} \quad (32)$$

- for transition temperature approach:

$$[K_{IC}]_3(T) = \min\{26 + 36 \cdot \exp[0.020 \cdot (T-T_k)]; 200\} \quad (33)$$

$$[K_{IA}]_3(T) = \min\{26 + 36 \cdot \exp[0.020 \cdot (T-T_k-30)]; 200\} \quad (34)$$

These equations are valid for emergency conditions (i=3), for other conditions, i.e. for i=1 and i=2, same safety factors as in the Russian Code are used.

Postulated defects:

If ISIs are performed with devices, procedures and personnel qualified according to requirements of a regulatory organisation, the maximum postulated crack depth a_{calc} may be defined on the basis of the plant specific non-destructive testing qualification criteria. In this case, the value a_{calc} is taken equal to higher of the two following values:

(i) "high confidence of detection" crack depth with applied safety factor 2,

(ii) "high confidence of sizing" crack depth without applied safety factor (safety factor = 1).

The recommended value corresponding to application of advanced qualified non-destructive testing techniques is a_{calc} = 0.1 s.

If the conditions concerning the qualification of non-destructive testing mentioned in the previous paragraph aren't satisfied, the maximum postulated crack depth shall be defined as:

a_{calc} = 0.25 s.

The postulated defects are defined as semi-elliptical cracks with aspect ratios:

a/c = 0.3 and a/c = 0.7.

Two orientations of postulated crack shall be considered: perpendicular to direction of first principal stresses and perpendicular to direction of second principal stresses, i.e. in the case of cylindrical vessel perpendicular to circumferential direction and perpendicular to axial direction.

3.6. DESIGN BASIS IN JAPAN

Design requirements for the RPV are prescribed by METI Notification No.501 [43] and JSME Code on Code for Design and Construction for Nuclear Power Plants, JSME SNA2-2002 [44], which are based on ASME Boiler and Pressure Vessel Code, Section III. In addition, JEAC 4206-2000, published in 2000 by the Japan Electric Association [45], prescribes experimental methods to confirm the integrity of nuclear power plant components against non-ductile failure. These methods include the linear elastic fracture mechanics analysis method and the PTS evaluation method. JEAC4206 incorporates US NRC 10 CFR Part 50 Appendix G (1995) and Appendix H (1995), the ASME Boiler and Pressure Vessel Code Section III, Nuclear Power Plant Components (1998); K_{IR} equations are slightly different from those of ASME Section III because they take into account Japanese experimental data.

Two equations are provided for 1 path bead drop-weight and 2 path bead drop weight tests:

$$K_{IR} = 29.46 + 15.16 \exp [0.0274 (T-RT_{NDT})] \text{ for 1 path bead;} \quad (35)$$

$$K_{IR} = 29.43 + 1.344 \exp [0.0261 (T-RT_{NDT}+88.9)] \text{ for 2 path bead} \quad (36)$$

where

K_{IR}= reference stress intensity factor in SI units (MPa $m^{0.5}$) as a function of the metal temperature T (°C) and the metal reference nil-ductility temperature RT_{NDT}.

In 2003, the addendum of JEAC 4206 [46] was published, which approved the use of K_{IC} calculated by the following equation for pressure- temperature limits of the operation condition I, II and the hydraulic pressure/ leak test condition instead of K_{IR}.

$$K_{IC} = 36.48 + 22.78 \exp [0.036 (T-RT_{NDT})] \quad (37)$$

4. AGEING MECHANISMS

This section describes the age related degradation mechanisms that could affect PWR RPV components and evaluates the potential significance of the effects of these mechanisms on the continued safety function performance of these components throughout the plant service life.

The set of age related degradation mechanisms evaluated in this section is derived from a review and evaluation of relevant operating experience and research. This set consists of the following mechanisms:

(1) Radiation embrittlement

(2) Thermal ageing

(3) Temper embrittlement

(4) Fatigue

(5) Corrosion

  (a) Intergranular attack and PWSCC of Alloy 600 components, Alloy 82/182 welds, radial keys, etc.

  (b) General corrosion and pitting

  (c) Boric acid corrosion

(6) Wear.

The technical evaluation of a particular age related degradation mechanism and its effects on the continued safety or functional performance of a particular PWR RPV component leads to one of two conclusions: (1) the degradation mechanism effects are potentially significant to that component and further evaluation is required relative to the capability of programmes to effectively manage these effects; or (2) the age related degradation effects are not significant to the ability of that component to perform its intended safety function throughout the remainder of plant life. For the latter case, specific criteria and corresponding justification are provided in this section. These criteria can be used as the basis for generic resolution of age related degradation mechanism/component issues.

4.1. RADIATION EMBRITTLEMENT

4.1.1. Radiation embrittlement of western PWR pressure vessels

The degree of embrittlement and hardening induced in ferritic steels after exposure to fast neutron radiation is an issue of the utmost importance in the design and operation of NPPs. The area of the RPV surrounding the core (called the beltline region) is the most critical region of the primary pressure boundary system because it is subjected to significant fast neutron bombardment. The overall effect of fast neutron exposure is that ferritic steels experience an increase in hardness and tensile properties and a decrease in ductility and toughness, under certain conditions of radiation.

For example:

(1) *Effect of neutron fluence* on radiation hardening and embrittlement has been reported to be significant at fluences above 10^{22} n/m^2 (E >1 MeV). Unless a steady state or saturation condition is reached, an increase in neutron fluence results in an increase in RT_{NDT}, yield strength and hardness, and a decrease in the Charpy toughness, also in the upper shelf

temperature region. There are significant variations in the fluence and radiation damage around the circumference and in the longitudinal direction of RPVs.

(2) *Alloy composition* (especially when consideration is given to impurity copper and phosphorus and alloying element nickel) is known to have a strong effect on radiation sensitivity. Data have been generated on both commercial and model alloys to show the effects of alloy composition.

(3) *Radiation temperature* has long been recognized to have an effect on the extent of the radiation damage. Data from the early 1960s demonstrated that the maximum embrittlement occurred during radiation at temperatures below 120°C (250°F). Recent studies have reported a decrease in radiation embrittlement at higher temperatures (>310°C), which is attributed to the dynamic in-situ "annealing" of the damage.

(4) *Microstructural characteristics*, such as grain size and metallurgical phases (lower or upper bainite, ferrite), can influence the severity of radiation damage associated with a given fluence.

(5) *The neutron flux energy spectrum* contributions to the embrittlement behaviour of ferritic steels are secondary effects. However, recent reactor experience has suggested that, under certain conditions, the flux spectrum may influence the degree of radiation embrittlement caused in ferritic steels.

The most important parameters listed above are fluence and alloy and impurity content. The deleterious effect of copper (Cu) as an impurity element on radiation embrittlement and hardening of pressure vessel steels and welds was recognized nearly 20 years ago. The dramatic increase in ductile-to-brittle transition temperature and reduction in upper shelf energy (USE) observed in a variety of pressure vessel welds after neutron radiation at ~288°C was broadly correlated with the nominal impurity Cu content in the steels. The increased sensitivity to embrittlement was more pronounced for welds because Cu-coated welding rods had frequently been used in the fabrication of the reactor vessels leading to Cu levels of ~0.3 weight %. For an equivalent copper level, the cast structure of weld metals is more sensitive to neutron radiation damage than the base metal.

Early methods used to quantify the effect of impurity elements on radiation sensitivity in western RPV materials indicated that both copper and phosphorus played a role [47, 48]. Later on, USNRC Regulatory Guide 1.99, Revision 2 omitted the effect of phosphorus but included nickel as a factor [49]. As a result, it is often assumed that copper and nickel play the dominant role in creating sensitivity to neutron radiation in low phosphorus steels. However, there are variations in alloying content and impurity element ranges in the various countries in which the RPV materials were produced and it is still necessary to consider the contribution of phosphorus, particularly when low levels of copper and nickel are present.

The radiation embrittling mechanism attributed to copper impurity level is well understood in terms of small copper-rich clusters or precipitates formed under the creation of minute matrix damage caused by fast neutron bombardment. Such precipitates can act as blocks to dislocation movement and cause hardening and embrittlement. Hawthorne and coworkers [50] examined the action of Cu and P in a variety of A533B and A302B steels. Phosphorus contents greater than 0.014 weight% exerted a strong effect on the sensitivity of A302B steels to radiation embrittlement.

Unlike Cu and P, the role of nickel (Ni) in radiation hardening/embrittlement has been unclear. The contradictory reports concerning the influence of Ni in the embrittlement behaviour of RPV steels indicated that its effect was a subtle one. The effect of Ni can be demonstrated qualitatively by studying the HY and A350LF steels (~3 weight % Ni). Although studies by Lucas et al. [51] and Igata et al. [52] showed no effect of Ni on radiation embrittlement of RPV steels, several other studies show a significant effect. In 1981, Guionnet et al. [53] concluded that Ni was deleterious to the behaviour of A508 irradiated to a fluence of 5×10^{23} n/m^2 at 290°C. A pronounced Ni effect in increasing the radiation sensitivity of high Ni (0.7 weight %) welds was reported by Hawthorne [54]. Similarly, Fisher and Buswell [55] noted that high Ni steels (i.e., those containing >1% Ni) were much more sensitive to neutron radiation than steels containing <0.85% Ni. Soviet experience with chromium (Cr) and Ni bearing RPV steels also indicated that Ni exerted a pronounced effect on embrittlement behaviour [56].

Odette and Lucas [57] examined the effect of Ni (0 to 1.7 weight %) on the hardening behaviour of A 533-B type steels as a function of neutron fluence, flux, temperature and manganese and copper content. Irradiations at fluxes of 5×10^{15} and 5×10^{16} n/m^2 / s gave final fluences of 9×10^{22} to 1.5×10^{23} n/m^2 (E>1 MeV). Low fluence irradiations were done at 306°C and 326°C; the higher fluences were accumulated at 271°C to 288°C. The results indicated that Ni increased the sensitivity to radiation embrittlement in these materials, with increasing fluence, lower flux levels, lower irradiation temperature and increased manganese (Mn) levels causing more damage. The synergisms and complex nature of the response of the alloys examined makes a complete interpretation of the mechanisms difficult.

Although the roles of Cu, P and Ni as promoters of radiation hardening and embrittlement are well-recognized, the contribution of other elements such as manganese (Mn), molybdenum (Mo), Cr, arsenic (As) and tin (Sn), to the radiation induced behaviour of RPV steels has not been unambiguously identified.

Significance for western PWR pressure vessels

A fluence value of 1×10^{22} n/m^2 (E >1 MeV) is approximately the threshold for neutron induced embrittlement of the ferritic steels used in western PWRs. Therefore, the beltline region is the region most likely to undergo significant changes in mechanical properties due to neutron radiation. Components made of materials such as Alloy 600 or Alloy 182 are less susceptible to neutron embrittlement. The following components are subjected to lifetime fluences less than 1×10^{22} n/m^2 (E >1 MeV) or are made of materials not susceptible to neutron embrittlement:

- Core supports,
- Nozzles,
- Head penetrations,
- Bottom head,
- Top head,
- Vessel flange,
- Closure studs,
- Safe ends.

Therefore, neutron embrittlement is potentially significant only for that part of the RPV shell beltline region which is located in a high flux region.

4.1.2. Radiation embrittlement of WWER pressure vessels

Two different types of steels, 15Kh2MFAand 15Kh2NMFA in two qualities (-A or -AA) were used to fabricate the WWER pressure vessels. These steels are affected by different embrittlement mechanisms and behave differently from each other as discussed below.

The 15Kh2MFA type steel is used for the WWER-440 V-230 pressure vessels and the Loviisa pressure vessels. It has almost no nickel and so its behavior is controlled by its phosphorus and copper impurity content. Contrary to western practice, in which specifications strictly limit the phosphorus content in both the base and weld metal, the original specifications used in the CMEA countries imposed only very mild requirements on the phosphorus content, allowing as much as 0.040 weight %. The phosphorus content was originally not even measured in the weld metal; it was measured only in the welding wires. The resulting phosphorus contents in some RPVs are listed in Table 26 and are mostly close to or even higher than the 0.040 weight % limit in the weld metal. The copper content in the 15Kh2MFA type steel typically ranges from 0.15 and 0.20 weight % and so its effect on embrittlement is small. Thus *phosphorus is practically the only controlling impurity* in the steel used for the WWER V-230 type of RPVs.

Phosphorus causes embrittlement because of thermal and radiation induced diffusion to and segregation at the grain boundaries and also inside grains that precipitate together with other elements like copper, manganese etc. However, intercrystalline (intergranular) fracture of Charpy surveillance specimens is very rare, even after high neutron fluences. Most of the Charpy failures are transgranular failures. Therefore, the effects of the high phosphorus segregation in only partially understood.

The 15Kh2MFA vessels become, of course, very embrittled during radiation and most of them have been annealed in the last several years. Radiation embrittlement remains the main concern for these types of vessels.

The beltline regions of the WWER-440/V-213 pressure vessels were also manufactured from 15Kh2MFA steel, but many of the V-213 pressure vessels have low phosphorous and copper contents and are similar in impurity content to the WWER-1000 pressure vessels with strict requirements on the residual element (Cu, P, As, Sn and Sb) content, i.e. their steel and welds are of 15Kh2NMFAA quality. Thus, radiation embrittlement does not seem to be a limiting factor for a 40 year vessel lifetime. Moreover, the degree of radiation embrittlement of the WWER-440/V-213 pressure vessels is lower than that of the western PWR vessels made of ASME A 533-B material even though the V-213s are irradiated at a relatively low temperature, about 265°C. This is probably due to the higher structural stability of the 15Kh2MFAA type steel, relative to the A 533-B steel, caused by the presence of vanadium carbides, which are very stable, together with the steel microstructure and the absence of nickel.

TABLE 26. COPPER AND PHOSPHORUS CONCENTRATIONS (WEIGHT %) IN THE BASE METAL AND WELD METAL OF WWER-440 RPVS

Plant	Cladding	Weld metal No. 4			Base metal		
		Cu	P	METHOD	Cu	P	METHOD
KOLA 1	N	0.13 0.146	0.032 0.033	calc. scrape inside	-	0.012	certif.
KOLA 2	N	0.154	0.036 0.0375	calc. scrape inside	-	0.012	certif.
ARMENIA 1	N	0.16	0.030	scrape inside	-	0.013	certif.
ARMENIA 2	Y	-	-	-	-	-	-
NOVOVORONEZH 3	N	0.15	0.031	template	0.16	0.012	template
NOVOVORONEZH 4	N	0.17	0.030	template	-	0.011	certif.
KOZLODUY 1	N	0.12	0.0515 0.036	scrape inside calc.	0.15	0.010	certif.
KOZLODUY 2	N	0.18	0.036 0.0375	template	0.17	0.017	template
KOZLODUY 3	Y	0.20	0.036	certif. based on test coupon	0.17	0.016	certif.
KOZLODUY 4	Y	0.04	0.021	certif. based on test coupon	0.10	0.012	certif.
BOHUNICE 1	Y	0.15 0.103	0.035 0.043	certif. scrape outside	0.13 0.091	0.012 0.014	certif. scrape outside
BOHUNICE 2	Y	0.20 0.109	0.036 0.026	certif. scrape outside	0.08 0.082	0.010 0.010	certif. scrape outside
GREIFSWALD 1	N	0.104 0.10	0.034 0.043	scrape inside template	0.17 0.18	0.010 0.015	certif. template
GREIFSWALD 2	N	0.157 0.15	0.037 0.032 0.036	certif. scrape inside	-	0.012	certif.
GREIFSWALD 3	Y	0.12	0.035	certif. based on test coupon	-	0.012	certif.
GREIFSWALD 4	Y	0.16	0.035	certif. based on test coupon	0.12	0.016	certif.

The beltline regions of the WWER-1000 pressure vessels are fabricated from Type 15Kh2NMFAA steel. This steel has almost no vanadium and much more nickel than the Type 15Kh2MFAA material used for the WWER-440 vessels. As mentioned in Section 2, a nickel rather than vanadium alloy steel was chosen for the WWER-1000 vessels so that it would be easier to weld the relatively large WWER-1000 forgings at lower pre-heating temperature, while still retaining the desired strength characteristics. The limits on the residual element content for this steel are very strict (similar to 15Kh2MFAA type).

The nickel in the base metal was controlled to values between 1.00 and 1.50 weight %, however, the nickel in the weld metal of many of the WWER-1000 RPVs is as high as 1.90 weight %. Thus, the nickel content in the weld metal is the controlling element for radiation embrittlement as impurity contents (Cu, P, As, Sn and Sb) are very low – see Table VII. The inlet water temperature of the WWER-1000 plants is higher than in the WWER-440 plants by about 20°C (i.e. 288°C) and is similar to western PWR inlet water temperatures. Since the operating temperatures and nickel contents are similar, the radiation embrittlement of the beltline of the WWER-1000 vessels is somewhat comparable to the embrittlement of the beltline regions of the western vessels fabricated with A 533-B and A 508.

The radiation coefficient, A_F, given in Ref. [36] and discussed in Section 3.4.4 of this report, was developed from weld metal data with a nickel content lower than 1.5 weight %. Therefore, use of the standard values of these coefficients for determining the allowable fracture toughness of weld metal with a high nickel content (using the K_{IC} curves for weld metal from the Code) may not be conservative especially if weld metals also contain high content of manganese (approximately over 0.8 weight %). The analysis of data from WWER-1000 surveillance specimens investigation was performed. Comparison of these results with temperature T_K design prediction according to [36] is presented in figures 28 and 29.

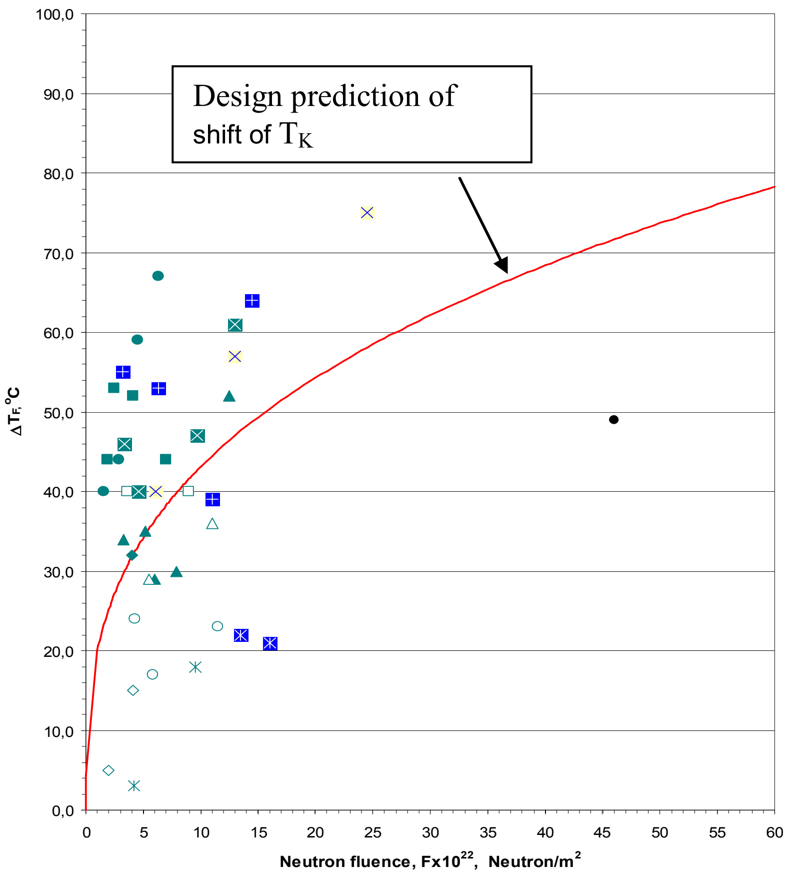


Fig. 28. Shift of ductile-to-brittle transition temperature T_K of weld metal due to irradiation.

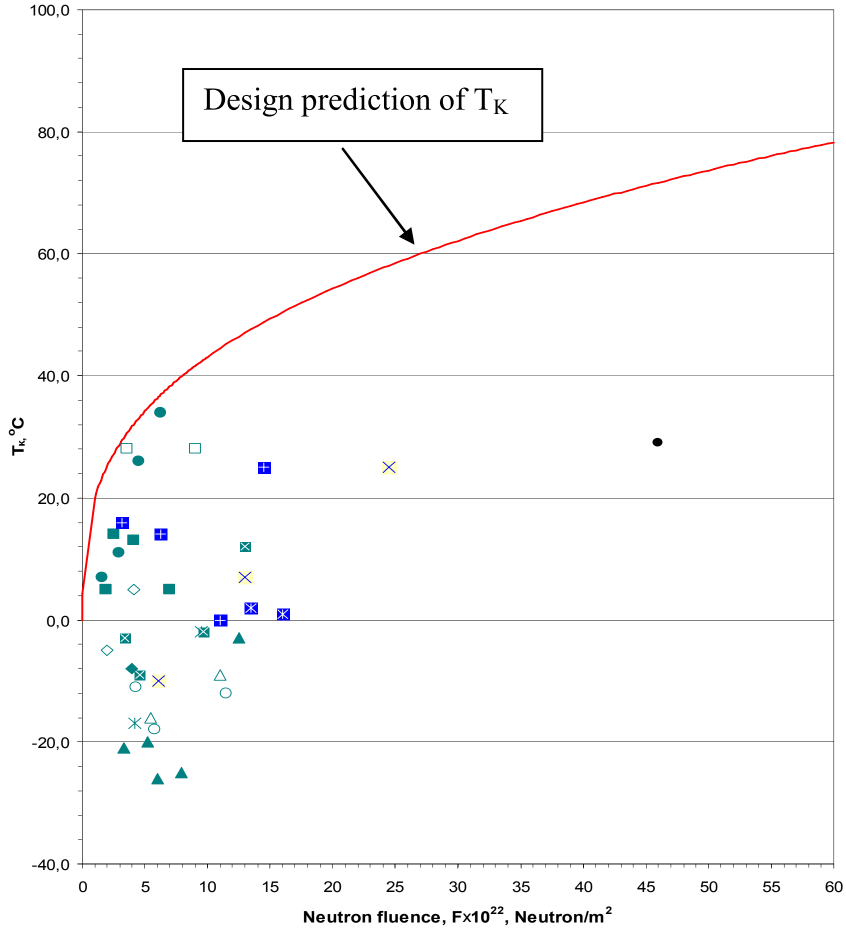


Fig. 29. Ductile-to-brittle transition temperature T_K of weld metal (absolute value).

As follows from Figure 29, real values of T_K temperature do not exceed design predictions, but the question about the influence of nickel content on radiation embrittlement now is still under consideration.

Also, it was suspected that the data from the WWER-1000 design surveillance specimens programme (except the data from the specimens from the three ŠKODA made vessels) might not be representative of the radiation embrittlement of the beltline materials because the surveillance specimens are located above the reactor core and the core barrel in a steep flux gradient where their temperatures are at least 10°C higher than the temperature of the RPV beltline region. Due to these reasons improvement of the surveillance programme was recognized to be necessary. This subject is discussed in greater detail in Section 6.

To summarize, the 15Kh2MFA weld metal in the WWER-440 V-230 pressure vessels is very susceptible to radiation damage because of its low operating temperature and high phosphorus content. The Type 15Kh2MFAA material in the WWER-440 V-213 pressure vessels is relatively resistant to radiation damage because of its good chemistry (lack of impurities) and vanadium carbides. However, this material is also exposed to relatively low operating temperatures where there will be more radiation damage than at higher temperatures. The Type 15Kh2NMFAA material used for the WWER-1000 pressure vessels sometimes contains relatively high levels of nickel in the weld metal, but relatively low levels of copper and other impurities. Its radiation damage may be somewhat comparable to some of the materials used in western PWR pressure vessels but in cases with high nickel content (over 1.5 weight %) and high manganese content (over 0.8 weight %) in welds it can be a limiting factor for the RPV lifetime.

Significance for WWER pressure vessels

Radiation damage becomes significant at neutron fluences greater than 1×10^{22} n/m^2 (E >0.5 MeV). The design end-of-life neutron fluence for the beltline region of the WWER- 440/V-230 pressure vessels has been calculated to be to approximately 1.5×10^{24} n/m^2 while for the V-213 type it is somewhat higher — up to 2.5×10^{24} n/m^2. The actual RPV life depends very strongly on the operation history and mitigation activities. Most of the V-230 plants use dummy elements in the periphery of the active core to decrease the neutron flux on the RPV wall; in other WWER reactors, a low leakage core (LLC) strategy has been implemented to reduce the flux hence fluence.

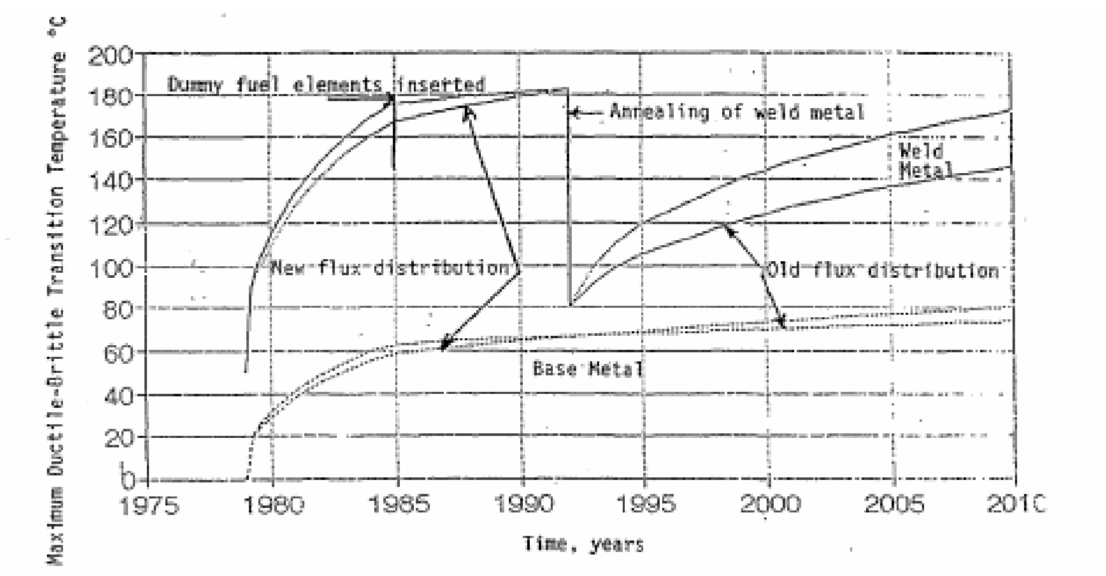


Fig. 30. Transition temperature values as a function of operation time for Bohunice Unit 2. The upper lines are the weld material and the lower lines are the base metal material.

Although the most irradiated part of the RPV is the base metal situated around the axial centre region of the reactor core, the most degraded material in the WWER V-230 vessels is the circumferential weld metal located in the lower part of the core, which is labelled the 0.1.4 or 5/6 weld. Its neutron fluence reaches only about 70% of that of the maximum fluence in the beltline region, but its embrittlement is much higher because of its high phosphorus content. This weld metal controls the vessel lifetime even after vessel annealing, as shown in Fig. 30. The circumferential weld at the top of the centre shell ring is subjected to much lower fluences, its neutron fluence being equal to about 3% of the maximum fluence in the beltline region. Therefore, it is necessary to anneal only a small region around the most embrittled weld to improve the state of the whole vessel and to extend its lifetime.

In contrast to the embrittlement behaviour of the weld and base metals used in the V- 230s, there is no substantial difference between the embrittlement of the base and weld metals in the WWER-440/V-213 pressure vessels. The only difference is that the initial transition temperature of the weld metal is much higher than in the base metal. Thus, the weld metals located at 0.1.4 again control the vessel lifetime.

There is only a small difference between the fluences in the circumferential weld metal situated in the lower part of the active core and the base metal exposed to the maximum fluences in the beltline region of the WWER-1000 pressure vessels. In this case, for weld metals with nickel content lower than 1.5 weight %, there is no substantial difference in the embrittlement of the base and weld metal. However, weld metals with high nickel content (up to 1.9 weight %) and high manganese content (over 0.8 weight %) experience greater embrittlement than the base metal. Thus, in most cases, the weld metals remain the controlling materials for the RPV embrittlement.

The IAEA conducted a coordination research programme (CRP) on the effects of Ni content on radiation embrittlement of WWER-1000 RPVs (see 5.3.5).

4.2. THERMAL AGEING

4.2.1. Description of mechanism

Thermal ageing is a temperature, material state (microstructure) and time dependent degradation mechanism. The material may lose ductility and become brittle because of very small microstructural changes in the form of precipitates coming out of solid solution. In the case of RPV steel with impurity copper, the important precipitates are copper-rich (however, there could be other precipitates). The precipitates block dislocation movement thereby causing hardening and embrittlement. The impurity copper in RPV steel is initially trapped in solution in a super-saturated state. With time at normal PWR operating temperatures (~290°C), it may be ejected to form stable precipitates as the alloy strives toward a more thermodynamically stable state, even if there is no radiation damage. As discussed in Section 4.1, neutron-induced structural damage promotes the copper precipitation process.

The effects of long-term aging at temperatures up to 350°C on the ductile-to-brittle transition temperature of RPV steels have been summarized in a paper by Corwin et al. [58]. The work was sponsored by the USNRC and performed at the Oak Ridge National Laboratory and the University of California, Santa Barbara. Corwin et al. concluded that "most of the data from the literature suggest that there is no embrittiement in typical RPV steels at these temperatures for times as great as 100,000 h...."

Some of the more important data is discussed next. The reader is referred to the Corwin et al. paper for additional references and discussions. Data are available on the behaviour of A 302 Grade B, A 533 Grade B and A 508 Class 2 and Class 3, and equivalent non-US steels. Limited thermal ageing studies by Potapovs and Hawthorne [59] for P-bearing A 302 Grade B steels at 290°C revealed no significant shift in the ductile to brittle transition temperature (decrease of 5 to 14°C). DeVan et al. [60] have reported that A 533 Grade B Class 1 plate materials from the Arkansas 1 reactor shifted -4 to 10°C after thermal ageing at 280°C for 93,000 hours. A 508 Class 2 forging materials encapsulated outside the beltline region of the Oconee Unit 3 reactor showed an increase of about 1°C after exposure to a temperature of 282°C for 103,000 hours [60]. Also, weld metal specimens from the Arkansas 1 and Oconee Unit 3 reactors showed changes in the ductile-to-brittle transition temperature of -8 to 0°C after exposure up to 103,000 hours at about 280°C [60]. The weld metal specimens were made with Linde 80 MnMoNi weld wire typical of that used for submerged arc welds.

Fukakura et al. [61] studied the effect of thermal ageing on A 508 Class 3 steel and concluded that after thermal ageing for 10,000 hours at temperatures of 350°C, 400°C and 450°C, the increase in the nil-ductility transition temperature was small. The J_{IC} and J-R resistance curves also decreased somewhat as a result of thermal ageing. It appears that grain size may be an important variable in assessing thermal ageing embrittlement. The effect of grain size on thermal ageing embrittlement may be due to grain boundary embrittlement by impurity segregation (e.g. P) at the grain boundaries.

Three studies have been conducted in Germany of the effects of thermal ageing of low alloy pressure vessel steels. All the tests were conducted at temperatures typical of operating temperatures and for durations of 10,000 to 100,000 hours. All found negligible detrimental effects. In the first study, base, butt-weld heat-affected zone, and weld-simulated heat affected zone 20 Mn Mo Ni 5 material was clamped to the main coolant lines of three NPPs for about 60,000 hours (7 years). The ageing temperature was approximately 290°C (555°F). The toughness transition temperature curves for these materials were unchanged at the end of the exposure period. In the second study, which was part of the German Component Safety Programme, base and heat-affected zone 20 Mn Mo Ni 5 and 2 Cr 37 material was placed in a laboratory furnace at 320°C (608°F) for up to 10 hours. Again, there were no significant changes in the toughness transition temperature curves. In the third study, parts of the Obrigheim main coolant line were removed after approximately 100,000 hours of operation at about 285°C (545°F) and then destructively examined (tensile and Charpy testing). The Obrigheim main coolant line was fabricated from 2 Ni Mo Cr 37 material. The mechanical testing indicated that there had been no thermal embrittlement of this material during the 100,000 hours of operation.

In contrast, Hasegawa et al. [62] observed some small shifts in the transition temperature for Cu and P-bearing A 533-B steels after thermal ageing at temperatures near 300°C and a maximum shift at about 500°C, well beyond the operating temperature of PWRs. Similar behaviour was reported for coarse-grain simulated and thermally aged heat-affected zones of A 533-B steel by Druce et al. [63]. However, this ageing was associated with temperatures higher than 400°C and with P segregation to the grain boundaries.

The 15Kh2MFA and 15KMMFAA steels used to fabricate the WWER-440 pressure vessels also do not appear to be susceptible to thermal ageing, even when they contain relatively high phosphorous impurity levels. The results from the thermal ageing surveillance specimens located in the upper plenums of the WWER-440 V-213 pressure vessels and removed and tested after 10 years at about 300°C indicate that the shift in the Charpy ductile to brittle transition temperature is small. These results are supported by Charpy ductile to brittle transition temperature measurements from RPV trepans removed from closed plants (Novovoronezh 1 and 2), as well as boat samples taken from operating plants. Laboratory tests carried out at 350°C for 10 hours also showed that the transition temperatures remain stable within the normal data scatter.

The type 15Kh2NMFA steel used to fabricate the WWER-1000 pressure vessels is slightly susceptible (a shift in the ductile-brittle transition temperature of 10 to 20°C) to thermal ageing at operating temperatures, due to the high nickel and low vanadium content of this material. Even though the Standard [35] recommends that thermal ageing should not be taken into account for this type of steel, the most recent results show some non-negligible shift that should be considered and incorporated into the Standard [64].

4.2.2. Significance

Thermal ageing does not appear to be generic but depends on the heat treatment, chemical composition and service time at temperature of the material. Microstructural aspects such as grain size and the phases present may also be involved in the thermal ageing of low alloy steels. The experimental results discussed above show that the thermal ageing mechanism should be classified as an insignificant degradation mechanism for PWR RPVs. In addition, it can be argued that thermal ageing degradation is at least partly taken into account in the RT_{NDT} shift prediction methodologies since all the PWR surveillance capsule specimens are irradiated at slightly higher temperatures than the RPV walls.

Thermal ageing does not appear to be significant for WWER type reactors, even for materials with high phosphorous content. The results from the surveillance specimens after 10 years of operation at about 300°C as well as the results from testing of the trepans and boat samples from components aged more than 15 years have not shown any substantial transition temperature increases.

4.3. TEMPER EMBRITTLEMENT

4.3.1. Description of mechanism

The term "temper embrittlement" has been traditionally used to describe the embrittlement of structural steels, mostly by impurity phosphorus concentrating at the grain boundaries. Temper embrittlement is found in quenched and tempered ferritic materials, especially when a tempering temperature around 450-500°C is used. The role of phosphorus in the overall embrittlement of western-type RPV materials has been a subject of much discussion over the years. The problems have been compounded by the lack of qualified data and the variation of alloy compositions and irradiation conditions. However, the effect of phosphorus in weld metals and the heat affected zones is of concern, particularly when a thermal annealing may be applied to restore toughness. The propensity of phosphorus to migrate to grain boundaries in the RPV materials and thereby cause embrittlement under certain thermal conditions should be accounted for. The generation of a non-hardening embrittled condition is theoretically possible (called temper embrittlement) if phosphorus levels are high enough and the diffusion paths and thermal activation are available.

4.3.2. Significance

RPV steels with phosphorus content well above about 0.02 weight % may be susceptible to temper embrittlement during fabrication. However, the western RPV materials normally contained less than 0.020 weight % phosphorus. Therefore, it is unlikely that any western RPVs will exhibit temper embrittlement. If a 450°C thermal anneal of an irradiated RPV is required for recovery of the fracture toughness, the possibility of temper embrittlement should be evaluated.

4.4. FATIGUE

4.4.1. Description of mechanism

Fatigue is the initiation and propagation of cracks under the influence of fluctuating or cyclic applied stresses. The chief source of cyclic stresses are vibration and temperature fluctuations. As discussed previously, the PWR RPV is designed so that no subcomponent of the RPV is stressed above the allowable limits described in the ASME Boiler and Pressure Vessel Code, Section III (or equivalent national codes) during transient conditions and the allowable cyclic fluctuations do not violate Miner's fatigue rule. Once a crack is detected, it's behaviour under cyclic loading is analysed according to Section XI of the ASME Code or similar codes.

The RPV should be designed in such a way that no subcomponent is stressed above the allowable limit, which is a usage factor of one. Even if the usage factors go slightly above one, fatigue cracks are not expected because the safety factors discussed in Section 3 are used in the design.

4.4.2. Significance

Significance for western pressure vessels

The RPV closure studs have the highest usage factor of any the subcomponents. However, the usage factor for the RPV closure studs are of the order of 0.66 for the 40-year design life. The head penetrations for the control rod drives and the vent tubes have very low fatigue usage factors. The RPV inlet and outlet nozzles also have relatively low fatigue usage factors. Unless there is some condition that results in extreme vibration to any of the RPV subcomponents, fatigue damage is considered an insignificant degradation mechanism in the assessment and management of the PWR pressure vessels.

Significance for WWER pressure vessels

From a fatigue point of view, the most important subcomponents of the WWER pressure vessels are the closure studs. The lifetime of the WWER-440 closure studs is limited to some 15 years of operation, when the expected usage factor will reach one. However, there is little chance of failure because these studs are tested every four years by ultrasonic and eddy current methods. Moreover, their exchange is a standard maintenance procedure, which is planned in advance.

The second most important WWER pressure vessel components are the primary nozzles, especially the cladding on their inner radius. However, the calculated usage factors for these locations are less than one for the whole design lifetime. And again, these parts are included in the ISI performed every four years when ultrasonic, eddy-current and dye-penetrant methods are applied.

4.5. CORROSSION

Corrosion is the reaction of a substance with its environment that causes a detectable change which can lead to deterioration in the function of the component or structure. In the present context, the material is steel and the reaction is usually an electrochemical (wet) reaction. The appearance of corrosion is governed by the so-called corrosion system consisting of the metal and the corrosive medium (the environment) with all the participating elements that can influence the electrochemical behaviour and the corrosion parameters. The variety of possible chemical and physical variables leads to a large number of types of corrosion, which can be subdivided into:

- corrosion without mechanical loading (uniform corrosion and local corrosion attack, selective corrosion attack as e.g. intergranular corrosion)
- corrosion with mechanical loading (stress corrosion cracking, corrosion fatigue)
- flow assisted corrosion attack (erosion-corrosion, flow induced corrosion, cavitations).

During the electrochemical processes, the metal ions dissolve in liquid electrolyte (anodic dissolution) and hydrogen is produced. This is the process of material loss and creation of corrosion products. When mechanical stresses or strains are also present, the anodic dissolution of the metal can be stimulated, protection layers (oxide layers) can rupture or hydrogen interaction with the metal (absorption) can be promoted which can produce secondary damage. The combined action of a corrosive environment and mechanical loading can cause cracking even when no material degradation would occur under either the chemical or the mechanical conditions alone.

Water chemistry control during operation, as well as during shutdown, is very important with respect to avoiding corrosion problems. Thus the content of al additives has to be carefully monitored and the ingress of impurities has to be strictly avoided, e.g. during stand still periods and maintenance work. The water chemistry regimes which are used in the primary coolant circuits of the various types of reactors and which have proven effective are presented in Table 27.

TABLE 27. TYPICAL PRIMARY COOLANT SYSTEM WATER CHEMISTRY PARAMETERS

Parameter[a]	Siemens-KWU (FRG)	EPRI (US)	Westinghouse (US)	VGB (FRG)	J-PWER (Japan)	EdF (France)	WWER 440/1000 (SU)	WWER 440 (Finland)
Lithium hydroxide	0.2-2*	0.2-2.2*	0.7-2.2*	0.2-2.2*	0.2-2.2*	0.6-2.2* 0.45-2.2**		
Potassium hydroxide	-	-	-	-			2-16.5#	2-22#
Ammonia	-	-	-	-			>5	>5
Hydrogen	2-4	2.2-4.5	2.2-4.4	1-4	2.2-3.15	2.2-4.4	2.7-4.5	2.2-4.5
Oxygen	<0.005	<0.01	<0.005	<0.005	<0.005	<0.01	<0.01	<0.01
Chloride	<0.2	<0.15	<0.15	<0.2	<0.05	<0.15	<0.1	<0.1
Fluoride	-	<0.15	<0.15	-	<0.1	<0.15	<0.05	<0.1
Conductivity (25°C)	<30	*	*	-		1-40*	4-80*	-
pH (25°C)	5-≈8.5	*	4.2-10.5*	*	4.2-10.5	5.4-10.5	>6	>6
Dissolved iron	(<0.05)[b]	-	-	-	-	-	-	-
Total iron		-	-	(<0.01)[b]	-	-	<0.2	
Sulphate	-	0.1	-	-	-	-	-	-
Silica	(<0.5)[b]	-	<0.2	-	-	<0.2	-	-
Suspended solids	(<0.1)[b]	0.35	<1	-	<0.5	<1	-	
Aluminium	-	-	<0.05	-	-	<0.1	-	-
Calcium	-	-	<0.05	-	-	<0.1	-	-
Magnesium	-	-	<0.05	-	-	<0.1	-	-

[a] concentrations in mg/kg (ppm), conductivities in µS/cm (µmhos/cm)
[b] normal operating value
\- not applicable/specified
* According to Li and B concentration
** According to Li and B concentration, new treatment
\# Calculated taking into account ΣK + Na + Li

4.5.1. Primary water stress corrosion cracking (PWSCC)

4.5.1.1. PWR control rod drive mechanism (CRDM) nozzles

Many western PWRs have CRDM (control rod drive mechanisms) penetrations in the RPV head made of stainless stain steel and Alloy 600. The lower portion of each penetration is made of Alloy 600, a high nickel content material, and is attached to the inside surface of the pressure head with a partial-penetration weld. The weld and the CRDM nozzle wall above the weld are part of the primary coolant primary vessel boundary. The upper portion of each CRDM penetration is made of stainless steel which is joined to the Alloy 600 material with a dissimilar metal weldment and joined to the CRDM housing with a screw fitting and seal weld. A typical Westinghouse-type CRDM penetration is shown in Fig. 31. There are typically 40 to 90 penetrations in a reactor pressure vessel depending on the plant size, distributed over each pressure vessel head as shown in Fig. 32. The CRDM penetration design and materials are essentially the same for all PWRs in Belgium, Brazil, China (including Taiwan, China), France, Japan, Republic of Korea, South Africa, Spain, Sweden, Switzerland, and the United States. However, in Germany the only PWRs with Alloy 600 penetrations are Obrigheim which is no longer in operation and Mülheim-Kärlich, which due to other reasons never went in operation.

Mechanism description

PWSCC requires the simultaneous presence of high tensile stresses, a corrosive environment (in this case, high temperature water) and a susceptible microstructure [65]. The PWSCC damage rate increases as a function of stress to the power of 4 to 7, i.e.:

$$\text{damage rate} \propto \sigma^4 \qquad (38)$$

where

σ is the maximum principal tensile stress, which includes both applied and residual stresses. This correlation suggests that a 50% reduction in the effective stress will result in a 16-fold decrease in the damage rate and a corresponding increase in PWSCC initiation time.

PWSCC is also a thermally activated process that can be described by an Arrhenius relationship of the form:

$$\text{damage rate} \propto e^{-Q/RT} \qquad (39)$$

where

Q is activation energy, R is universal gas constant and T is temperature. Various estimates for the activation energy, Q, of Alloy 600 tube materials have been derived from laboratory studies and field experience. The estimates range from 163 to 227 kJ/mole (39 to 65 kcal/mole), with a best-estimate value of 209 kJ/mole (50 kcal/mole) (Ref. [65] and references therein). Estimates for the activation energy for Alloy 600 components fabricated from bar material may be different than those fabricated from tube materials. Both the initiation and growth of PWSCC are very sensitive to temperature. For example, a PWSCC initiation time would typically be reduced by a factor of two for a 10°C (18°F) increase from an operating temperature of 315°C (600°F).

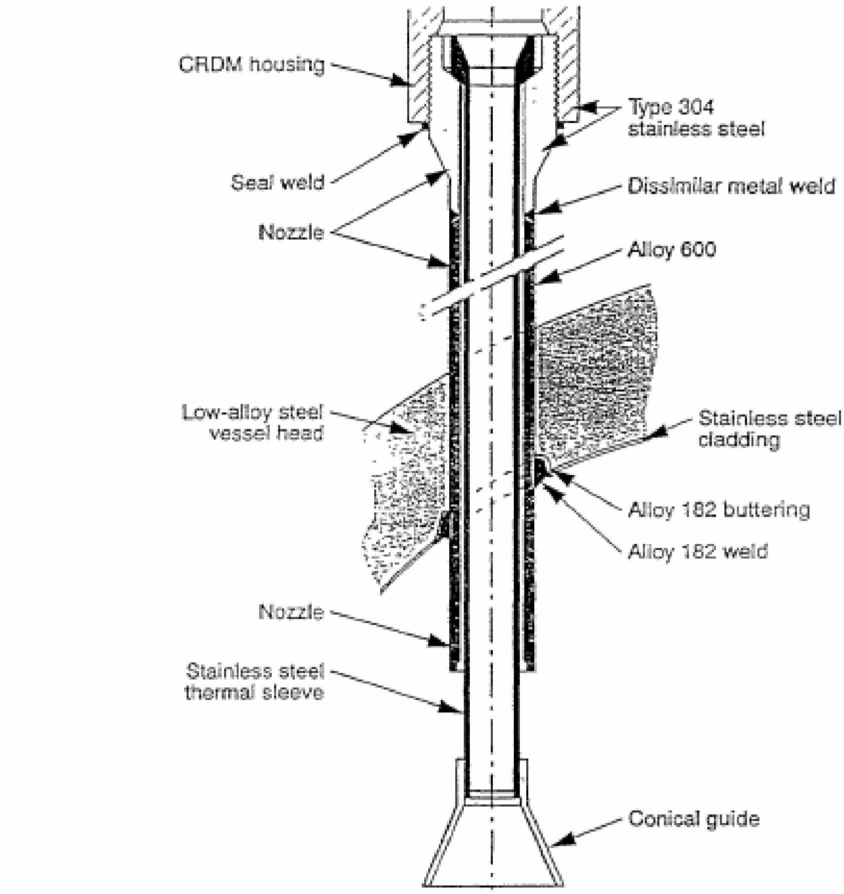


Fig. 31. Typical control rod drive mechanism penetration in a Westinghouse-type PWR (Buisine et al. 1994). Copyright the Minerals, Metals & Materials Society; reprinted with permission.

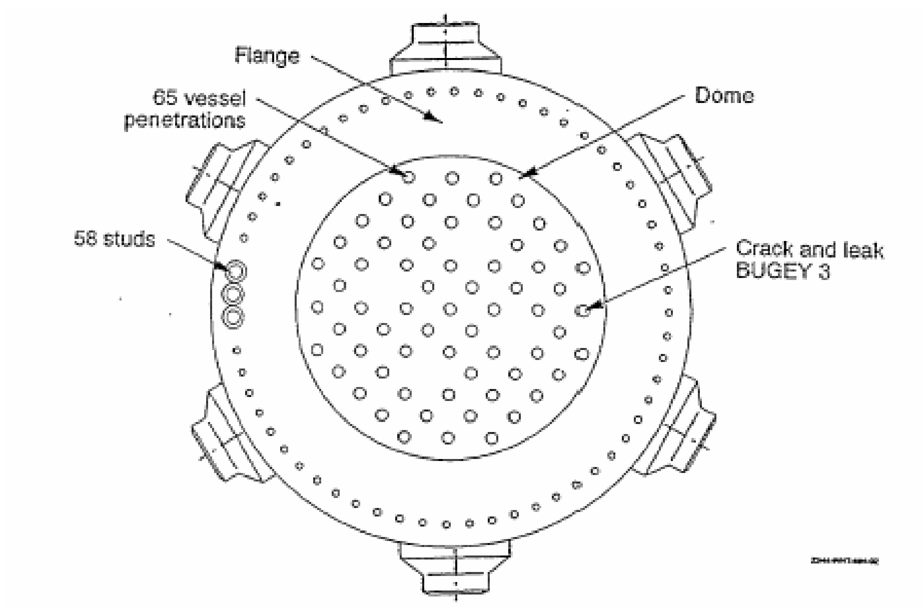


Fig. 32. Plan view of the PWR pressure vessel head (Buisine et al. 1994). Copyright the Minerals, Metals & Materials Society; reprinted with permission.

Field experience and research results show that the PWSCC resistance of Alloy 600 is highest when the grain boundaries are covered with continuous or semi continuous carbides. For example, the PWSCC initiation time increases by a factor of five as the grain boundary carbide coverage increases from 0 to 100% [66]. The percentage of the grain boundaries covered with intergranular carbides depends on the material heat treatment temperature and time, carbon content and grain size. High temperature heat treatments which put the carbides back in solution result in good carbide coverage of the grain boundaries and a microstructure that is more resistant to PWSCC. Also, larger grain material has less grain boundary area than small grain material, so it is easier to get complete coverage with larger grains.

Primary coolant chemistry has a secondary effect on the time of PWSCC initiation. However, increasing the hydrogen concentration in the primary coolant decreases the PWSCC initiation time. Therefore, EPRI's PWR Primary Water Chemistry Guidelines [67] recommend that plant operators maintain hydrogen concentrations in the range of 25 to 35 cm^3/kg, which is near the lower end of the typically used range of 25 to 50 cm^3/kg.

Penetration fabrication and installation

The penetration material and the installation process can determine whether the penetrations are susceptible to PWSCC or not. The penetrations in many Westinghouse plants, all B&W plants, and several Combustion Engineering plants were fabricated from Alloy 600 pipes, as were the penetrations in the Swedish plants. The penetrations in all French, Swiss and Belgian plants and the remaining Westinghouse and Combustion Engineering plants were fabricated from Alloy 600 bars. One difference in the bar materials is that the French, Swiss and Belgian plants used forged bars whereas the Westinghouse and Combustion Engineering plants generally used rolled bars. A machining process was used to fabricate the penetrations. Machining introduces a thin layer of cold-worked material on the machined surfaces. The yield strength of the cold-worked material is higher than that of the base metal. In addition, machining also introduces compressive residual stresses. The penetration material was usually heat treated in the temperature range of 870 to 980°C (1,600 to 1,800°F) for 90 minutes or longer. One exception is the French PWRs, where the material was heat treated in the temperature range of 710 to 860°C (1,310 to 1,580°F) if the yield strength exceeded 343 MPa (49.7 ksi). A higher heat treatment temperature results in lower yield strength and lower residual stresses. Also, a higher heat treatment temperature (above the solution temperature) is one of the parameters that can result in a more PWSCC resistant microstructure. The Alloy-600 penetrations at Obrigheim were stress relieved during fabrication, and this is probably the reason that cracks have not been detected in this material.

The penetrations are shrunk fit into the vessel head openings by dipping them into liquid nitrogen and quickly inserting them into the openings. When the penetration returns to ambient temperature, a tight fit results. Then, the penetrations are attached to the bottom of the head with a partial penetration weld, shown in Fig. 31. These attachment welds are made with multiple passes (up to an estimated 50 passes) of Alloy 182 weld metal. Due to the geometry of the vessel head, the attachment welds are not axisymmetric, except the one for the central penetration, and therefore, the amount of weld metal deposited around the penetration is not uniform. The 180-degree location has a wider weld bead than the 0-degree location. Also, the average volume of weld metal deposited around the penetration varies from plant to plant. The standard minimum partial penetration weld size is given in Figure NB-4244(d)-l(d) of Section III of the ASME Code. Some fabricators may have used larger weld sizes to ensure that the minimum size was met. As their experience increased, the fabricators may have been able to use smaller weld sizes on later heads. In addition, the size requirements in the ASME Code were changed between the 1968 and 1971 editions to permit an alternate configuration for partial penetration weld connections. Use of the alternate configuration will reduce the depth of the weld groove by about 40% and the length of the weld leg (length of the weld in contact with the nozzle) by about 17%.

The weld metal shrinks as it cools and pulls the lower end of the penetration radially outward (because of the difference in the axial location of the 0 and 180° weld metal), bending the penetration and ovalizing its cross-section at the weld region [68]. The ovality (the difference in major and minor diameters, as a percentage of the original diameter) can be as high as 2% in the penetration cross-section at the downhill location of the weld. A penetration with a larger setup angle or larger welds has more welding-induced deformation, that is, bending of the penetration and ovalization of its cross-section. The ovalized cross section has its major diameter along the circumferential direction of the vessel head. The deformation also causes the penetration above the weld to lose its interference fit with the head opening.

Residual stresses

The welding-induced residual stresses on the inside surface of the penetration have been measured using a mockup of a typical CRDM penetration [69, 70]. As expected, the measurements show that the highest welding-induced stresses on the inside surface are in the peripheral penetrations, for which the setup angle is the largest. The stresses are highest at the 0-degree location, toward the periphery of the vessel head. The circumferential stresses exceed the axial stresses by about a factor of 1.6. Stresses at the 180-degree location on peripheral penetrations are lower, but the circumferential stress is still higher than the axial stress. As the setup angle decreases, the magnitude of the measured circumferential and axial stresses and the difference between them also reduce.

Axial tensile stresses are generally lower than hoop stresses; but, later than axial crack initiation, they can initiate a circumferential crack on some parts of nozzles. However, complete circumferential through wall crack is an unlikely event because the axial stresses across the wall thickness vary from tensile to compression and cannot support the growth of a circumferential crack in current license period.

Penetration environment

The penetration temperature is determined by the temperature of the coolant in the upper head. Estimated head temperatures vary from 289 to 327°C (552 to 621°F). The penetration temperatures could be affected by the bypass of the vessel inlet flow into the upper head, which varies from an estimated 5% in some Westinghouse-designed plants to 0.5% in Combustion Engineering-designed plants. Framatome is modifying the vessel internals to increase the bypass flow in some of its plants. Lower penetration temperatures are beneficial in mitigating PWSCC. Measurements have been taken on some reactor pressure vessel heads to confirm differences between center and peripheral nozzles in cold domes (cold heads) and hot domes (hot heads), but the French field experience doesn't show any significant differences in initiation time for hot and cold domes. As will be discussed Section 5, Inspection and Monitoring Requirements and Technologies, Bamford and Hall [71] reported that there has been approximately 41 head penetrations in which pressure boundary leaks have been discovered as of June 2004. Figure 1 of Bamford and Hall [71] paper provides the results from reactor vessel head penetration inspections. The Figure 1 of [71] has been reproduced as Figure 33 of this TECDOC. As may be seen in Figure 33, the experience in US plants has been very strongly correlated to the time and temperature of service.

Operating experience

A CRDM penetration began leaking in September 1991 at Bugey 3, a French PWR. The leak occurred during a hydrotest (after 10 years of operation) conducted at a pressure of 20.7 MPa (3,000 psi) and about 80°C (175°F) and was detected with acoustic emission monitoring equipment [68]. The leak rate was about 0.7 L/h (0.003 gpm).

Subsequent inspection revealed that the leaking crack was axially oriented and located on the downhill side at an elevation corresponding to the lowest portion of the partial penetration weld attaching the penetration to the RPV head. Several other approximately axial (within about 15 degrees of being axial) cracks were also found on the inside surface of the penetration at both the downhill and uphill locations. A sketch showing the crack locations is shown in Fig. 34. In addition to the leaking crack, there was another through-wall axial crack located at the counterbore and below the weld at the uphill location, shown in Fig. 34. This crack is in the portion of the penetration wall that does not constitute a part of the primary pressure boundary.

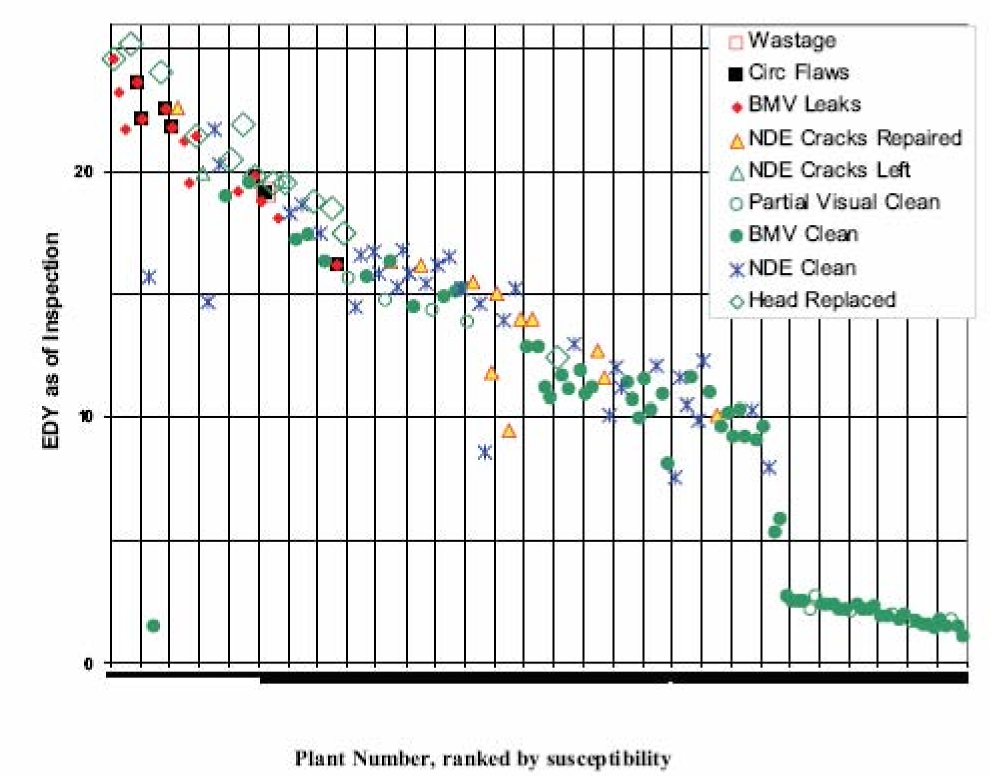


Fig. 33. Results of Reactor Vessel Head Penetration Inspections, Updated through 12/31/04, Compiled by Larry Mathews.

Destructive examination of the damaged penetration material revealed that a through-wall PWSCC crack was initiated on the inside surface (downhill location) at the upper corner of the counterbore; it was 25 mm (1.0 in.) long on the inside surface and 2 mm (0.08 in.) long on the outside surface. The crack length was greater underneath the inside surface; its maximum value was 52 mm (2.0 in.) [72]. The crack also penetrated the Alloy 182 weld metal over a length of about 15 mm (0.6 in.) and to a maximum depth of 2.7 mm (0.1 in.). The examination also revealed oxidation at the crack tip, which implies that the through-wall crack was present prior to the hydrotest [69]. In addition, there were no boric acid deposits on the surface of the head opening, indicating that significant leakage did not occur during operation [68].

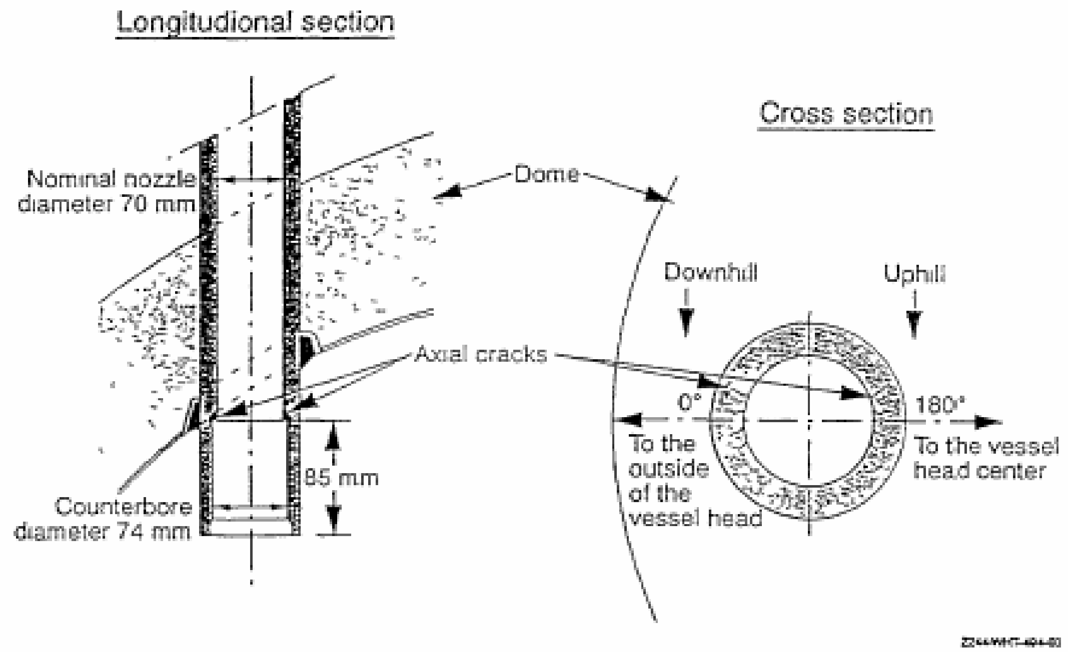


Fig. 34. Location of cracks on the Bugey 3 CRDM nozzle (Buisine 1994)
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The destructive examination also revealed the presence of two circumferential cracks on the outside surface of the penetration: one in the weld, which was found to be a *hot crack* resulting from the original welding process; and one in the base metal, which was connected to the axial through-wall crack. The crack in the base metal was on the downhill side just above the weld, making an angle of 30 degrees with the horizontal plane. The crack was about 3 mm long and 2.25 mm deep. Further examination verified that the crack was caused by PWSCC [73]. It appears that the primary coolant may have leaked through the axial crack into the annular region between the nozzle wall and the vessel head and caused PWSCC on the outside surface, or the circumferential crack in the base metal was part of the through-wall axial crack.

After the leakage was detected at Bugey 3, the CRDM penetrations at plants in Belgium, Brazil, France, Japan, South Africa, Spain, Sweden, Switzerland and the USA were inspected. According to Thomas R. Mager [74] inspection results to April 30, 2002 showed that out of 7,384 penetrations, 6,373 penetrations were inspected and 318 penetrations were identified as having indications (5%). The results of these inspections are summarized in Table 28 [74]. Most of the indications were found in the French PWRs [75, 76], in part, because more French penetrations have been inspected; however, indications were also found in Sweden, Switzerland and the USA. If the inspection results from France are excluded, the percentage of penetrations with indications in the remainder of the world is approximately 0.5%.

Most of the penetration cracks were short (less than 25 mm) and axial, making a small angle with the vertical, initiated on the inside surface and located at either the uphill or downhill side of the peripheral penetrations and near the partial penetration weld. The maximum angle of inclination was about 30 degrees. The first circumferential indications where found at three plants. These indications were on the outside surface of penetrations at Bugey 3, in the attachment weld at Ringhals 2 and at Zorita [77]. A few more penetrations have been found with circumferential cracks over the past few years. A total of 19 circumferential cracks nozzles above the weld or in the weld elevation zone were detected at Crystal River 3, Davis-Besse, North Anna 2, and Oconee 2 and 3 [78]. An additional 16 circumferential cracks were detected in nozzles below the weld. Cracks below the weld are not considered safety significant [78]. Today, 53 leaking penetrations (32 in the nozzles and 21 in the weld) have been detected in the United States plus one in France and one in Japan. A few cracks were found through wall located below the welds in the region of penetration that is not part of the reactor coolant pressure boundary [76,79].

TABLE 28. OPERATIONAL INFORMATION AND INSPECTION RESULTS FOR UNIT EXAMINED

Country	Plant Type	Units Inspected	K Hours	Head Temp (˚F)	Total Penetrations	Penetrations Inspected	Penetrations with Indications
France	CPO	6	80-107	569-599	390	390	23
	CPY	28	42-97	552	1820	1820	126
	1300 MW	20	32-51	558-597	1542	1542	95
Sweden	3 Loop	3	75-115	580-606	195	190	8
Switzerland	2 Loop	2	148-154	575	72	72	2
Japan	2 Loop	7	105-108	590-599	276	243	0
	3 Loop	7	99	610	455	398	0
	4 Loop	3	46	590	229	193	0
Belgium	2 Loop	2	115	588	98	98	0
	3 Loop	5	60-120	554-603	337	337	6
Spain	3 Loop	5	65-70	610	325	102	0
Brazil	2 Loop	1	25	NA	40	40	0
South Africa	3 Loop	1	NA	NA	65	65	6
Slovenia	2 Loop	1	NA	NA	49	49	0
South Korea	2 Loop	3	NA	NA	49	49	3
	3 Loop	2	NA	NA	130	130	2
United States	2 Loop	2	170	590	98	98	0
	3 Loop	1	NA	NA	65	20	12
	4 Loop	18	NA	NA	1149	537	35
Totals		117	-	-	7387	6373	318

As of April 30 2002

CRDM penetration cracking that was probably caused by chemical attack due to intrusion of demineralizer resins containing sulphur into the reactor coolant system has occurred in the 160-MW, one-loop Spanish PWR, Zorita. A total of 171 crack indications were found in 34 of the 37 Zorita penetrations [77, 80]. Most of the indications were axial and located in the free span region of the penetrations rather than near the attachment welds. These indications were not included in Table 28 because the cracking was not PWSCC and the Zorita water chemistry excursion is not typical of what might occur in most PWRs.

Operating experience in the United States of America

The susceptibility of head penetrations to PWSCC appears to be strongly linked to the operating time and temperature of the reactor pressure vessel head. Problems related to PWSCC have therefore increased as plants have operated for longer periods of time.

G. White et al published a paper titled, "Summary of US PWR Reactor Vessel Head Nozzle Inspection Results" [78], which provides a summary of USA PWR plants head penetration cracking as well as a summary of head penetration leakage. G. White et al reported that there was a total of 144 penetrations with cracks in the tube or weld metal (81 tubes and 64 welds). G. White et al also reported 51 leaking penetrations (31 tubes and 20 welds).

As stated above, the susceptibility of reactor pressure vessel head penetrations to PWSCC appears to be strongly linked to operating time and temperature of the reactor pressure vessel head. Problems related to PWSCC in the USA have therefore increased as plants have operated for longer periods of time. Inspections of the reactor pressure vessel head nozzles at the Oconee Nuclear Station, Units 2 and 3, in early 2001 identified circumferential cracking of the nozzles above the J-groove weld, which joins the nozzle to the reactor pressure vessel head. Circumferential cracking above the J-groove weld is a safety concern because of the possibility of a nozzle ejection if the circumferential cracking is not detected and repaired.

Section XI of the American Society of Mechanical Engineers Boiler and Pressure Vessel Code (ASME Code), which is incorporated into NRC regulations by 10 C.F.R. 50.55a, “Codes and standards,” currently specifies that inspections of the RPV head need only include a visual check for leakage on the insulated surface or surrounding area. These inspections may not detect small amounts of leakage from an RPV head penetration with cracks extending through the nozzle or the J-groove weld. Such leakage can create an environment that leads to circumferential cracks in RPV head penetration nozzles or corrosion of the RPV head. In response to the inspection findings at Oconee and because existing requirements in the ASME Code and NRC regulations do not adequately address inspections of RPV head penetrations for degradation due to PWSCC, the NRC issued Bulletin 2001-01, “Circumferential Cracking of Reactor Pressure Vessel Head Penetration Nozzles,” dated August 3, 2001 [81]. In response to the Bulletin, PWR Licensees provided their plans for inspecting RPV head penetrations and the outside surface of the heads to determine whether any nozzles were leaking.

In early March 2002, while conducting inspections of reactor pressure vessel head penetrations prompt by Bulletin 2001-1, FENOC the licensee for the Davis-Besse Nuclear Power Station (Davis-Besse) identified a cavity in the reactor vessel head near the top of the dome. The cavity was next to a leaking nozzle with a through-wall axial crack and was in an area of reactor vessel head that the Licensee had left covered with boric acid deposits for several years. The cavity in the Davis-Besse reactor vessel head was 4-inches by 5-inches down to the 3/8-inch stainless steel reactor vessel head cladding. The cavity was due to boric acid wastage. FENOC used the Midland reactor pressure vessel head as a replacement head at Davis-Besse. Immediately following the discovery of the cavity in the reactor vessel head the plant was not given approval to return to power until a NRC investigation of this incident was completed. FENOC did not receive approval from the NRC to return Davis-Besse to power until early March 2004. On March 18, 2002, the NRC issued Bulletin 2002-01, "Reactor Pressure Vessel Head Degradation and Reactor Coolant Pressure Boundary Integrity, which requested PWR Licensees to provide information on their reactor vessel heads and their boric acid inspection programmes. In their responses, the Licensees provided information about their boric acid inspection programmes and their inspections and assessments to ensure that their respective plant did not have reactor vessel head degradation like that identified at Davis-Besse. The NRC concluded that none of the 68 plants in the USA had boric acid wastage like the Davis-Besse plant.

The experience at Davis-Besse and the discovery of leaks and nozzle cracking at other plants reinforced the need for more effective inspections of RPV head penetration nozzles. The absence of an effective inspection regime could, over time, result in unacceptable circumferential cracks in RPV head penetration nozzles or in the degradation of the RPV head by corrosion. These degradation mechanisms increase the probability of a more significant loss of reactor coolant pressure boundary through ejection of a nozzle or other rupture of the RPV head. The NRC issued Bulletin 2002-02 Reactor Pressure Vessel Head and Vessel Head Penetration Nozzle Inspection Programs," dated August 9, 2002, requesting that Licensees provide information about their inspection programmes and any plans to supplement existing visual inspections with additional measures (e.g. volumetric and surface examinations). Licensees responded to Bulletin 2002-02 with descriptions of their inspection plans for at least the first re-fueling outage following the issuance of Bulletin 2002-02 or with a schedule to submit such descriptions before the next re-fueling outage. Many of the Licensees' responses to Bulletin 2002-02 did not describe long-term inspection plans. Instead the Licensees stated that they would follow guidance being developed by the industry-sponsored Materials Reliability Program.

Inspections performed at several PWR plants in late 2002 found leakage and cracks in nozzles or J-groove welds that have required repairs or prompted the replacement of the RPV head. In addition, as discussed in NRC Information Notice 2003-02, "Recent Experience with Reactor Coolant System Leakage and Boric Acid Corrosion," issued January 16, 2003, leakage has recently occurred at some plants from connections above the RPV head and has required additional assessments and inspections to ensure that the leakage has not caused significant degradation of RPV heads.

The operating history of PWRs supports a general correlation among certain operating parameters, including the length of time plants have been in operation, and the likelihood of occurrence of PWSCC of nickel-based alloys used in RPV head penetration nozzles. Bulletin 2002-02 presented a three-tier categorization of susceptibility to RPV head penetration nozzle degradation based on reactor operating durations and temperatures. Licensees' responses to the Bulletin included an estimate of the effective degradation years (EDY) and the appropriate categorization of each plant into one of the three susceptibility categories. Each Licensee proposed an inspection plan for RPV head penetrations based upon the susceptibility to degradation via PWSCC (as represented by the value of EDY calculated for the facility). In addition, recent operating experience has shown that, under certain conditions, leakage from mechanical and welded connections above the RPV head can lead to the degradation of the low alloy steel head by boric acid corrosion.

The Licensees' actions to date in response to the NRC bulletins have provided reasonable assurance of adequate protection of public health and safety for the near term operating cycles, but cannot be relied upon to do so for the entire interim period until NRC regulations are revised. Additional periodic inspections of RPV heads and associated penetration nozzles at PWRs, as a function of the unit's susceptibility to PWSCC and as appropriate to address the discovery of boron deposits, are necessary to provide reasonable assurance that plant operations do not pose an undue risk to the public health and safety. Consequently, it was necessary to establish a minimum set of RPV head inspection requirements, as a supplement to existing inspection and other requirements in the ASME Code and NRC regulations, through the issuance of an Order to PWR Licensees.

PWR Licenses were notified in a letter dated February 11, 2003 (NRC Order EA-03-009) that the NRC established interim inspection requirements for RPV heads at PWRs. The inspection requirements are discussed in Section 5.1.1 of this report. Following the issuing of NRC Order EA-03-009, the ASME Code, Section XI published Code Case 694 which meets the intent of NRC Order EA-03-009. ASME Code, Section XI, Code Case 694 is also discussed in Section 5.1.1 of this document.

Operating experience in Japan

In may 2004, small boron deposit was found around one CRDM penetration and a thermo couple nozzle during a visual inspection of 70 CRDM penetration nozzles at Ohi Unit 3. (See Fig. 35) It was concluded deposit around the CRDM nozzle was attributed to leak from the penetration. The utility, Kansai Electric Power Company (KEPCO), investigated a root cause of this leakage and found that it was PWSCC at J-Weld. Grinding of the weld surface caused high tensile stress.

Operating experience in Germany

No cases of PWSCC or boric acid corrosion have been reported at the CRDM penetrations in German PWR RPV. Nevertheless, as a result from the cases in Bugey and Davis-Besse, different inspections have been performed with different techniques on the CRDM penetrations to verify the crack free status of the locations.

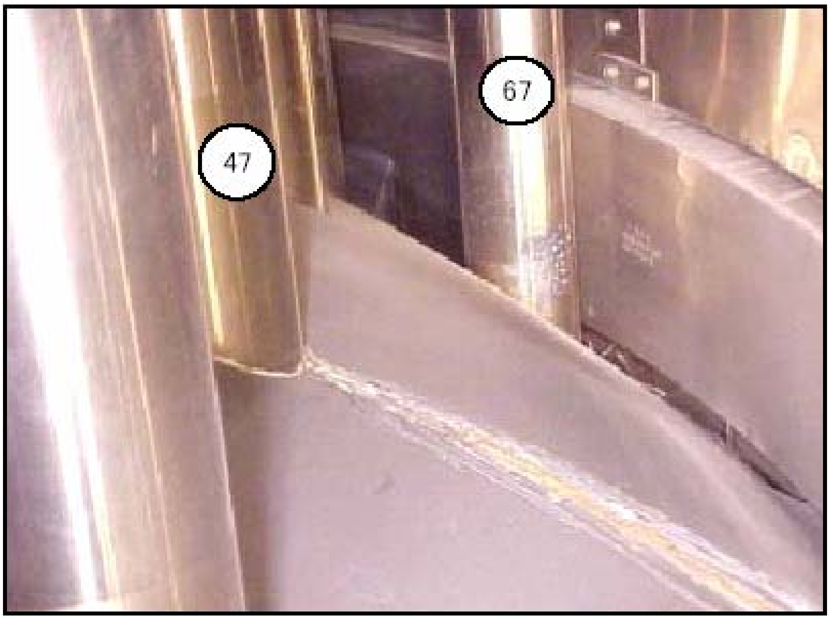


Fig .35. CRDM penetration leakage at Ohi #3 in Japan.

4.5.1.2. PWR bottom-mounted instrumentation (BMI) nozzles

Operating experience in the USA

Most RPVs have penetrations in the lower head for in-core nuclear instrumentation. These penetrations are typically made of nickel-based Alloy 600 and are welded to the inside of the RPV with nickel-based Alloy 82/182 materials.

In April 2003, small boron deposits around two of the 58 BMI penetrations (penetrations 1 and 46) were identified in South Texas Project Unit 1 (STP Unit 1) [82]. This was the only evidence of BMI nozzle penetration leakage reported by a U. S. facility to date. The STP Unit 1 BMI penetrations were constructed from drilled Alloy 600 bar stock and connected to the reactor vessel lower head by an Alloy 82/ 182 J- groove weld. The licensee subsequently performed nondestructive examination which included ultrasonic test, visual, and eddy current testing. As a result of this testing the licensee identified three axially oriented crack like indications in the penetration #1 nozzle wall and two axially oriented crack like indications in the penetration #46 nozzle wall. One of the indications in penetration #1 was characterized as an axial crack with a length of about 35.1 mm (1.38 inches), surface-breaking on the outside diameter (OD) of the nozzle above and below the J- groove weld, as well as surface- breaking on the inside diameter (ID) of the nozzle. The other two indications in penetration #1 were characterized as being small, embedded cracks near the interface between the nozzle wall and the root pass of the J- groove weld. One of the indications in penetration #46 was characterized as an axial crack with a length of about 24.9 mm (0.98 inches), surface breaking on the OD of the nozzle above and below the J- groove weld. The other indication in penetration #46 was characterized as an embedded crack having an axial length of 24.1 mm (0.95 inches).

The results of the UT inspection identified other features within the BMI penetrations which were deemed to be relevant by the licensee. UT reflectors were observed and characterized as “discontinuities” at the interface of the nozzle and the J- groove weld in all 58 of the STP Unit 1 BMI penetrations. These discontinuities were particularly evident in seven penetrations, including penetrations #1 and #46. The discontinuities in penetrations #1 and #46 were located in the same general azimuthal locations as the crack like indications.

To further investigate the potential root causes of the STP Unit 1 BMI penetration cracking, the licensee attempted to cut material samples (boat sample) using electrical discharge machining (EDM) tool, from penetrations #31 and #46 for destructive examination. Due to the difficulties of the EDM cutting process, only one sample, from penetration #1, was successfully removed and destructively evaluated. The penetration #1 boat sample was taken from the same azimuthal location as the 35.1 mm (1.38 inches) flaw and was intended to sample the nozzle and J- groove weld material. The penetration #1 also was intended to contain portions of the 35.1 mm (1.38 inches) flaw as well as one or more of the observed UT discontinuities at the nozzle- to- weld interface. Fig. 36, discussed in more detail below, provides a composite overlay of the penetration 1 boat sample with the tube (nozzle) wall and the penetration 1 J- groove weld.

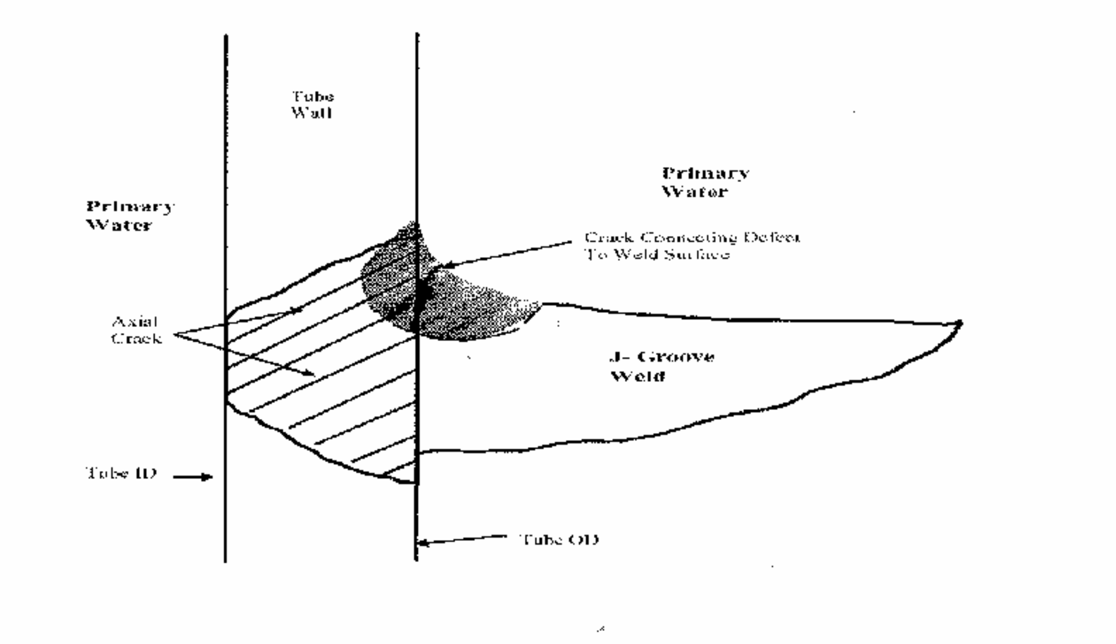


Fig. 36. Overlay of cross section of boat sample from STP Unit 1 penetration #1 with drawing of penetration #1 tube and weld geometry.

The destructive examination of the STP Unit 1 penetration #1 boat sample provided the following information:

(1) The axial crack in the penetration #1 tube wall was entirely intergranular in nature and consistent with primary water stress corrosion cracking (PWSCC) as the mechanism of crack propagation. Essentially no PWSCC of the J- groove weld material was observed in the boat sample.

(2) The UT discontinuity at the tube- to- weld interface, which was captured in the boat sample (the dark area in the boat sample in Fig. 36) was confirmed to be a weld lack-of-fusion zone from initial fabrication. A weld material ligament of approximately 2.0 mm (80 mils (0.080 inch)) separated the weld lack- of- fusion zone from the surface of the J- groove weld. The length of the lack- of- fusion zone in the circumferential direction was about 5.1 mm (0.2 inch). The axial PWSCC crack in the tube wall was located at one end of the lack- of- fusion zone.

(3) A second crack like defect was observed in the weld material (the dark line in Figure 36), running in the circumferential direction. The length of this defect was about 5.1 mm (0.2 inch) consistent with the length of the lack-of-fusion zone. This defect was completely through the 2.0 mm (80 mils) weld ligament and would have permitted primary water to leak into the lack-of-fusion zone. The precise mechanism for the initiation and propagation of this defect through the weld material could not be determined from the boat sample. However, its location and size relative to the associated lack-of-fusion zone suggest that the formation of this defect was related to initial fabrication processes. Based on this information, and the results of the NDE, the licensee concluded that the following scenario most likely explains the PWSCC flaws observed at STP Unit 1:

(4) Initial fabrication of the STP Unit 1 BMI penetrations resulted in lack-of-fusion zones between the nozzle (tube) and the J- groove weld. In addition, in penetrations #1 and #46, conditions existed from initial fabrication, which resulted in the

formation of defects through the J- groove weld, subsequently allowing primary water to flood the embedded lack- of- fusion zones early in its operating history.

(5) Primary water flooding of the embedded lack-of-fusion zones established conditions (i. e., a high- temperature, high- purity water environment, a susceptible material, and high local stresses) which are known to promote PWSCC.

(6) PWSCC flaws initiate "inside" the weld joint, adjacent to the lack- of- fusion zones, and propagate through the tube wall, eventually establishing a leakage path to the exterior of the reactor pressure vessel lower head.

In addition to the boat sample analysis discussed above, this scenario is supported by the observation that the large, 24.1 mm (0.95 inches) flaw in penetration #46 was not, based on NDE results, surface- breaking either on the ID of the nozzle wall or above the J- groove weld. Assuming that the same mechanism was responsible for all of the flaws observed in the STP Unit 1 BMI penetrations, this observation points toward a scenario that is not dependent on PWSCC initiation at a normally wetted surface.

Operating experience in France

The major fabrication difference between French and US manufacturing is the post weld heat treatment done in France in all cases (with the vessel heat treatment). Nevertheless, different inspections have been done with different techniques (visual, die penetrant and Eddy Current) on the weld and the base metal inner surface: no crack has been encountered for the moment [83].

Operating experience in Japan

In January 2003, one small indication was detected at one BMI penetration nozzle as a result of eddy current test of 50 BMI penetrations at Takahama Unit 1. The indication was within the acceptance criteria (≤ 3mm depth). The utility of the unit concluded that it could possibly be initiation stage of SCC. The utility performed water jet peening on inner surfaces of the BMI penetration nozzles as a preventive measure.

Operating experience in Germany

In German PWR there are no penetrations through the RPV bottom head.

4.5.1.3. PWR nozzle safe ends

Operating experience in the USA

Many RPVs have RPV nozzle and the safe end that are welded together with nickel-based Alloy 82/182 materials. As discussed in the previous sections, these materials are susceptible to PWSCC.

"A" loop RCS hot leg pipe was observed at the V. C. Summer Nuclear Station [84]. Ultrasonic testing (UT) and eddy current testing (ET) identified an axial crack-like indication approximately 68.6 mm (2.7 inches) long located approximately 7 degrees counterclockwise from top dead center of the first weld between the reactor vessel nozzle and the "A" loop hot leg piping approximately three feet from the reactor vessel. Based on the UT data, the axial crack-like indication began at the inner diameter and shows evidence of complete through-wall extension. Visual examination from the outer diameter identified a small "weep hole" in the center of the weld at approximately the same circumferential location as the UT and ET indications.

Based on non-destructive examination (UT, ET, and visual) results, the "A" loop hot leg weld was cut out and destructively tested. The 68.6 mm (2.7 inches) long indication was determined to be an axial crack approximately 63.5 mm (2.5 inches) long and almost through wall which was caused by PWSCC. High tensile stresses were present in the weld as a result of extensive weld repairs during original construction and these stresses were considered a contributing cause for the PWSCC. The extensive weld repairs complicated previous ISIs of the weld because weld roughness made it difficult to perform UT on portions of the weld. In addition to the axial crack, the licensee identified several other ET indications in the "A" loop hot leg weld. The destructive examination of the "A" loop hot leg weld confirmed that a number of the ET indications were PWSCC cracks. This is the only instance of PWSCC in reactor vessel nozzle to safe end welds observed, to date. Although the V.C. Summer reactor vessel is the only reactor vessel nozzle-to-safe-end weld to have PWSCC, there have been two other instances of PWSCC in nozzle-to-safe-end welds in the reactor coolant system. PWSCC was observed during a re-fueling outage in 2003 at Three Mile Island Unit 1 in the nozzle-to-safe-end weld connecting the steam generator “A” hot leg of the primary coolant loop to the pressurizer surge line. PWSCC was also observed at Palisades during a heat-up following a re-fueling outage in September 1993 in the nozzle-to-safe-end weld heat affected zone between the power operated relief valve (PORV) line and the PORV pressurizer nozzle.

Operating experience in France

Only 3 French plants have Alloy 182 safe end welds (only on RPV nozzles); they are post weld stress relieved with the RPV, and the basic ISI programme is applied for these welds, considering no specific sensitivity of these areas regarding PWSCC. No ISI has been done on these (young) plants in the first five years of operation.

Operating experience in Germany

The design and/or material selection for the nozzle-to-safe-end weld is different in German PWR RPV. Two solutions have been applied: either the safe-end to nozzle weld is performed with buttering and welding using austenitic welding material or alternatively the welds are performed with nickel base alloy material 82. In any case, the material in contact with the primary water is either stainless steel or Alloy 82. Up to now no problems have been encountered with this design

4.5.1.4. Other locations

Operating experience in France

Some repairs of cladding of nozzle bore (on 7 3-loop plants) have been done with Alloys 182. All of them have been inspected during the second 10-year shutdown and no crack has been reported [83].

The radial keys that are welded on the RPV inner surface are Alloy 600 material welded with Alloy 182. This location is not normally a relevant location in term of PWSCC initiation, nevertheless an inspection tool is under development to make some compensatory inspections on some plants [83].

Steam Generator partition plate (hot leg side) is considered as a possible precursor due to higher temperature and no stress relief. 42 stub welds in the hot side and 26 in the cold side has been inspected and no crack was found [83].

Today, no crack in the weld metal (Alloys 182- 82) has been found, in any locations.

4.5.1.5. *Safety significance*

The inspection results have shown that most of the crack are initiated first on the inside surface of the penetration and have an axial orientation, circumferential cracks appears later in few cases. Crack growth analyses show that these cracks are not likely to lead to a catastrophic failure of the penetration, because Alloy 600 is very ductile and the critical flaw size are large enough to previously detect a leak in normal operation.

The safety significance of circumferential cracks initiated on the outside surface may also be limited because the crack growth rates are likely to be low. The circumferential crack has to grow through the thickness and around the circumference before the penetration can rupture. The results of one analysis show that such crack propagation would take much more than the current license period [85]. Other analyses have shown that short circumferential cracks on the outside surface are possible; however, these cracks are not expected to become through-wall and cause rupture because of the comprehensive axial stresses present in front of the cracks [86].

Limited field experience suggests that, during normal operation, leakage of the primary coolant from a through-wall axial crack is unlikely because of the tight fit between the penetration and the reactor vessel head will prevent opening of the crack and will restrict the leakage. (Note that no boric acid deposits were detected at the Bugey 3 plant, where leakage was detected only during a hydrotest; this means that little or no leakage took place during operation.) However, if a leak occurs it will be at least 9 years, according to one analysis, before the boric acid corrosion of the vessel head could challenge the structural integrity of the head [87]. It is very unlikely that such leakage could remain undetected.

When considering the use of crack growth rates of a given crack, it must be realized that the French and the American growth rates are different. The American growth rates are much faster than the French crack growth rates. The American derived crack growth rates are those developed by EPRI. The EPRI crack growth rates were experimentally developed by periodically loading and unloading the test specimen. By loading and unloading the specimen crack is extended by fatigue and the crack front becomes then a new crack. The France (EDF) utilizes constant loading of the test specimens without periodic fatigue at the crack tip. The EDF crack growth rate is much slower than the crack growth rates reported by EPRI.

There has been little or no experience feedback on certain other Alloy-600 components such as the radial keys, vent nozzles, or bottom head penetrations. Some investigations of the bottom penetrations are still in progress, mainly in France and USA. The bottom penetrations operate at lower temperatures than the CRDM penetrations and are generally stress relieved, but some have been installed without stress relief and were distorted during the fabrication process. These penetrations may be susceptible to SCC.

In principle, the above statement would apply to the BMI at the South Texas Project Unit 1 (STP Unit 1) indications/leakage. However, it was determined that the leakage at STP Nuclear Power Station Unit 1 was not due to PWSCC. If circumferential cracking of BMI occurs, for whatever reason, it can be considered safety significant because it would result in a small LOCA and could extend to a large LOCA if not detected.

The PWR nozzle safe end cracking that occurred at the V.C. Summer Nuclear Station in the USA and at Ringhals Units 3 & 4 in Sweden can only be considered safety significant if the cracking/leakage is not detected prior to crack growth to a critical size. The safe ends in both the V.C. Summer Nuclear Station and the Ringhals Units 3 & 4 were unique in that the safe ends were fabricated from Alloy 600 rather than stainless steel. In addition, leakage occurred at the V.C. Summer Nuclear Power Station but did not progress to leakage at Ringhals Units 3 & 4. The crack-like indication was axial, thus unlikely to result in a double ended pipe break (large LOCA), but could result in a small LOCA. Use of Leak-Before-Break (LBB) analysis would have predicted leakage of the V.C. Summer Nuclear Station axial crack like indication. However, if leakage or cracking is not detected prior to growth to a critical crack size, a LOCA may occur which is safety significant.

Finally, all the different locations with Alloy 600, Alloy 182 and Alloy 82 need a minimum of investigation and evaluation to assure the susceptibility of the different locations to PWSCC (e.g. radial keys, Alloy 182/82 welds).

4.5.1.6. Stress corrosion cracking of WWER pressure vessel components

Nickel based alloys have not been used in the WWER pressure vessels. The cladding, penetrations and welded joints between the head materials and austenitic tubings (for control rods instrumentation, etc.) are made of austenitic materials of the same type as the cladding itself, i.e. the first layer/bead on the ferritic material is Type 25/13 material and the upper layers are stabilized Type 18/10 austenitic stainless steel.

Consequently WWER plants are not concerned by PWSCC of Alloys 600 materials.

4.5.2. General corrosion and pitting on the inside surfaces

Corrosion can commonly lead to uniform material loss, shallow pit formation, pitting or selective attack at the surface. Often the metal is relatively uniformly removed. However, when there are inhomogeneities at the metal surface and/or local differences in the electrochemical reactivity of the environment, the creation of local cells is possible which commonly results in local corrosion attack, causing shallow pit formation or severe pitting. Pitting of chromium or chromium nickel alloyed steels is mainly caused by the action of chloride ions. Pitting is often combined with transgranular stress corrosion cracking (TGSCC) of austenitic stainless steel material. Such incidents occurred for example with the sealing surfaces of the nozzle flanges (close to the O-ring) in some WWER pressure vessels. In the case of selective corrosion, the attack is concentrated on distinct material phases or regions along the gram boundaries of the metal A well known type of selective corrosion is the intergranular corrosion of sensitized austenitic stainless steel, which in principle can be neglected in PWR pressure vessel environments due to the reducing atmosphere.

The interior of the western RPVs are clad with austenitic stainless steel that provides good general corrosion resistance in the PWR environment (the metal loss rate caused by uniform corrosion attack is smaller than 5 µm per year). Even in the one known case where one region of the RPV (the beltline region) was unclad, due to a poor cladding process, the general corrosion rate was so low that it has been concluded that general corrosion is not a significant factor in western RPV service life.

Most of the WWER 440/V-213 and V-230 RPVs are protected against corrosion by a relatively thick (8 mm) austenitic stainless steel cladding, with stabilized austenitic material used for the outer layers. However, there are some WWER 440/V-230 RPVs which do not have their inside surfaces clad with stainless steel (see Table 29). These low alloy steel vessels are therefore exposed directly to the primary coolant. All unalloyed or low alloyed ferritic steels are subject to the formation of a magnetite protection layer as a consequence of the reaction between the water and the steel (iron) at operating temperature. Nevertheless, large scale surface corrosion and pitting has been observed in most of these vessels (in the core region and in the nozzle to safe-end zone). This corrosion was caused by the oxygen pick-up during shutdown periods which remained in the primary system for a period after startup.

Significance

As long as the water chemistry regime is controlled within its specified limits, general corrosion, pitting and selective corrosion on the inside surface is not a severe matter of concern for the ageing management of the RPV. Care has to be taken to protect unclad surfaces during shutdown periods and to use maintenance auxiliaries to avoid the ingress of impurities in unacceptable concentrations.

TABLE 29. WWER-440/V-230 RPVS

PLANT	BOL	CLADDED	ANNEALED	DUMMIES
KOLA 1	1973	N	1989	1985
KOLA 2	1974	N	1989	1985
ARMENIA 1[a]	1976	N	1988	N
ARMENIA 2[a]	1979	Y	N	N
NOVOVORONEZH 3	1971	N	1987,1991	N,LLCAA
NOVOVORONEZH 4	1972	N	1991	N,LLCAA
KOZLODUY 1	1974	N	1989	1987
KOZLODUY 2	1975	N	1992	1988
KOZLODUY 3	1980	Y	1989	1987
KOZLODUY 4	1982	Y	N	N, LLC 1986
BOHUNICE 1	1978	Y	1993	1992, LLC 1983
BOHUNICE 2	1980	Y	1993	1985, LLC 1984
GREIFSWALD 1[B]	1973	N	1988	1986
GREIFSWALD 2[B]	1974	N	1990	N
GREIFSWALD 3[B]	1977	Y	1990	1986
GREIFSWALD 4[B]	1977	Y	1990	1986

[a] shut down 1989
[b] shut down 1990
LLCAA low leakage core after annealing
LLC low leakage core
Y yes
N no
BOL Beginning of operating life

4.5.3. Boric acid corrosion of outer surfaces

IAEA-TECDOC-1120 described the mechanism and operating experience of boric acid corrosion and concluded that boric acid leakage is not considered as a safety issue. However the Davis Besse case showed that boric acid corrosion can be a safety significant issue.

Mechanism

Leakage of primary coolant may cause boric acid corrosion (wastage) of RPV parts made from carbon steel or low-alloy steel materials and lead to loss of material. The primary coolant contains boric acid and some lithium hydroxide in solution, and its pH at 25°C (77°F) varies over the range of 4.2 to 10.5. The boric acid in the leaking primary coolant may cause wastage or general dissolution corrosion of carbon steel and low-alloy steel components. The corrosion rate appears to depend upon the pH of the solution, the solution temperature, and the boric acid concentration in the solution. Some studies have shown that the corrosion rates of the steel at pH values of 8 to 9.5 are six times those at pH values of 10.5 to 11 [88]. As temperatures increase to the boiling point of water, the water evaporates, the solution concentrates, and the corrosion rate increases at much faster rates. Concentrated boric acid is highly corrosive at ~95°C (~200°F).

Operating experience

Davis Besse

In early March 2002, while conducting inspections of reactor vessel head penetrations prompted by Bulletin 2001-01, the Licensee for the Davis-Besse Nuclear Power Station (Davis-Besse) identified a cavity in the reactor vessel head near the top of the dome [89]. The cavity was next to a leaking nozzle with a through-wall axial crack and was in an area of the reactor vessel head that the Licensee had left covered with boric acid deposits for several years. The cavity extended through the low alloy steel vessel head and terminated at the stainless steel clad which was welded to the inside surface of the RPV head. The cause of the leakage was a crack in one of the Alloy 600 head penetration tubes resulting in the primary coolant being carried to the RPV head. Upon discovery of the waste at the Davis-Bessie RPV head the plant was not permitted to return to power. FENOC used the head from the cancelled Consumer Power's Midland plant. After being out of service for approximately 2 years, the Davis-Bessie plant returned to power in the spring of 2004.

Turkey Point Unit 4:

In 1987, the plant operating staff found over 230 kg (500 pounds) of boric acid crystals on the RPV head. They also found crystals in the exhaust cooling ducts for the CRDMs. After removing the boric acid and steam cleaning the head, the plant staff noted severe corrosion in several areas [90].

The cause of the boric acid buildup was a leak from a lower instrument tube seal (conoseal) onto one of the in core instrument tubes. The "small leak" was noted during an outage in August 1986 because of the buildup of some boric acid crystals. In October 1986, during an unrelated shutdown, the staff found about 0.03 m^3 (one cubic foot) of boric acid crystals on the head; they subsequently removed the crystals. In both cases, the staff deemed the leak rate acceptable for continued operation. The borated reactor coolant leaking from the conoseal flowed down the head insulation and beneath the insulation to the exposed head. This caused damage to the head, the conoseal clamps and some of the head bolts. Three of the 58 head studs were so corroded that the bolts and nuts had to be replaced.

Additionally, vapors containing bone acid had been borne into the CRDM cooling coils and ducts and condensed there, forming crystals. The control rod drive cooling shroud support was also so severely corroded that it had to be replaced.

During extensive inspections of the entire head area, the plant staff found either heavy deposits and/or general corrosion in many areas. Several components had wasted away [90]. Fig. 37 shows the corroded areas on the Turkey Point head.

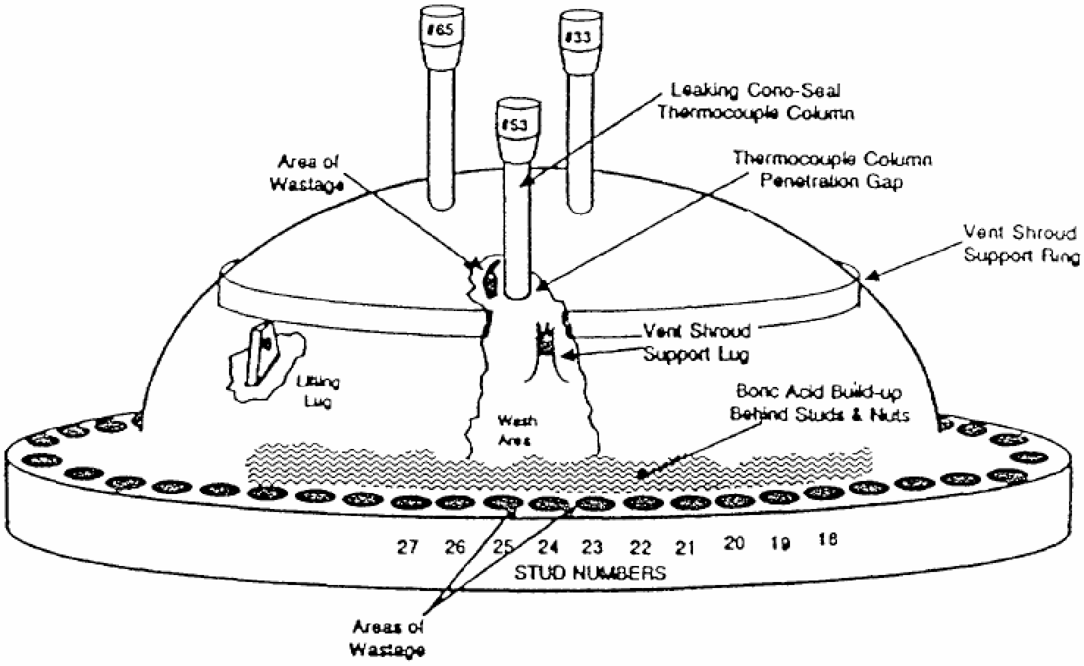


Fig. 37. sketch of the Turkey Point Unit 4 vessel head showing the areas affected by boric acid corrosion.

Salem Unit 2:

During an unplanned cold shutdown in 1987, the plant staff found boric acid crystals on a seam in the ventilation cowling around the head. An inspection team removed some of the cowling and insulation and discovered a pile of boric acid residue near the head. The size of the pile was about 0.9 × 1.5 × 0.3 m. Pitting was found beneath the deposit. Nine pits ranged from 25 to 76 mm in diameter by 9 to 10 mm deep. The minimum thickness of the material in these areas still exceeded the minimum required design thickness.

The boric acid buildup was attributed to a leak in a seal weld at the base of the threaded connection for the thermocouple instrumentation Borated water had leaked onto the head from ventilation supports.

Ringhals Unit 2:

The plant staff noted a somewhat higher leakage rate than usual from the primary system during the summer of 1985. The leakage rate was about 2-3 times higher than expected but well within the limit of the technical specifications. Despite extensive searching, the staff could not find any leaking valve and thus assumed that maintenance of the steam generator had been less effective than usual and attributed the higher rate to steam generator leakage.

During startup after a shutdown in December 1985 for preventive steam generator maintenance, inspection showed that the reactor flange was leaking. The head was lifted and the reactor flange and head flange were cleaned and inspected. The reactor flange had some minor defects in the groove for the 0-rings. Four to six head studs had been affected by the leakage. The studs were cleaned and inspected.

Germany

Major inspections have been performed in German PWR RPV after the incident in Davis-Besse. No cases of boric acid corrosion have been reported for German PWR RPVs up to now.

Significance

Field experience and test results indicate that the corrosion rates for carbon steels and low-alloy steels exposed to primary coolant leakage are greater than previously estimated and could be unacceptably high. The field experience is mainly associated with the carbon steel and low-alloy steel pressure boundary components such as closure bolting and carbon steel safety valve bonnets. The related field experience with reactor pressure vessel head is summarized here. In one incident, leakage from the CRDM housing penetrated the reactor vessel head insulation at Salem Unit 2 and ran down along one side of the reactor vessel head. Three reactor vessel head bolts were severely corroded and had to be replaced [91]. In addition, nine corrosion pits of 25 to 76 mm (1 to 3 inches) diameter and 9 to 10 mm (0.36 to 0.4 inches) deep were found in the reactor vessel head [92]. Turkey Point Unit 4 personnel discovered more than 227 kg (500 lb) of boric acid crystals on the reactor vessel head in 1987. The cause was a leak from a lower instrument tube seal (Conoseal) of one of the in-core instrument tubes [93, 94]. About 0.028m^3 (one cubic foot) of boric acid crystals had been removed from the same area in 1986. Vapors containing water-soluble boric acid had been borne into the upper CRDM area, and into the CRDM cooling coils and ducts. The CRDM cooling shroud support was severely corroded and required replacement. High carbon steel acid wastage was the cause of a cavity found in the First Energy Nuclear Operating Company (FENOC) Davis-Besse reactor. The boric acid wastage was through the reactor vessel head down to the 3/8-inch (9.5 mm) stainless steel cladding. The cavity in the Davis-Bessie RPV head was reported as 4 inches by 5 inches deep. Reference [95] reported carbon steel wastage rates as high as 4,800 mils/year at approximately 212 ºC. Dry boric acid crystals can result in waste of carbon steel, however with humid conditions or leakage of primary coolant onto boric acid crystals can result in much higher carbon steel wastage rates.

The observed boric acid corrosion rates are relatively high. Therefore, it is important to ensure that adequate monitoring procedures are in place to detect boric acid leakage before it results in significant ageing of the reactor pressure vessel, such as wastage of low-alloy steel base metal.

4.6. WEAR

Wear is defined as the motion between two surfaces that results in the removal of material surface layers. Wear occurs in parts that experience intermittent relative motion. Wear may occur due to either flow induced vibration or displacement of adjacent parts. Incore instrumentation tubes (flux thimble tubes) in some Westinghouse plants have exhibited wear due to flow induced vibration. However, wear is not considered to be a significant ageing mechanism for RPV, because the only location concerned is the RPV bolted flange, and the degradation can be detected by visual inspection long before the effects of wear begin to compromise the RPV structural integrity. All the other locations are analysed in the Reactor Vessel Internals report [96].

5. INSPECTION AND MONITORING REQUIREMENTS AND TECHNOLOGIES

5.1. NDE REQUIREMENTS

5.1.1. Requirements in the United States of America

Requirements for RPV head penetration nozzles

RPVs in the USA are inspected in accordance with Section XI of the ASME Code [17]. There are three types of examinations used during ISIs: visual, surface and volumetric. The three types of in-service inspections are a carry-over from the pre-service inspection (PSI) that are required in Section III of the ASME Code [14]. Each NPP follows a pre-service and in-service programme based on selected intervals throughout the design life of the plant. The in-service category is described in Table IWB 2500-1 of Section XI of the ASME Code, which details the inspection requirements. The ISI intervals are determined in accordance with the schedule of Inspection Program A of IWA-2410, or optionally Inspection Program B of IWA-2420. All shell, head, shell-to-flange, head-to-flange, nozzle-to-vessel welds and repair welds (repair depth greater than 10% of wall thickness) in the beltline region must subjected to a 100% volumetric examination during the first inspection interval (over 3 to 10 years). Successive inspection intervals also require 100% volumetric examination of all of these welds. The nozzle inside radius sections must be subjected to volumetric examination during each of the inspection intervals. The external surfaces of 25% of the partial-penetration nozzle welds (CRDM and instrumentation nozzles) must have a visual examination during each inspection interval (leading to total coverage of all nozzles). However, as discussed below, it is expected that the external surfaces of all the partial-penetration nozzle welds of the CRDM instrumentation nozzles as well as the nozzles themselves must have a volumetric examination during each inspection interval.

Under consideration by Section XI of the ASME Code is the introduction of "Risk-Based-Inspection" of the RPV. Risk-Based-Inspection" is based on the used of probabilistic-risk assessment in determining the areas to be inspected and the number of intervals."

As a result of leakage, cracks, corrosion identified in the reactor vessel head near the top of the dome in Davis-Besse and the information received by the NRC from generic letters to licensees, the NRC issued Order EA-03-009 on February 11, 2003. [97] (Generic letters and information notices associated with this issue are discussed in section 4.5.1.1) In early 2003, consideration in the NRC was given to deleting the term "Interim" from the order EA-03-009 and issue it as an Nuclear Regulatory Rule. While rulemaking was the most expeditious way to codify the reactor pressure vessel head inspection requirements, it would not have been completed in early 2006. However, the NRC commissioners have directed the NRC staff to wait until publication of a Code Case of the ASME before starting the rulemaking process. The ASME published Code Case 694 in 2005. The NRC Order EA-03-09 is now in the rulemaking process. The NRC rulemaking on RPV heads and associates penetrations inspections (Order EA-03-009) is now expected to be completed in February 2007. Once the NRC Order EA-03-009 completes the rule making process, ASME Code Case 694 will required each licensee to determine the required inspection(s) for each refueling outage at their facility based on the susceptibility category of each reactor vessel head to PWSCC-related degradation, as represented by a value of EDY for the end of each operating cycle, using the following equation:

$$EDY = \sum_{j=1}^{n} \left\{ \Delta EFPY_j \exp\left[-\frac{Q_i}{R}\left(\frac{1}{T_{head,j}} - \frac{1}{T_{ref}} \right) \right] \right\} \tag{40}$$

where

EDY = total effective degradation years, normalized to a reference temperature of 600° F

$\Delta EFPYj$ = operating time in years at $T_{head,j}$
Q_i = activation energy for crack initiation (50 kcal/mole)
R = universal gas constant (1.103x10-3 kcal/mole °R)
$T_{head,}j$ = 100% power head temperature during time period j (°R = °F + 459.67)
T_{ref} = reference temperature (600°F = 1059.67°R)
n = number of different head temperatures during plant history.

This calculation is to be performed with best estimate values for each parameter at the end of each operating cycle for the RPV head that will be in service during the subsequent operating cycle. The calculated value of EDY shall determine the susceptibility category and the appropriate inspection for the RPV head during each refuelling outage.

The order EA-03-009 imposed additional inspection requirements. Licensees are required to address any findings from these inspections (i.e., perform analyses and repairs) in accordance with existing requirements in the ASME Code and 10 C.F.R. 50.55a. All Licensees were required to use the following criteria to assign the RPV head at their facility to the appropriate PWSCC susceptibility category:

High	(1) Plants with a calculated value of EDY greater than 12, OR (2) Plants with an RPV head that has experienced cracking in a penetration nozzle or J-groove weld due to PWSCC.
Moderate	Plants with a calculated value of EDY less than or equal to 12 and greater than or equal to 8 AND no previous inspection findings requiring classification as High.
Low	Plants with a calculated value of EDY less than 8 AND no previous inspection findings requiring classification as High.

All Licensees were required to perform inspections of the RPV head using the following techniques and frequencies:

For repaired RPV head penetration nozzles that establish a new pressure boundary, the ultrasonic testing inspection shall include the weld and at least one inch above the weld in the nozzle base material. For RPV head penetration nozzles or J-groove welds repaired using a weld overlay, the overlay shall be examined by either ultrasonic, eddy current, or dye penetrant testing in addition to the examinations required by (1)(b)(i) or (1)(b)(ii), below.

(1) For those plants in the High category, RPV head and head penetration nozzle inspections were required to be performed using the following techniques every refuelling outage.

(a) Bare metal visual examination of 100% of the RPV head surface (including 360° around each RPV head penetration nozzle), and

(b) Either:

(i) Ultrasonic testing of each RPV head penetration nozzle (i.e. nozzle base material) from two inches above the J-groove weld to the bottom of the nozzle and an assessment to determine if leakage has occurred into the interference fit zone, or

(ii) Eddy current testing or dye penetrant testing of the wetted surface of each J-Groove weld and RPV head penetration nozzle base material to at least two inches above the J-groove weld.

(2) For those plants in the Moderate category, RPV head and head penetration inspections were required to be performed such that at least the requirements of 2(a) or 2(b) are performed each refuelling outage. In addition the requirements of 2(a) and 2(b) shall each be performed at least once over the course of every two refuelling outages.

(a) Bare metal visual examination of 100% of the RPV head surface (including 360° around each RPV head penetration nozzle).

(b) Either:

(i) Ultrasonic testing of each RPV head penetration nozzle (i.e. nozzle base material) from two inches above the J-groove weld to the bottom of the nozzle and an assessment to determine

if leakage has occurred into the interference fit zone, or

(ii) Eddy current testing or dye penetrant testing of the wetted surface of each J-Groove weld and RPV head penetration nozzle base material to at least two inches above the J-groove weld.

(3) For those plants in the Low category, RPV head and head penetration nozzle inspections were required to be performed as follows. An inspection meeting the requirements of 3(a) must be completed at least every third refuelling outage or every five years, whichever occurs first. If an inspection meeting the requirements of 3(a) was not performed during the refuelling outage immediately preceding the issuance of the Order, the Licensee must complete an inspection meeting the requirements of 3(a) within the first two refuelling outages following issuance of the Order. The requirements of 3(b) must be completed at least once over the course of five years after the issuance of the Order and thereafter at least every four refuelling outages or every seven years, whichever occurs first.

(a) Bare metal visual examination of 100% of the RPV head surface

(including 360° around each RPV head penetration nozzle).

(b) Either:

(i) Ultrasonic testing of each RPV head penetration nozzle (i.e. nozzle base material) from two (2) inches above the J-groove weld to the bottom of the nozzle and an assessment to determine

if leakage has occurred into the interference fit zone, or (ii) Eddy current testing or dye penetrant testing of the wetted surface of each J-Groove weld and RPV head penetration nozzle base material to at least two (2) inches above the J-groove weld.

During each refueling outage, visual inspections are required to be performed to identify potential boric acid leaks from pressure-retaining components above the RPV head. For any plant with boron deposits on the surface of the RPV head or related insulation, discovered either during the inspections required by the Order or otherwise and regardless of the source of the deposit, before returning the plant to operation the licensee is required to perform inspections of the affected RPV head surface and penetrations appropriate to the conditions found to verify the integrity of the affected area and penetrations.

5.1.2. Requirements in Germany

ISI in Germany dates back to the late 1960s, when a large research and development programme funded by the Federal Ministry for Research and Technology was launched. In 1972, a draft version for the Inservice Inspection Guidelines [98] of the Reactor Safety Commission was published and this document remained almost unchanged in the subsequent issues. This became the basis for the formulation of the German KTA 3201.4 Code [99], which today specifies the NDE requirements for ISI.

The inspection scope and the NDE-methods to be applied to a RPV are listed in Tables 30, 31 and 32. The ISI includes all welds, the nozzle radii, the control rod ligaments in the top head, the studs, nuts and threaded stud boreholes. The inspection intervals for the RPV are four years (for conventional vessels, it is five years); however, the scope of an inspection may be subdivided and each part carried out separately during the four year period, e.g., each year at the refuelling outage.

The inspection technique usually used is UT. The tandem technique is required for wall thicknesses larger than 100 mm. The inspection method and techniques have to be chosen to detect all safety relevant flaws in the planes perpendicular to the main stresses, the planes parallel to the fusion lines of the welds and the planes perpendicular to the welds.

TABLE 30. NON-DESTRUCTIVE IN-SERVICE INSPECTION ON THE REACTOR PRESSURE VESSEL

Item to be inspected	Test procedure / Test technique	Flaw orientation	Extent of testing	Test intervals
Longitudinal and circumferential welds	US	l, q	all weld seams, entire length, entire volume as well as the surface areas with their near-surface regions	5 years
Nozzle-to-shell welds ≥ DN 250 1)	US			
Nozzle inside edge ≥ DN 250 1)	US	r	surface areas with their near-surface regions of the entire inside edge of all nozzles	
Ligaments in nozzle fields	US		all ligaments, surface areas and centres of ligaments	
Cladding	SV	any	representative locations, the test extent shall be specified for the individual plant	
Screw bolts	US or MT or ET, SV	q, relative to the bolt axis	surface areas with their near-surface regions of all bolts, entire tensioned length including the threaded regions	Within 5 years 2) at least 25 % of the bolts with the corresponding threaded blind holes, nuts and washers, however, at three successive test intervals of 5 years 100 % shall be tested. Alternatively, the test may be performed at intervals of 10 years 2) where 100 % each shall be tested
Threaded blind holes	US or ET, SV	q, relative to the thread axis	surface areas with their near-surface regions of all blind holes, entire thread length	
Nuts	SV or ET or US	q, relative to the thread axis	threaded region and loaded end face (contact surface) of all nuts	
Washers	SV	any	both contact surfaces as well as the surface of the washer hole	
Attachment welds	Agreements shall be made because of the differing design details. The type and extent of the tests shall be incorporated in the test instructions.			
Auxiliary welds	MT or US	The requirements shall be specified in accordance with 5.2.1.1 (3).		
Abbreviations for the test procedures and techniques are explained in Table 2-1. l : longitudinal flaw q : transverse flaw r : radial flaw (e.g. for nozzle inside edges or ligaments in nozzle fields)				
1) In the case of nominal diameters of the connecting pipe < DN 250 the requirement for in-service inspections shall be reviewed from case to case. 2) Selective visual inspection of stud bolts (where accessible), nuts and washers after each unbolting of bolted joints				

TABLE 31. NON-DESTRUCTIVE IN-SERVICE INSPECTION ON PRESSURE-RETAINING WALLS OF CONTROL ROD DRIVERS

Item to be inspected	Test procedure / Test technique	Flaw orientation	Extent of testing	Test intervals
Circumferential welds PWR [1)]	ET or RT or US	l	Inner surface of representative welds on 10 % of pipes in due consideration of accessibility	10 years
Circumferential welds BWR	LT or ET or US	l	Inner surface of circumferential welds of 4 rod drive housing pipes	
Abbreviations for the test procedures and test techniques are explained in **Table 2-1**. l : longitudinal flaw				
[1)] These welds also include in-core instrumentation and control rod nozzle welds				

TABLE 32 TYPE OF TESTS, TEST PROCEDURES AND TECHNIQUES

Serial Number	Type of Test	Test Procedure	Test Technique
1	Examination with regard to cracks in the surface or in near-surface regions	Magnetic particle flaw detection	Magnetic particle examination (MT), magnaflux examination
		Liquid penetrant examination (LT)	e.g. dye penetrant examination
		Ultrasonic examination procedure (US)	e.g. surface waves, mode conversion, dual search units with longitudinal waves, electromagnetic ultrasonic waves
		Eddy-current examination procedure (ET)	Single frequency, multiple frequency
		Radiographic examination procedure (RT)	X-ray Radioisotope
		Selective visual examination (SV)	With or without optical means
2	Volumetric examination	Ultrasonic examination procedure (US)	e.g. single probe technique with straight (ES) or angle beam scanning, tandem (angled pitch-catch) technique, mode conversion
		Radiographic examination procedure (RT)	X-ray Radioisotope
		Eddy-current examination procedure for thin walls (ET)	Single frequency Multiple frequency
3	Integral examination	Integral visual examination	—
		Pressure test	—
		Functional test	—

UT inspection sensitivity — detection level

The detection level is defined as the limit of the echo height which can be detected over the noise observed when moving the probe (see Fig. 38). Therefore, it is mainly a function of the inspection technique and the data acquisition equipment should have little, if any, negative influence.

UT inspection sensitivity — recording level for subsurface defects

The size of defects to be detected is not defined explicitly in the code; but implicitly the authors had in mind that embedded flaws of 10 mm diameter should be detected in walls greater than 40 mm thick. Therefore, they defined the 10 mm diameter flat bottomed borehole as the recording level for specular reflection, e.g., by the tandem technique, and a 3 mm diameter flat bottom borehole as the recording level for detection using flaw tip scattering and diffraction techniques. A comparison of these reflectors with the ASME Section XI recording level sensitivity in terms of echo height is given in Fig. 39.

UT inspection sensitivity — recording level for surface defects

The calibration and sensitivity settings for surface inspection have to be done with notches. The depth of these calibration notches depends on the wall thickness and is 3 mm for wall thicknesses larger than 40 mm. The notches must have reflecting planes perpendicular to the surface: i.e. rectangular or triangular notches or spark eroded slots.

All indications with an echo height of the above mentioned notch echo, minus 6 dB or more have to be recorded. The influence of the cladding or the structure on the signals has to be evaluated using the component and has to be considered during data acquisition and data evaluation. For the commonly used 70° transmitter-receiver-longitudinal (TRL) wave-probe-technique, the echo height is comparable to a 6 mm diameter flat bottom borehole or more, and is near the echo height of a backwall. Since 1991, the recording level has been lowered 6 dB below the echo height of the notch, to give more distance to the backwall echo, but in some inspections from the outside, this level is also near to the noise from the cladding.

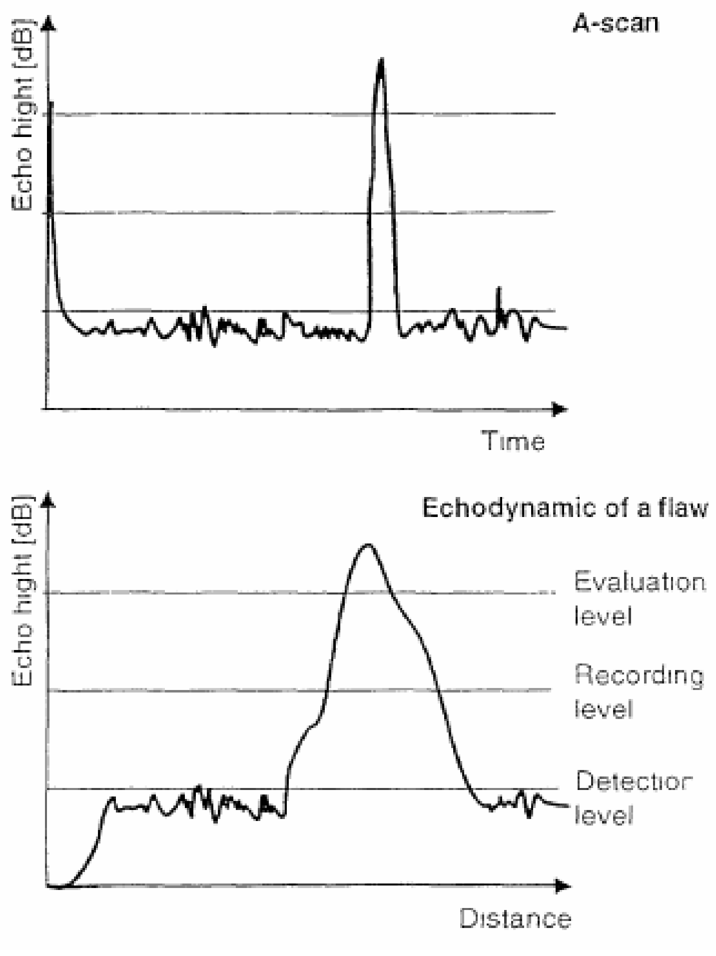


Fig. 38. Schematic diagram of the different amplitude levels of indicators. The upper sketch displays the signal amplitude as function of time, the so called A-scan, the usual screen picture from an UT – instrument. In most cases, only the high signal requires further evaluation. The lower sketch displays the flaw, signal as function of the probe movement and illustrates the detection level as defined by the surrounding noise signals. This is the relevant level for the sensitivity of an) automated inspection technique.

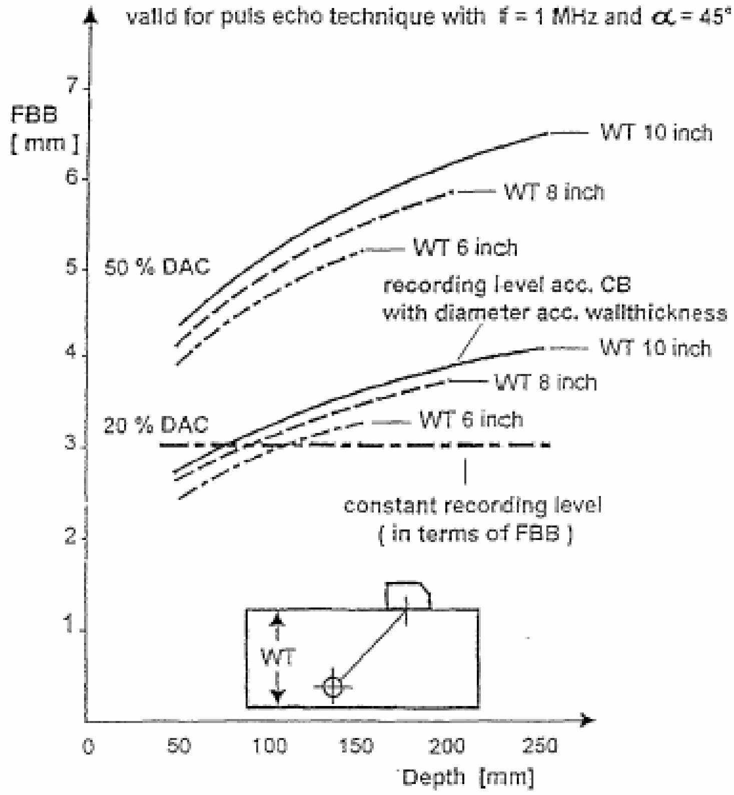


Fig. 39. Comparison of recording levels required by the ASME and KTA 3101.4 Codes for the case of a pulse echo technique with a 45 degree angle and a I MHZ frequency. The KTA-required recording level of a 3 mm flat bottom borehole (FBB) is constant over the wall thickness. The echos from the cylindrical boreholes follow a less strong distance dependence than do the echos from a FBB and therefore the recording level rises (the sensitivity decreases) with an increasing sound path.

UT inspection sensitivity — evaluation level

It is the philosophy of KTA 3201.4 that important indications should be evaluated and compared with earlier data to check on any possible growth of the flaws. Surface and near surface indications must be evaluated if the indication has an echo height 6 dB over the recording level or more, or if the indication has an echo height over the recording level and the indication length is more than half the wall thickness, or more than 50 mm. The method to evaluate the indication length has to be agreed with the TÜV. Subsurface indications must be evaluated if the same criteria are met, but the length of the reflector is measured by the length of the indication at -12 dB under the recording level.

Procedure for indications above evaluation level

The Code requires the analysis of indications above the evaluation level when they are found for the first time or if it is suspected that they are growing. Indications above the evaluation level must be compared with the results of the last inspection. If there is a change to a higher amplitude or a longer length beyond the usual tolerances, the results of all earlier inspections are compared to see if there has been a change in the course of the time. If there is evidence of a new or growing indication, one has to analyze the data for evidence of the kind, position, and size of the flaw. New measurements with specialized techniques may be necessary. If it is thereby confirmed that the defect is new or has grown, then it is necessary to find its root cause and prepare a safety analysis using, for example, the operation records. The safety analysis may include: fracture mechanics analysis, experimental investigations, and evaluations of the experience at other plants. The fracture mechanics analysis method (analysis of brittle fracture) applied for the RPV is dealt with in KTA 3201.2. (The ASME, Section XI, procedure could also be used). The safety factors and the crack growth velocity are usually taken from ASME Section XI.

The results of the safety analysis should determine whether the flaw can be accepted in the component or not; there is no general acceptance level independent of the specific circumstances.

As a result of the incidents in Bugey and Davis-Besse, many inspections have been performed to verify that no incidents are to be expected in German PWR RPVs, but no additional requirements due to PWSCC or boric acid corrosion have been stipulated.

5.1.3. Requirements in France

The requirements for the French ISI programmes are published in RSE-M [29]. The Code requires periodic hydrotests with acoustic emission monitoring during the hydrotests, NDE during the outages, a material surveillance programme, loose-parts (noise) monitoring during operation, leak detection during operation, and fatigue monitoring. The Code specifies a complete programme including both the utility and regulatory agency inspections. Areas of the RPV that must be inspected are listed in Table 33 and include the beltline region of the shell, all the welds, the top and bottom heads, the nozzles and safe end welds, the penetrations, the control rod drive housings, the studs, the threaded holes, and the supports.

One of the major differences between the French ISI programme and the programmes in other countries is the hydrotesting. A hydrotest at 1.33 times the design pressure (22.4 MPa) is required after the RPV fabrication is completed. A hydrotest at 1.2 times the design pressure (20.4 MPa) is then performed after every 10 years of operation. The 10-year internal tests must be performed at a temperature of $RT_{NDT} + \underline{12°C}$.

The NDE techniques which are used in France are also listed in Table 33 and include focused under water UT, radiography, visual examinations, tele-visual examinations under water, die-penetrant tests, acoustic emission monitoring, and eddy- current testing (ECT). UT of the welds generally covers the weld area plus about 50 mm of base metal on both sides of the weld. Base metal regions of the RPV shell subjected to fluences above 10^{22} n/m^2 are also inspected with ultrasonic, focused on the first 25 mm from the inner surface.

TABLE 33 INSPECTION TECHNIQUES IN FRANCE - SCOPE AND INTERVALS FOR RPV

Area to be Inspected	Inspection Method/Technique	Orientation of Flaws	Scope of Inspections	Inspection Intervals
Circumferential welds: core shells/ shell-flange/ nozzle-shell/shell-bottom head	Underwater UT	longitudinal/ transverse	100% welds and wear surface areas	Each CV[a]
Flange-head circum-ferential weld	UT	Longitudinal/ transverse	--	Between first and second CV
Underclad inner surface	Underwater UT	Longitudinal/ transverse	Irradiated area first 30 mm from the inner surface	After 20 years
Cladding surfaces	Visual/televisual	--	All the inner surface	After 20 years
Threaded stud boreholes	Televisual	--	--	If stud deterioration, every 2 years
Ligament in thread-hole area	Underwater UT	Longitudinal/ transverse	--	If stud deterioration, every 2 years
Radial key welds	Televisual	--	--	Each CV
CRDM and vent penetrations	Eddy-current, UT	--	Inner surface	Every year
Studs	Eddy-current	--	--	Three times in 10 years
Nuts	Eddy-current	--	--	Three times in 10 years
CRDM penetration welds, BMI penetration welds	Televisual + austic emission	--	--	--

(a) The first complete visit (CV) is after the first hydro test; the second CV is before 30 months of operation; the third is before 10 years of operation; and the rest are every 10 years.

For vessel head penetrations, all the major techniques were studied (dye penetrant, visual, eddy current) with the difficulty connected to the sleeve [83]. The solution used was "eddy current sword probe" for crack initiation and "ultrasonic sword probe" qualified using "Time Of Flight Diffraction Technique" (maximum thickness of two mm). A three mm surface bricking crack was sized with a good accuracy using these techniques. Today 12 vessel heads have not been replaced, two of them have no indication, the others have some limiting degradation. The frequency of the inspection is connected to crack height and shutdown frequency, between every one to three years [83].

For all the other locations, an adapted sampling is used for NDE.

5.1.4. Requirements for WWERs

This subsection is partly revised.

The WWER RPV ISI in accordance with [33] is carried out at least every four years (30,000 hours) of operation and includes NDE (visual, dye-penetrant, magnetic particle, ultrasonic and eddy-current), surveillance specimen evaluation, and hydraulic testing. Parts and sections of the reactor to be inspected, locations, volume, and periodicity are specified in the procedure. A change to an eight-year inspection interval for examination of the RPV inner surface is now under consideration by the regulatory bodies in the Czech Republic, Hungary, and Slovakia but all other type of inspections remains in their initial volume.

Examination of the RPV base and weld metal in the zones with stress concentrations or high neutron flux, the cladding/base metal interface, the nozzle transition areas, sealing surfaces, outer and inner surfaces of the vessel bottom and top heads, bolts, nuts, and threaded holes is obligatory. The inspections are carried out according to the requirements listed in Table 34. A special shielded cabin is used at some NPPs for visual and dye-penetrant inspections from the inside of the RPV, as well as for the repair of any defects. The ISI of the vessel head includes only a visual inspection and also a dye-penetrant inspection of sealing surfaces, welds, and cladding, performed at the locations which are accessible. Ultrasonic inspection of the circumferential weld is also performed.

The examination results are evaluated using PNAE G–7-010-89 [100]. These standards and procedures have been approved by the Russian regulatory body. Although they are not officially accepted by all the safety authorities responsible for WWER ISIs, they have been used in general at most of the WWER plants since no other procedure or standard for defect acceptance/ rejection was available, except in Hungary, the Czech Republic, and Slovakia - in these countries national procedures were developed which have been now incorporated into the VERLIFE procedure [42].

The ultrasonic examination equipment is calibrated using a flat bottomed hole according PNAE G–7-010-89 [100]. However, the most recent inspections in some plants have been performed using calibration methods similar to those used in the West. Regulatory bodies in all WWER countries now required full qualification procedure for the equipment, evaluation of results and personnel, principally in accordance with the ENIQ [101] (European Non-destructive Inspection Qualification) Group and/or the IAEA document [102] with some national modifications.

TABLE 34. WWER REQUIREMENTS FOR NDE

REACTOR TYPE REACTOR PART	WWER-440/V-230	WWER-440/V-213	WWER-1000/ V-320
CYLINDRICAL PART CORE BELTLINE	INSIDE	OUTSIDE/INSIDE	OUTSIDE/INSIDE
BOTTOM HEAD	INSIDE/OUTSIDE	INSIDE/OUTSIDE	INSIDE/OUTSIDE
NOZZLE AREA	INSIDE/OUTSIDE	OUTSIDE/INSIDE	OUTSIDE/INSIDE
SAFE-ENDS	INSIDE[1]/OUTSIDE	INSIDE/OUTSIDE	INSIDE/OUTSIDE
CLADDING	INSIDE	INSIDE	INSIDE

5.1.5 Requirements and practices in Japan

The basic inspection requirements are given in the JEAC-4205 [103], the Japan Electric Association Code for ISI of light water cooled nuclear power plant components and also in JSME Code on Fitness-for-Service for Nuclear Power Plants, JSME S NA1-2002 [104]. Requirements in them are same. Examination Categories B-A to B-D, B-F to B-H, B-J, B-N (in JSME Code JP-1), B-O and B-P (Section 2, Class 1 Components) prescribe the methods, inspection area and frequencies for the RPV ISI. The basic examination required is a periodic volumetric examination of the reactor pressure vessel weld lines. The following volumetric inspections are required at every inspection interval (10 years): 7.5% of all core belt region weld which receives neutron fluence $\leq 10^{23}$ n/m^2 (E>1 MeV) and other pressure-retaining welds than core belt region welds (i.e., shell, head and repair welds) in accordance with Examination Category B-A and B-B; all core belt region weld which receives neutron fluence $> 10^{23}$ n/m^2 (E>1 MeV), all head-to-flange weld, shell-to-flange weld and full-penetration nozzle welds (i.e., nozzle-to vessel welds) in accordance with Examination Category B-A, B-C and B-D. From the 4th examination (after 30 years of operation), the examination interval for Class 1 components becomes seven years.

CRDM penetration inspection

For CRDM penetrations, the current codes [103, 104] require a visual test (VT-2) during the RPV leak test.

In December in 2003, NISA, Japanese regulatory body published a notification which required the bare metal visual inspection of the RPV top and bottom heads. NISA also required PWR utilities in to establish an inspection programme of RPV heads taking into account the Davis Besse event and CRDM SCCs. The PWR utilities have produced a draft programme for review by NISA.

5.2. NDE TECHNIQUES

5.2.1. Ultrasonic examination methods

Smooth, sharp-edged flaws oriented in a plane normal to the vessel surface and located in the beltline region near the cladding/base-metal interface are the most critical type of flaws because the material in that area of the RPV exhibits the highest degree of neutron embrittlement and corresponding high RT_{NDT} and high tensile stresses (thermal) occur near the vessel inner surface during a PTS accident or a cooldown violating the P-T limits.

Such flaws are difficult to detect and size with an ultrasonic technique based on signal-amplitude alone, which was the technique originally developed for ISIs in the USA and elsewhere.

In the amplitude-based technique, the sensitivity setting of the ultrasonic equipment is referenced to a distance-amplitude correction curve, which can be obtained from an ASME reference block with one 3-mm (0.125-in.) side-drilled hole [105]. The ASME Section XI code (1986 Edition) specifies an amplitude cut-off level of 20% the distance-amplitude correction; only defect indications that exceed that level are recorded. ASME Section XI Code also specifies use of an additional scan angle of 70-degrees longitudinal wave to inspect clad-base metal interface regions [106, 107].

The amplitude-based technique uses the decibel-drop method to determine flaw sizes much larger than the width of the sound field [105, 108]. In the decibel-drop method, the transducer is positioned to obtain a maximum height for an echo from the defect, and then it is traversed until the height of the echo drops to a specified threshold (50% of the maximum height for the 6-decibel-drop method). This position of the transducer is assumed to be over the edge of the flaw. Similarly, the transducer is moved in other directions from the maximum height position, and finally the flaw size is determined. A flaw size much smaller than the width of the sound field can be determined by the 20-decibel-drop method (beam edge method) or by comparing the amplitude of the reflection from the flaw with a range of reflection amplitudes from various flat-bottomed holes in test blocks. The accuracy of flaw sizing by the amplitude-based technique depends not only on the transducer sound field size, acoustic impedance differences between the flaw and the surrounding material (that is, the ultrasonic reflectivity of the flaw) and the flaw size, but also on the orientation of the flaw, the surface condition and the ultrasonic scattering properties of the flaw. This technique is effective in sizing a smooth, flat flaw that is at a right angle to the ultrasonic beam and away from the clad-metal interface, but it under sizes near-surface and other flaws. Cladding surface roughness also affects sizing of the flaws; it causes scattering of the ultrasound, which may result in under sizing of near-surface flaws [109].

Tip-diffraction techniques developed in the United Kingdom more accurately size underclad and embedded flaws. With one of the tip-diffraction techniques, the time-of-flight diffraction (TOFD) technique, the difference in the travel times of ultrasonic waves diffracted from each of the flaw tips is measured to estimate the flaw size [110]. Examples of time-of-flight diffraction are depicted in Fig. 40 [111]. The technique consists of a separate transmitter and receiver oriented in opposite directions, as shown in Fig. 40(a). Two signals are present in the absence of a crack, a direct lateral wave signal and a backwall reflection signal from the opposite surface. Diffraction occurs when the incoming sound beams impinges upon a finite planar reflector such as a crack. The diffracted sound energy from the end or "tip" of the crack acts as a point source and radiates a sound wave to the receiving transducer. The time of arrival of this signal can then be used to pinpoint the tip of the crack and determine crack depth. Figure 40(b) illustrates such a diffracted signal produced by the tip of a surface crack: note the presence of a backwall reflection signal and the absence of a lateral wave signal.

Although cracking on the inside surface is a primary concern, cracking on the outside (back wall) surface could also occur. As illustrated in Fig. 40(c), the presence of an outside surface crack will cause the loss of a backwall reflection signal, but the lateral wave and the diffracted signal from the crack tip are present. In Fig. 40(d), two diffracted signals from the ends of an embedded crack are evident, and both a lateral wave and a backwall reflection signal are present.

Flaw orientation and roughness, which interfere with flaw sizing using amplitude-based techniques, have very little effect on flaw sizing with tip-diffraction techniques. Laboratory test results, including the Programme for the Inspection of Steel Components II test results, show that the tip-diffraction techniques are the most accurate for sizing underclad and embedded flaws [108, 112]. One disadvantage of the time-of-flight diffraction method is that the diffracted crack tip signals are often small in amplitude and can easily be confused with grain noise or other small amplitude reflectors. In addition, crack branches may interfere with the interrogating sound beam or cause additional diffracted signals. These additional signals may cause cracks to be undersized.

Flaws located in the nozzle-to-shell welds are also of considerable interest in assessing RPV integrity. The nozzle-to-shell welds can be ultrasonically inspected from the nozzle bore; however, sizing of the flaws is difficult when conventional (unfocused) transducers are used [108]. The main reason for this difficulty is the large distance between the nozzle bore and nozzle weld. At these distances, the ultrasonic beam of conventional transducers provides poor resolution of flaws in the welds. A large-diameter, focused ultrasonic transducer produces a small diameter beam at the flaw location and can be used for accurate mapping of flaw edges. Laboratory results show that the large-diameter focused transducers are substantially more accurate than unfocused transducers in sizing flaws in the nozzle-to-shell welds [113].

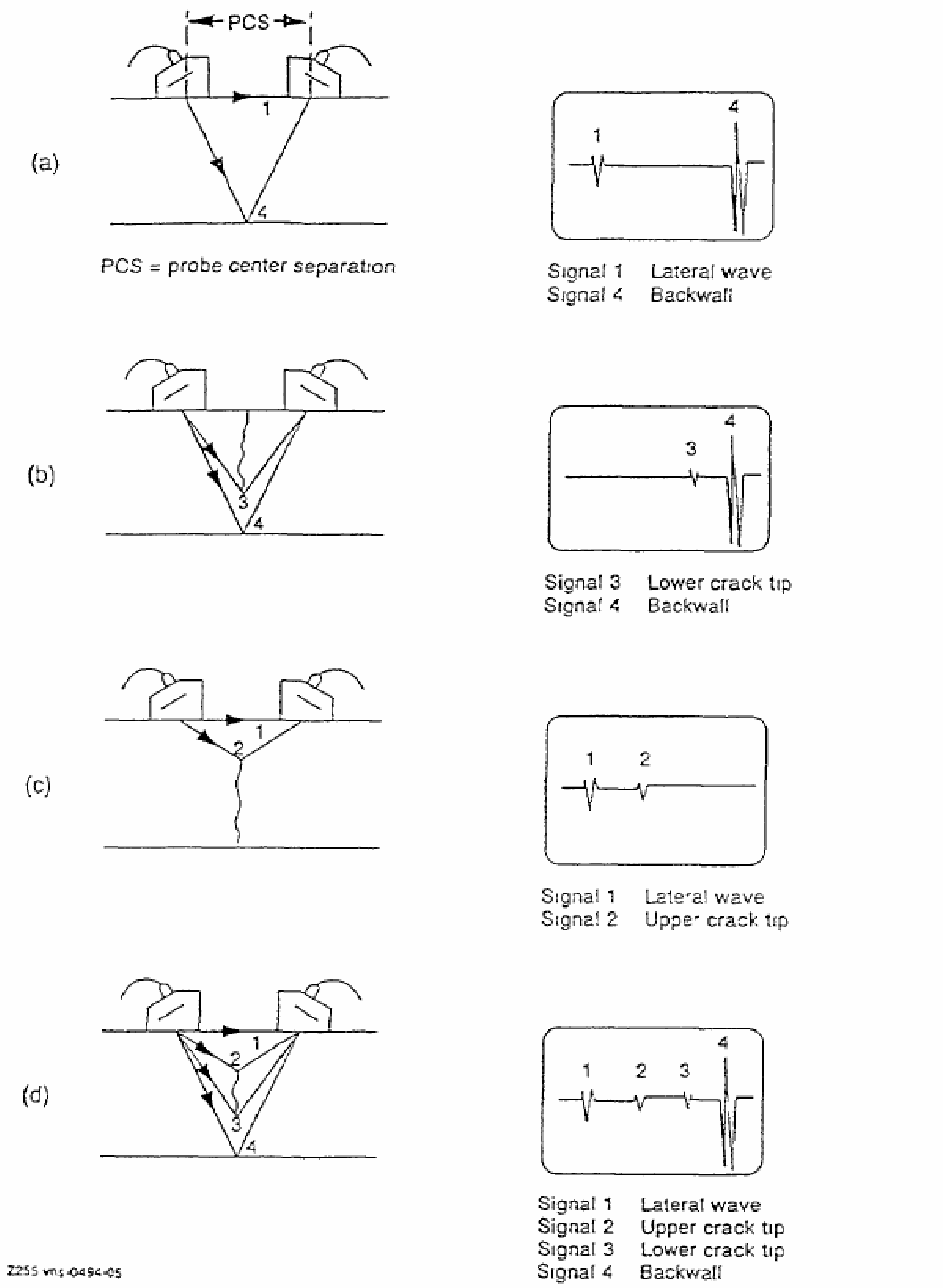


Fig. 40. Examples of time-of-flight diffraction (TOFD) signals (Pers-Anderson 1993) Copyright TRC, reprinted with permission.

Focused transducers are used commonly in France and Belgium, but infrequently in the United States. Examples of the applications of focused transducers are inspection of RPV welds and heat-affected zones in Westinghouse 350-MWe and Framatome 900-MWe reactors in Belgium and in a 660-MWe reactor at Krsko in Slovenia [114]. Also, a large-diameter focused transducer was used to inspect the nozzle-to-vessel welds of the Ginna reactor vessel during its second ISI interval [115]. This inspection with the focused transducer characterized the earlier detected ultrasonic indications as closely spaced slag inclusions; a conventional transducer was unable to resolve these closely spaced indications. Earlier, a focused transducer was used to characterize the flaws in the cladding under the head of the Yankee Rowe RPV[116].

Ultrasonic examination methods based on a phased array technique have also been developed for ISI of components which have complex geometries and have limited access and clearance. One such technique developed by Siemens has been used for inspection of the BWR feedwater nozzle inner radius regions, nozzle bore and nozzle-to-vessel welds; the BWR bottom head ligaments; and the PWR closure head ligaments [117]. This technique has also been used for inspection of PWR feedwater nozzles inner radius regions.

A phased array transducer consists of multiple elements that can be controlled individually to create a variety of beam patterns. The use of multiple elements with a computer controlled pulsing sequence results in the ability to steer and/or focus the sound beam. With an appropriate phase-shifting of the transducer elements, the focal length of the transducer can be changed and the specimen can be scanned in depth. The transducer design can be tailored to the needs of the specific examination. For example, the examination of a nozzle inner radius region employs a fixed incident angle with a variable skew angle whereas the vessel shell welds require a fixed skew angle with a variable incident angle. Echoes received in many cross-sectional directions are stored during inspection and echo tomography utilizes the spatial relationships of the signals in order to enhance the signal to noise ratio. The combination of these modes allows a rapid and accurate analysis of the reflectors. Flaw sizing is typically done with a tip diffraction method [118].

Recently, the ASME Section XI Code has developed more stringent requirements for demonstrating the performance of ultrasonic inspection procedures, equipment and personnel used to detect and size flaws at the susceptible sites in pressure vessels. The susceptible sites include the clad-base metal interface, nozzle inside radius section, reactor vessel structural welds, nozzle-to-vessel welds and bolts and studs. These requirements are needed to ensure that inspectors apply the appropriate ultrasonic inspection techniques in the field to correctly characterize the flaws at the susceptible sites in the vessel. These requirements are presented in two appendices of ASME Section XI: Appendix VII, Qualification of Nondestructive Examination Personnel for Ultrasonic Examination; and Appendix VIII, Performance Demonstration for Ultrasonic Examination Systems. Implementation of Appendices VII and VIII will take several years. The enhanced inspection programme will provide more reliable ISI data on US RPVs, which then may be used for the development of a plant-specific vessel flaw distribution or a generic flaw distribution more representative of operating PWR vessels than currently used distributions such as the Marshall distribution [119].

5.2.2. Acoustic emission monitoring

Acoustic emission methods may be used to monitor potential flaw growth in the beltline region welds and base metal if the outside surface of the vessel is accessible. Some PWR vessels are supported by neutron shield tanks, which will prevent access to the vessel outside surface.

An acoustic emission method for crack growth detection was tested at Watts Bar Unit 1 during hot functional testing. A preloaded, precracked fracture specimen was placed in the primary system to test the capability of the acoustic emission method to detect a signal during reactor operation. The specimen was designed such that the system operating temperature would impose thermal loads and cause crack growth. The test results showed that the coolant flow noise could be filtered out and that crack growth acoustic emission signals can be detected under operating conditions [120]. Acoustic emission was also used to monitor possible crack growth during the 1987 hydro test of the High Flux Isotope Reactor located at the Oak Ridge National Laboratory; no evidence of crack growth was detected [121].

Several significant steps have been taken to validate continuous, on-line acoustic emission monitoring in the field. Work on the application of the acoustic emission method at Watts Bar Unit 1 has shown that it can be effectively used for in-service monitoring of crack growth in thick wall, geometrically complicated components such as RPV nozzles [122].

Continuous acoustic emission monitoring has also been used by the Pacific Northwest National Laboratory to monitor a flaw indication in an inlet nozzle safe end weld at the Limerick Unit 1 reactor [123]. In addition, ASME Code Case N-471 has been developed and approved, which provides for continuous on-line acoustic emission monitoring for growth of known flaws. The Code Case applies to components in which flaws exceeding the acceptance evaluation of the flaws found the components acceptable for continued service according to ASME Section XI, IWB-3132.4.

All of the WWER-440/V-213C and WWER-1000/V-320 RPVs manufactured at SKODA Plzen were subjected to a hydraulic test in the shop with acoustic emission monitoring. The same acoustic monitoring techniques are also applied during the hydraulic tests at the plants in the Czech Republic, Hungary and Slovakia.

5.2.3. Inspection of PWR CRDM penetrations

PWSCC in Alloy 600 CRDM penetrations in European PWRs has stimulated the development of special NDE techniques to detect and size the cracks in these penetrations. The primary challenge associated with the inspection is assessing the examination area. With the head removed and on its stand, the penetrations are physically accessible from the underside of the head, but high radiation fields dictate the use of extensive shielding or remotely operated inspection systems. In addition, direct access to the inside surface of most CRDM penetrations is impeded by the stainless steel thermal sleeve. An air gap of only approximately 3 to 4 mm (0.12 to 0.16 in.) between the sleeve and the nozzle inside surface is available for the access. Removal of the sleeve is time and dose intensive. Therefore, examination of the penetration with the sleeve in place is highly desirable. For penetrations without sleeves, examination is possible, and conventional techniques (visual, penetrant testing, ECT and UT) can be applied.

The current industry practice for examining CRDM penetrations for PWSCC on the inside diameter surface is to use ECT for detection; and UT for sizing the detected indications. Small-diameter ECT probes have been developed by several inspection vendors to inspect penetrations with thermal sleeves. The probes are attached near the tip of the long thin (1.5 mm) blade and are typically spring-loaded to maintain continuous contact with the penetration inside surface. With these "gap scanners," cracks as shallow as 1 mm (0.04 in.) can be detected. In addition, information on the crack length can be obtained more accurately, and small, closely spaced cracks can be resolved.

The primary physical limitation to this approach is that the gap can vary by as much as 30% because the penetrations might have deformed and the sleeves may not be centered [124]. The deformation includes ovalized nozzle cross sections and bending of the penetrations, as discussed in Section 4.5.1. This variation in the gap can prevent full inspection for the peripheral nozzles with thermal sleeves. In addition, boric acid deposits in the gap can obstruct access; however, this obstruction can be removed by rotating the thermal sleeve, which is freely hanging inside the penetration.

Once a crack is detected, accurate crack sizing is important to determine if repair of the penetration is necessary. UT is the primary inspection method for sizing cracks. The most widely accepted UT method for sizing a crack in the CRDM penetrations is the crack-tip diffraction or time-of-flight diffraction method discussed in Section 5.2.1 above. Low-profile UT probes which will fit in the gap between the penetration and thermal shield have been developed for this purpose. Inspection of a penetration without a thermal sleeve is performed using rotating ultrasonic time-of-flight probes. Rotating transducers may contain several sets of dual-element probes to optimize the sizing of different type of cracks, that is, isolated or cluster cracks and deep or shallow cracks. UT is also used to search for cracks on the penetration outside surface. Because of the penetration wall thicknesses, the losses are too large for eddy-current techniques to be an effective means of detecting outside surface cracks.

5.3. RPV MATERIAL SURVEILLANCE PROGRAMMES

5.3.1. Requirements in the USA

Every PWR pressure vessel operating in the western world has an ongoing RPV material radiation surveillance programme. To date, close to 30 surveillance capsules have been removed from their host RPV and tested. The results from these surveillance capsules have been used to develop heatup and cooldown curves and to analyse al potential or postulated accident or transient conditions.

Fracture toughness requirements

On 17 July 1973 the USNRC published Appendix G of 10 CFR Part 50, which delineates requirements for prevention of fracture of the ferntic materials in the primary coolant pressure boundaries of the US NPPs, with emphasis on the RPV [21]. The significant points in Appendix G to 10 CFR Part 50 are:

(a) demonstrate compliance with the minimum fracture toughness requirements of Appendix G, the ferritic materials must be tested in accordance with the ASME Code, Section III NB-2300 Drop weight tests (NB-2321 1) and Charpy V-notch tests (NB-2321 2) are used to define the reference nil-ductility transition temperature RT NDT (NB-2331a) Further, NB-2300 requires that the Charpy V-notch specimens be oriented normal to the main rolling or working direction of the material (NB-2322 2)

(b) The reactor vessel beltlme materials must have a minimum initial upper shelf energy (USE), as determined by Charpy V-notch tests on unirradiated specimens in accordance with NB-2322 2 of the ASME Code of 102 J (75 ft-lbs.) unless it can be demonstrated by data and analysis that lower values of upper shelf fracture energy are adequate.

    10 CFR Part 50 Appendix G also limits the reactor vessel operation to only that service period during which the Charpy impact energy, as measured in the weakest direction, is above 68 J (50 ft-lb) or 0.9 mm (35 mils) lateral expansion In the event that the RT_{NDT} cannot be defined (Charpy impact energy drops below 68 J), the reactor vessel may continue to be operated provided the requirements listed below are satisfied.

    - An essentially complete volumetric examination of the beltlme region of the reactor vessel including 100 % of any weldments shall be made in accordance with the requirements of Section XI of the ASME Code.

- Additional evidence of the changes in the fracture toughness of the beltline materials resulting from neutron radiation shall be obtained from results of supplemental tests, such as measurements of dynamic fracture toughness of the beltline materials.

- A fracture analysis shall be performed that conservatively demonstrates the existence of adequate margins for continued operation.

Paragraph IV.A.1 of Appendix G to 10 CFR 50 states, "Reactor vessel beltline materials must have a Charpy USE of no less than 102 J (75 ft-lb) initially and must maintain a USE throughout the life of the vessel of no less than 68 J (50 ft-lb) unless it is demonstrated in a manner approved by the Director, Office of Nuclear Reactor Regulation, that lower values of USE will provide margins of safety against fracture equivalent to those required by Appendix G of the ASME Code." This allows licensees to submit an USE equivalent margins analyses instead of performing the three tasks cited here.

If the results of the above tasks do not indicate the existence of an adequate safety margin, thermal annealing of the reactor vessel beltline region is required to recover the reactor vessel beltline material fracture toughness properties or the plant must be shutdown.

(c) calculated stress intensity factor (K_I) shall be lower than the reference stress intensity factors (K_{IR}) by the margins specified in Appendix G to the ASME Code. However, if there is no fuel in the reactor during the initial pre-operational hydrostatic pressure tests, the safety factor on K_{IM} can be reduced from 1.5 to 1.0.

10 CFR Part 50 Appendix H, reactor vessel material surveillance programme

With the publication of Appendix G, "Fracture Toughness Requirements", the US NRC also published Appendix H, a set of rules for the reactor vessel material surveillance programmes [22]. The significant points given in Appendix H to 10 CFR Part 50 are:

(a) That part of the surveillance programme conducted with the first capsule withdrawal must meet the requirements of ASTM E l85 that is current on the issue date of the ASME Code to which the reactor vessel was purchased.

(b) Surveillance specimen capsules must be located near the inside vessel wall in the beltline region so that the specimen radiation history duplicates to the extent practicable within the physical constraints of the system, the neutron spectrum, temperature history and maximum neutron fluence experienced by the reactor vessel inner wall.

(c) Surveillance capsule withdrawal schedule must be submitted to and be approved by the NRC prior to implementation.

(d) Each surveillance capsule withdrawal and the test results must be the subject of a summary report submitted to the NRC.

Regulatory Guide 1 99

Appendix G, "Fracture Toughness Requirements" and Appendix H, "Reactor Vessel Material Surveillance Programme Requirements", necessitate the calculation of changes throughout the service life in fracture toughness of reactor vessel materials caused by neutron radiation

USNRC Regulatory Guide 1.99 [126, 127] describes general procedures acceptable to the USNRC staff for calculating the effects of neutron radiation of the low-alloy steels currently used for light-water-cooled reactor vessels in the western world. As discussed in more detail in Section 6, the pertinent rules or guidelines are:

(a) The ART for each material in the beltline is given by the following expression

$$ART = \text{Initial } RT_{NDT} + \Delta RT_{NDT} + \text{Margin} \quad (40)$$

(b) ΔRT_{NDT} is the mean value of the adjustment in reference temperature caused by radiation and is calculated as follows

$$\Delta RT_{NDT} = (CF)\ f^{(0.28-0.10 \log f)} \quad (41)$$

where CF is a chemical factor which is a function of the copper and nickel content, f is the fluence in 10^{23} n/m^2 and ΔRT_{NDT} has unit s of Fahrenheit degrees. Regulatory Guide 1.99 Revision 2 presents the CF in tabular form for welds and base metal (plates and forgings). If more than two credible surveillance capsule data are available, the CF should be calculated by curve fitting. The neutron fluence f, is the fluence at any depth in the vessel wall. The fluence factor, $f^{(0.28-0.10 \log f)}$ is determined by calculation or from a figure presented in the regulatory guide.

Regulatory Guide 1.99 Revision 0 and 1 [125, 126] considered the detrimental effect of copper and phosphorus. R.G. 1.99 Revision 2 introduced the CF and replaced the element phosphorus with nickel.

Other regulatory guides

Regulatory Guide 1.43 [128] provides guidance to assure that stainless steel protection cladding complies with ASME Section III and XI requirements to prevent underclad cracking. The presence of intergranular cracking in the base metal under the cladding is a possibility in a RPV.

Regulatory Guide 1.65 [129] provides guidance on vessel closure bolting materials and inspections PWR plants have closure bolts in compliance with ASME Section III and are inspected according to ASME Section XI. All studs are volumetrically examined and receive a surface examination during each 10 year inspection interval. Regulatory Guide 1.150 [130] provides guidance on ultrasonic test procedures which supplement those provided in ASME Section XI. PWR procedures for inspection of vessels comply with this guidance.

5.3.2. Requirements in Germany

According to the stipulations in the Code, the radiation embrittlement can be neglected when the neutron fluences are lower than 10^{21} n/m^2 (E >1 MeV). Since the maximum allowed RPV fast neutron fluence in Germany is limited to 1.1×10^{23}n/m^2 (E > 1 MeV) and KTA 3203 is valid for this fluence or lower values only, the number of radiation sets and the withdrawal schedule (relative to the RPV fluence) are fixed (two sets covering 50% and 100%, respectively, of the RPV design life fluence). KTA 3203 allows higher lead factors (>3) on the radiation capsules. This ensures that the results for the first set of irradiated specimens withdrawn at approximately 50% of the fluence predetermined for the vessel at end-of-life are available prior to the first in-service pressure test of the RPV. The surveillance specimens are located between the core barrel and RPV along the entire core length as shown in Fig. 41. Each set has to contain 12 Charpy V-notch specimens and three tensile specimens from both base metals (the upper and lower ring forgings), and the weld metal and 12 Charpy V-notch specimens from one heat-affected zone. However, for end-of-life- fluences between $1.1 \times 10^{21} n/m^2$ and $1.1 \times 10^{22} n/m^2$ (E>1 MeV), it is sufficient to implement 12 Charpy V - notch specimens from one base metal and the weld metal set each. The specimen configuration and the quantities of specimens are shown in Fig. 42. A sketch of the surveillance programme capsules is shown in Fig. 43.

The differences between the surveillance programmes required by ASTM and KTA such as the number of the specimen sets and the removal schedule, reserve material (an approximately 1.5 m long section of the fabricated test coupon) instead of optional specimens in the standard capsules, and the magnitude of the lead factor, are justified by the fact that the predicted transition temperature shift does not exceed 40K, and a pre-irradiation nil-ductility transition temperature of <-12°C is required for the steels used in the beltline region of the RPV.

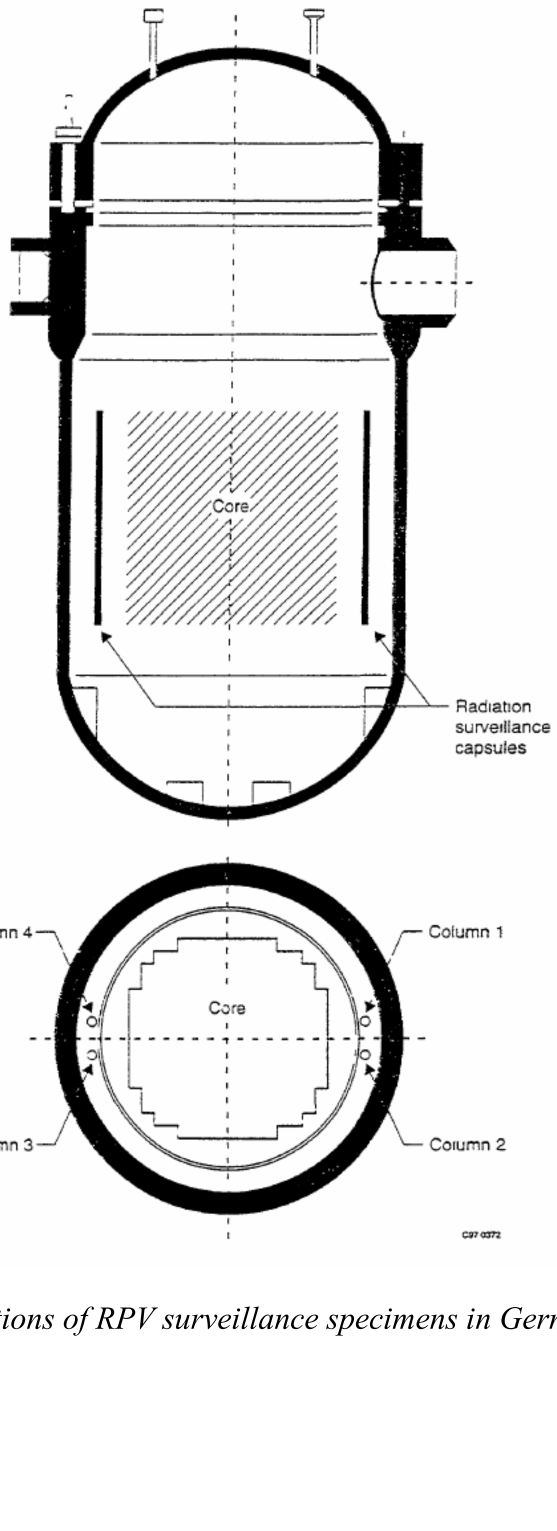
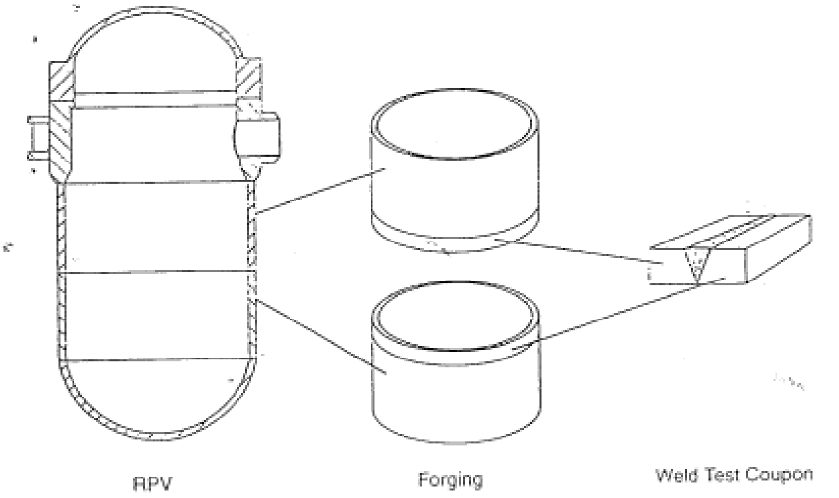
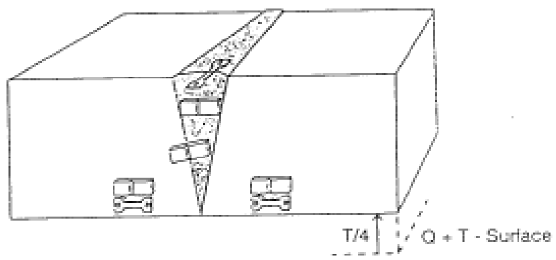


Fig.41. Locations of RPV surveillance specimens in Germany.





Quantities of Specimens

Specimen Set No.	Charpy V-Notch Impact Specimens				Tensile Specimens			Withdrawal Schedule
	Base I	Base II	Weld	HAZ	Base I	Base II	Weld	
1	12	12	12	12	3	3	3	Unirradiated
2	12	12	12	12	3	3	3	50% EOL
3	12	12	12	12	3	3	3	100% EOL

Fig. 42. Configuration, types, and quantities of specimens used in the PRV surveillance programme in Germany).

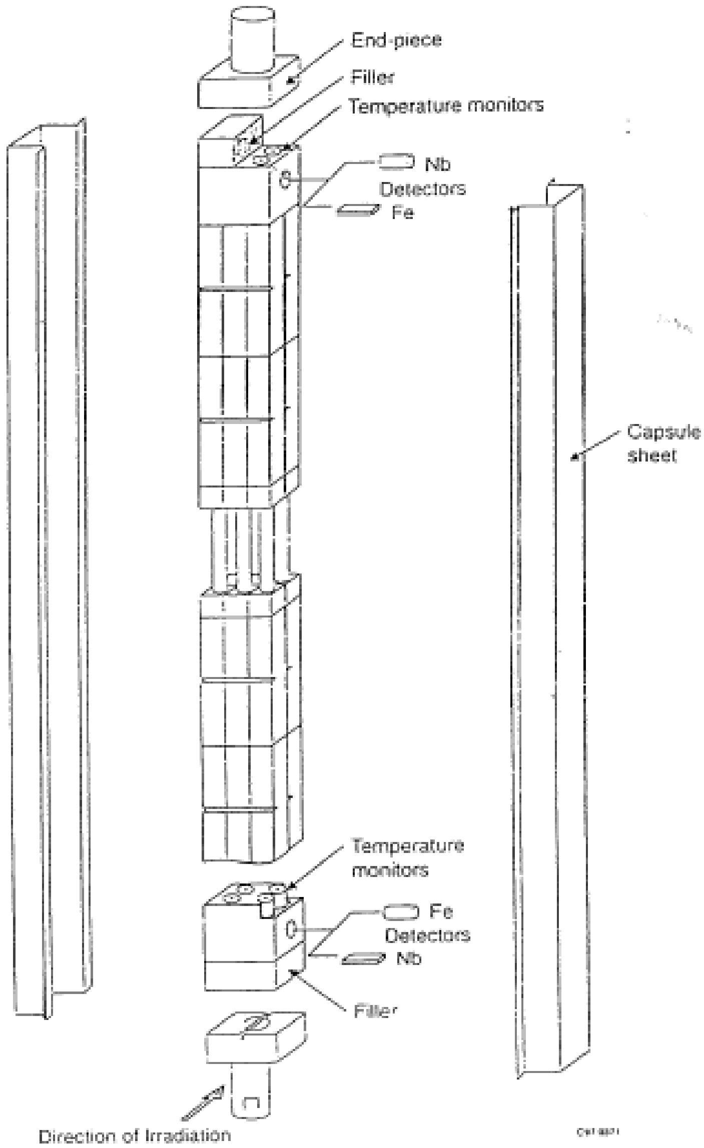


Fig. 43. Irradiation capusles used in the RPV surveillance programme in Germany.

5.3.3. Requirements in France

The material surveillance programme specified in RSE-M [28] is similar to the U.S. programme discussed above. Capsules are regularly removed from the plants and the specimens subjected to Charpy testing. The measured shifts in the Charpy nil-ductility transition temperatures are compared with the predicted values (Equation 27 in Subsection 6.1.3). As discussed in Section 6.1, the anticipation factor is less than 3. However, all the design end-of-life (40 years) ΔRT_{NDT} values are practically available for 3-loop plants and 4-loop 1300MWe plants. The results are compared with the predictions on more than 45 plants to confirm the shift formula used for based metal and slightly change the formulae for welds.

Mainly Charpy-V specimens are used for this comparison. The complementary pre-cracked specimen are now under analysis to look at possible uses for new toughness evaluation, such as "master curve" or similar methods.

In the same time, EDF started a new programme of capsule re-insertion in order to cover a possible extension of the initial design life to 60 years and to ensure a surveillance programme covering all the life of each vessel.

5.3.4. WWER material surveillance programme requirements

The requirements for the WWER material surveillance programmes are given in Ref. [32] and updated in Ref. [33], but they were applied only to the WWER-440/V-213 and WWER-1000 NPPs. The oldest design type, the WWER-440/V-230, was not supplied with a material surveillance programme, even though, as was shown later, the materials used for these RPVs are more susceptible to radiation embrittlement than the materials used for the WWER-440/V-213 and WWER-1000 pressure vessels.

According to [131] WWER surveillance specimens must be removed from the RPV and tested at least six times during the pressure vessel design life. The first batch of specimens of the WWER-440/V-213 RPVs must be removed and tested after 1-year of reactor operation. The next three batches of specimens must be removed and tested within the first five years of operation. This schedule is based on the assumption that the neutron fluence on the RPV wall will be greater than 10^{22}n/m^2 (E $\geq$ 0.5 MeV) but less than 10^{23}n/m^2 during the first year of operation. A surveillance programme is not required when the end-of-life RPV fluence is less than 10^{22}n/m^2 (E $\geq$ 0.5 MeV) and at the same time the RPV operating temperature is greater than 250°C.

The following material properties are measured after each removal:

- tensile properties (yield and ultimate strength and elongation)
- ductile-to-brittle transition temperature
- fracture toughness (or crack opening displacement).

The maximum allowable ductile-brittle transition temperature and the allowable fracture toughness curves (K_{IC} versus reference temperature) for various materials and operating conditions were discussed in Section 3.4.4. There is no lower limit on the USE specified in the WWER codes because experiments have shown that the USE will remain sufficiently high during the expected RPV lifetimes. In the VERLIFE procedure [42] there is an additional requirement for the Charpy upper shelf energy to be higher than 70 J.

The WWER-440/V-213 pressure vessel radiation damage Standard Surveillance Programmes are characterized by the following features:

- Individual specimens were manufactured from either base metal, weld metal, or heat affected zone material. The base metal specimens were removed from the core beltline ring as it was cut to size during fabrication of the vessel. The weld metal and heat affected zone material were removed from welding coupons for welding joint No. 0.1.4 (the circumferential weld in the lower part of the beltline region).
- Tensile, Charpy V-notch and pre-cracked Charpy (for static fracture toughness testing – called COD type) type specimens were made from each of the three materials. A complete set of specimens includes 18 tensile specimens (6 of each of the 3 materials),

36 Charpy V-notch specimens (12 of each of the three materials), and 36 pre-cracked Charpy specimens. There are a total of 90 surveillance specimens per set.

- The specimens are put into stainless steel containers, six tensile specimens or two Charpy-type in one container as shown in Fig. 44.
- The containers are connected into chains, each chain consisting of 20 or 19 containers which are then placed adjacent to the active core region.
- Two chains hold one complete set of 90 specimens and contain all the aforementioned specimen types and materials; the two chains are located symmetrically very close to a corner of the hexagon shaped active core and are removed at the same time.
- Six sets of specimens are located in each reactor, one set (two chains) at each corner of the hexagon core.
- The planned withdrawal interval of the individual sets is usually: 1, 3, 5 and 10 years (or 1, 2, 3, 5 and 10 years).
- Some of the containers are supplied with neutron fluence monitors and some are supplied with diamond powder temperature monitors, but the diamond temperature monitors have been found to be unreliable. X-ray diffraction techniques were used to decode the diamond powder information.
- Additionally, in several RPVs (Loviisa, Bohunice and Kola) special measurements of surveillance specimens temperature were performed using thermocouples inserted in special surveillance chains. Results of these measurements showed that real specimen temperature is not higher than 5 to 8 °C over the inlet water temperature.
- The containers are located on the outer wall of the active core barrel, where a high lead factor, between 6 and 18, is obtained – see Fig.44. (The lead factor is the ratio of the neutron flux with energies > 0.5 MeV in the test specimens to the maximum neutron flux on the inner surface of vessel wall.)
- The length of the chain of 20 containers located in the active core region is about 2.4 m, a distance corresponding to a factor of about 10 between the maximum and minimum neutron flux. However, the containers with the Charpy V-notch specimens are all located in the centre (maximum flux) region.

In addition to the surveillance specimens for monitoring the radiation damage in the beltline materials, the WWER surveillance programmes include specimens for monitoring the thermal ageing damage in the pressure vessel materials. Two complete sets of 90 surveillance specimens (39 containers) are located well above the active core (virtually no damage from neutron bombardment) in front of the upper (outlet) nozzle ring. These sets are usually removed and tested after 5 and 10 (or 20) years of operation.

Since the WWER-440/V-213 Charpy specimens for monitoring radiation damage are located on the outer wall of the core barrel in the axial centre region of the core with lead factors ranging from 12 (base metal) to 18 (weld metal), the original radiation damage surveillance programmes are now essentially finished in all the WWER-440/V-213 plants. (Irradiation for times longer than 5 years has no practical meaning as it represents more than 60 to 90 years of operation, a time much longer than the RPV design life.) At least four complete specimen sets have been removed and tested from each of the 18 WWER-440/V-213 reactors (six in Russia, two in Finland, four in Hungary, two and partially two others in the Slovak Republic and four in the Czech Republic).

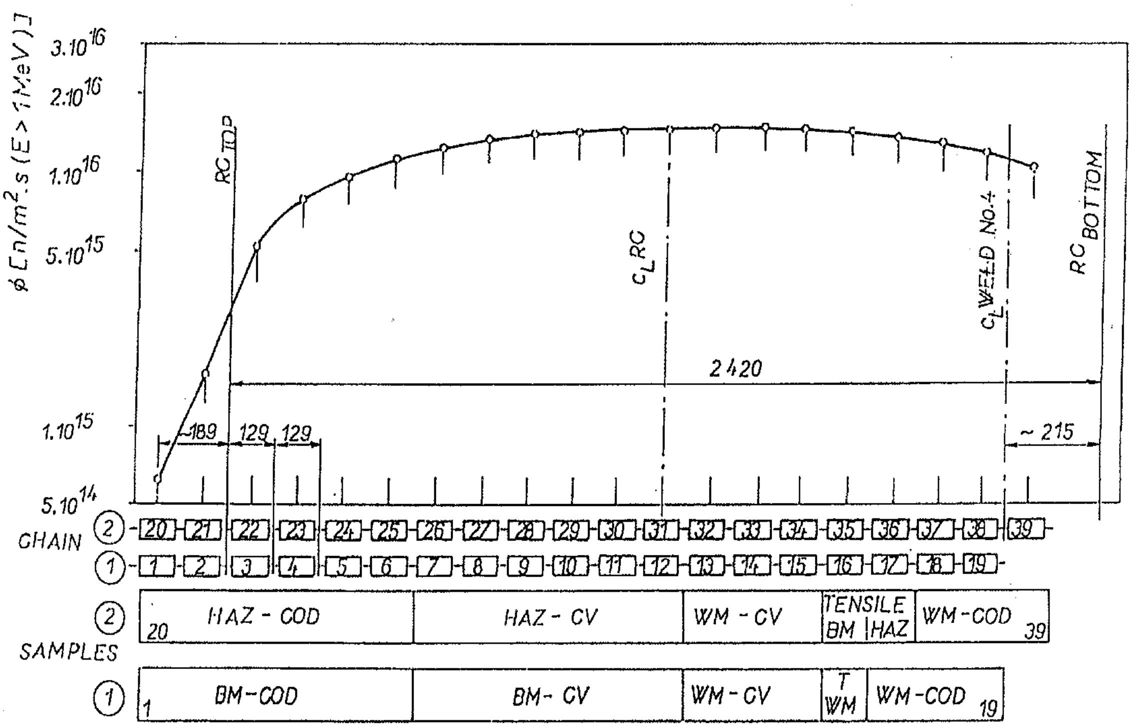


Fig. 44. Typical distribution of neutron flux along irradiation chain with containers.

However, the radiation damage measured in these specimens may not accurately predict the damage expected in the WWER-440 pressure vessels because the neutron flux in the specimens was so much higher than the highest flux in the vessel walls (12 to 18 times). These problems have lead some of the WWER regulatory bodies to require supplementary surveillance programmes designed to monitor the RPV material behavior and the neutron flux and fluence throughout the plant life. In the Paks plant (Hungary), quasi-archive and reference material specimens are located in the usual locations, but removed and replaced every four years. Thus this programme serves to check the stability of irradiation conditions in the RPV beltline during reactor operation without a standard surveillance programme. In the Bohunice (Slovak Republic) and Dukovany plants (Czech Republic), in Supplementary Surveillance Programmes, archive material specimens are located in relatively low flux positions on the outside of the core barrel near the top and bottom of the core (with lead factors below five) and will be withdrawn and tested at periodic intervals during the remaining plant life. Additionally in Dukovany, specimens from cladding materials are irradiated, too. Some of irradiated specimens within the Standard Surveillance Programme were annealed and put again into this Supplementary Surveillance Programme for determination of re-embrittlement rate after annealing. Specimens are located in modified containers and reconstitution techniques of irradiated specimens is used – each container contains twelve inserts for either Charpy or pre-cracked Charpy type specimens with dimensions 10 × 10 × 14 mm – see Fig. 45. Each of these containers is supplied by three sets of neutron fluence monitors – activation foils and fission monitors for determination of the absolute value of neutron fluence in the container and with wires (ring type and axial one) for relative determination of fluence in each of specimens.

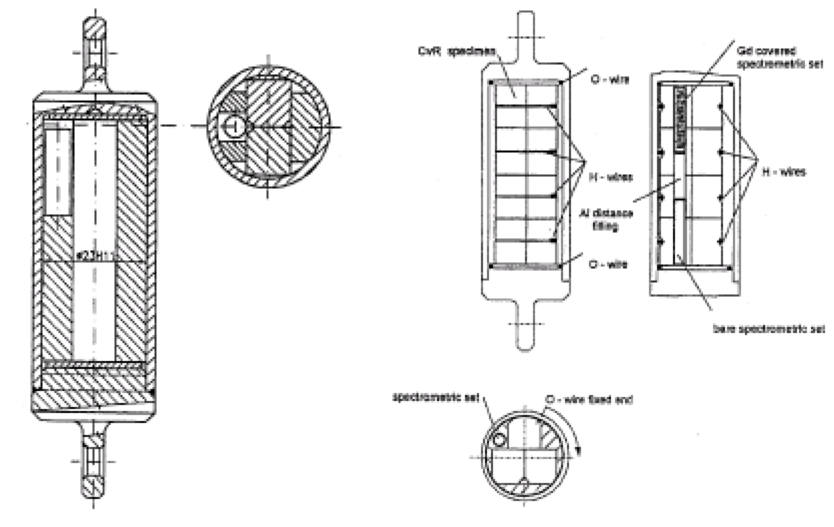


Fig. 1 Scheme of a container from the Standard Surveillance Program

Fig. 2 Scheme of a container from the Supplementary Surveillance Program

Fig. 45. Scheme of a container from the Standard Surveillance Programme (left) and the Supplementary Surveillance Programme (right).

RPVs in Mochovce NPP (Slovak Republic) are supplied, from the beginning of operation, by Modified Surveillance Programme that is similar to Supplementary Surveillance Programme in Bohunice (Slovak Republic) using archive material from the original, not used Standard Surveillance Programme.

The number and type of surveillance specimens placed in the Standard Surveillance Programmes of WWER-1000 plants is similar to the number and type of specimens used in the WWER-440/V-213 plants, but specimens for low-cycle fatigue are also included. Both neutron embrittlement and thermal ageing specimens are placed in the WWER-1000 pressure vessels, and the same type of containers (shown in Fig. 46) are used. However, the locations in which the WWER-1000 radiation embrittlement surveillance specimens are placed are quite different than the locations discussed above for the WWER-440 neutron radiation specimens. The axial and radial locations of the WWER-1000 surveillance assemblies are shown in Fig. 47. The locations labeled 1M, 2M, etc. are where the thermal ageing sets are placed, the locations labeled 1L, 2L, etc. are where the neutron radiation embrittlement specimens are placed. The surveillance assemblies hold five containers stacked either one or two high (radiation embrittlement specimens) or five high (thermal ageing specimens) as shown in Fig. 48.

Since the neutron radiation embrittlement surveillance specimens are located above the core, they are in a relatively steep flux gradient in radial, circumferential as well as axial directions as shown in Fig. 49. Also, the mean flux level at most their positions is approximately close or lower than that on the RPV wall (a lead factor of less than 1.0) but the energy spectrum is different. Additionally, neutron fluence monitors are located only in some of containers which, together with a low knowledge of neutron energy spectra in specimens locations, create a large uncertainty in neutron fluence determination in individual specimens. Thus, additional methods, like gamma monitoring of each specimen together with large calculations and modelling have to be used for precision and determination of neutron fluence in each specimen. Moreover, existing flux gradients result in the fact that it is difficult to find a sufficiently large set (12) of specimens for one test curve. In addition, the containers are located in outlet water and therefore the specimens are irradiated at least by 10 °C above the vessel wall temperature. (The specimen temperature is also monitored with diamond powder, but, as mentioned above, this method has been found unsuitable.) Due to the high temperatures and atypical flux, the use of these surveillance results for vessel radiation embrittlement assessment must be very carefully analysed before their use in RPV integrity and lifetime evaluation. The Russian WWER-1000 surveillance programme has been modified. Changes in the design of container and the container location inside the container assembly have been made under the surveillance programme modification: the surveillance programme modification improves the reliability of radiation embrittlement monitoring. The surveillance programme is representative in terms of irradiation temperature (irradiation temperature does not exceed 300°C). 3-D neutron fluence calculations combined with g - activity measurements provide the correct assessment of neutron fluence for surveillance specimens. Using the reconstitution technique provides the possibility to obtain the required number of specimens irradiated at the same conditions and the possibility of manufacturing new specimens for a direct fracture toughness assessment. The time schedule of specimen withdrawal and testing was also modified. Recently, a modified surveillance programme has been started in Kozloduy NPP (Bulgaria). One assembly with specimens from archive materials was put into a similar position as standard ones but in a lower position (by about 300 mm) to obtain a higher neutron flux. This container will serve for comparison of results from standard and this modified irradiation positions.

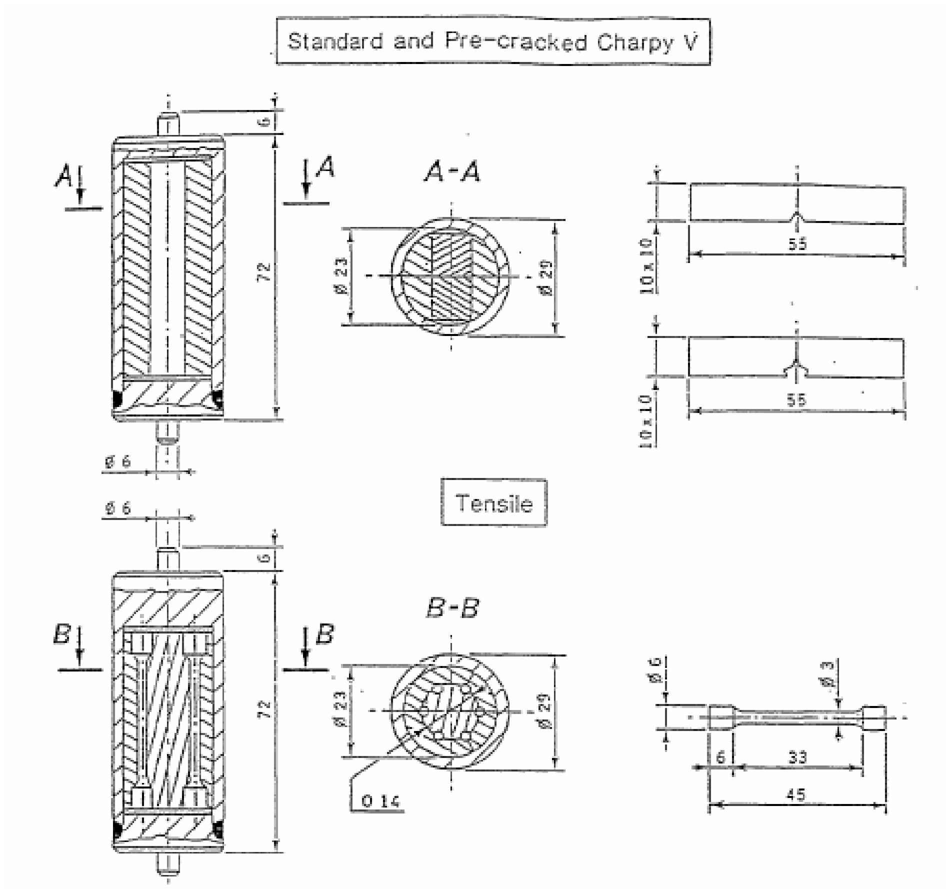


Fig. 46. Charpy and tensile material surveillance specimen containers used in the WWER-440/V-213 and WWER-1000 pressure vessels.

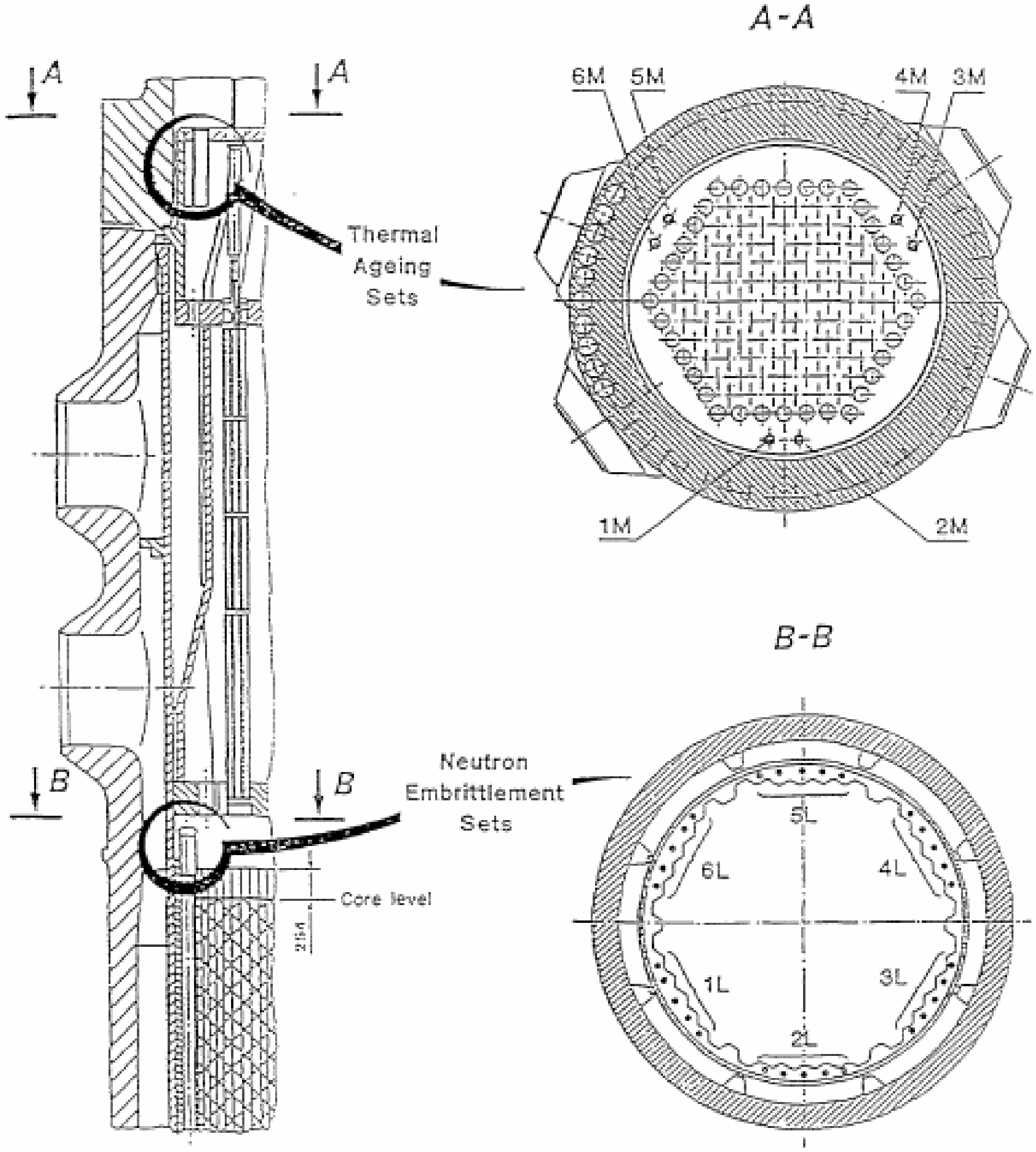


Fig. 47. Locations of the WWER-l000 pressure vessel material surveillance specimen assemblies. The thermal ageing specimens are at IM, 2M, etc. and the neutron radiation embrittlement specimens are at 1L, 21, etc.

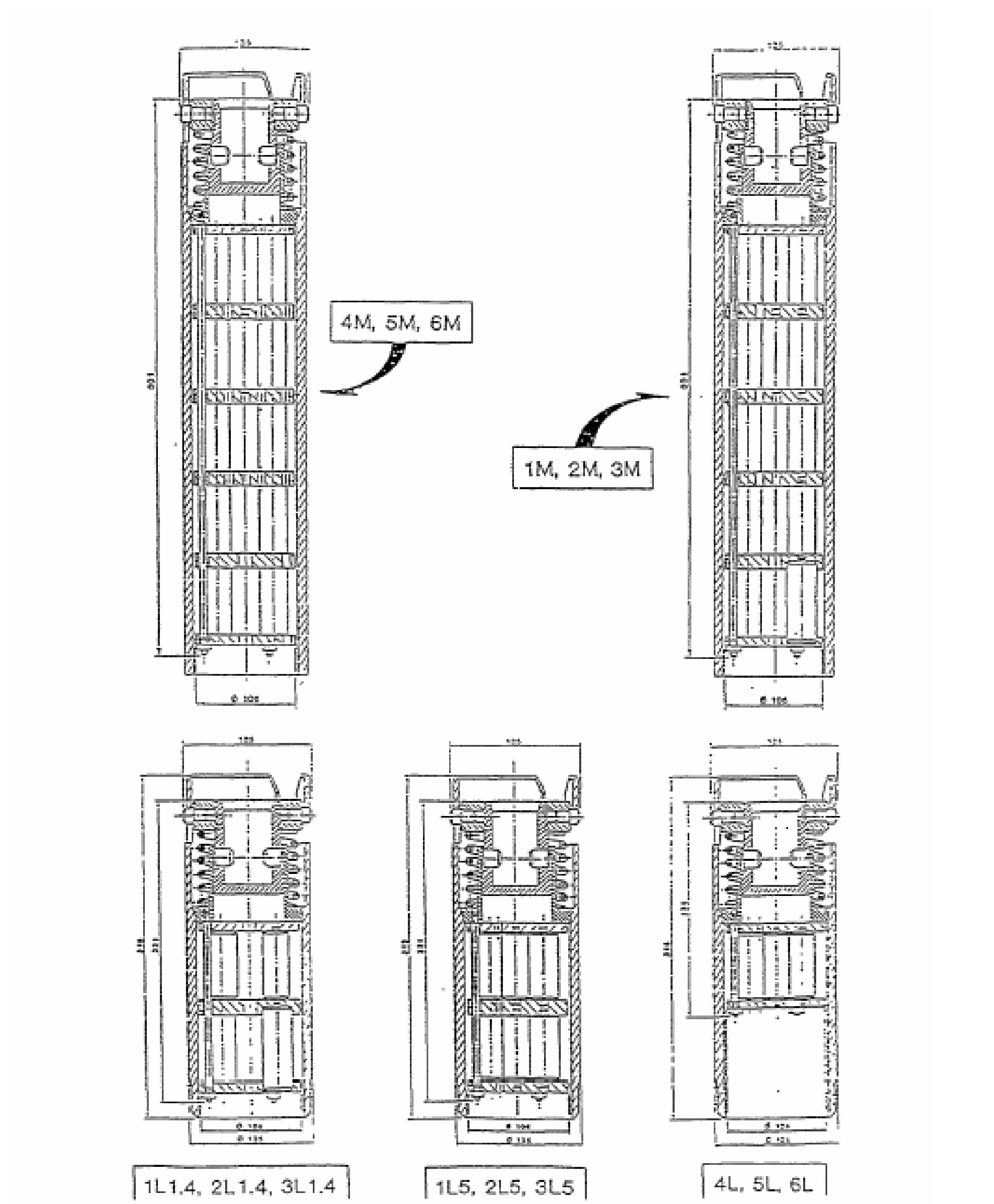


Fig. 48. WWER-1000 pressure vessel material surveillance specimen assemblies The assemblies hold five of the containers shown in Fig. 36 at each axial elevation.

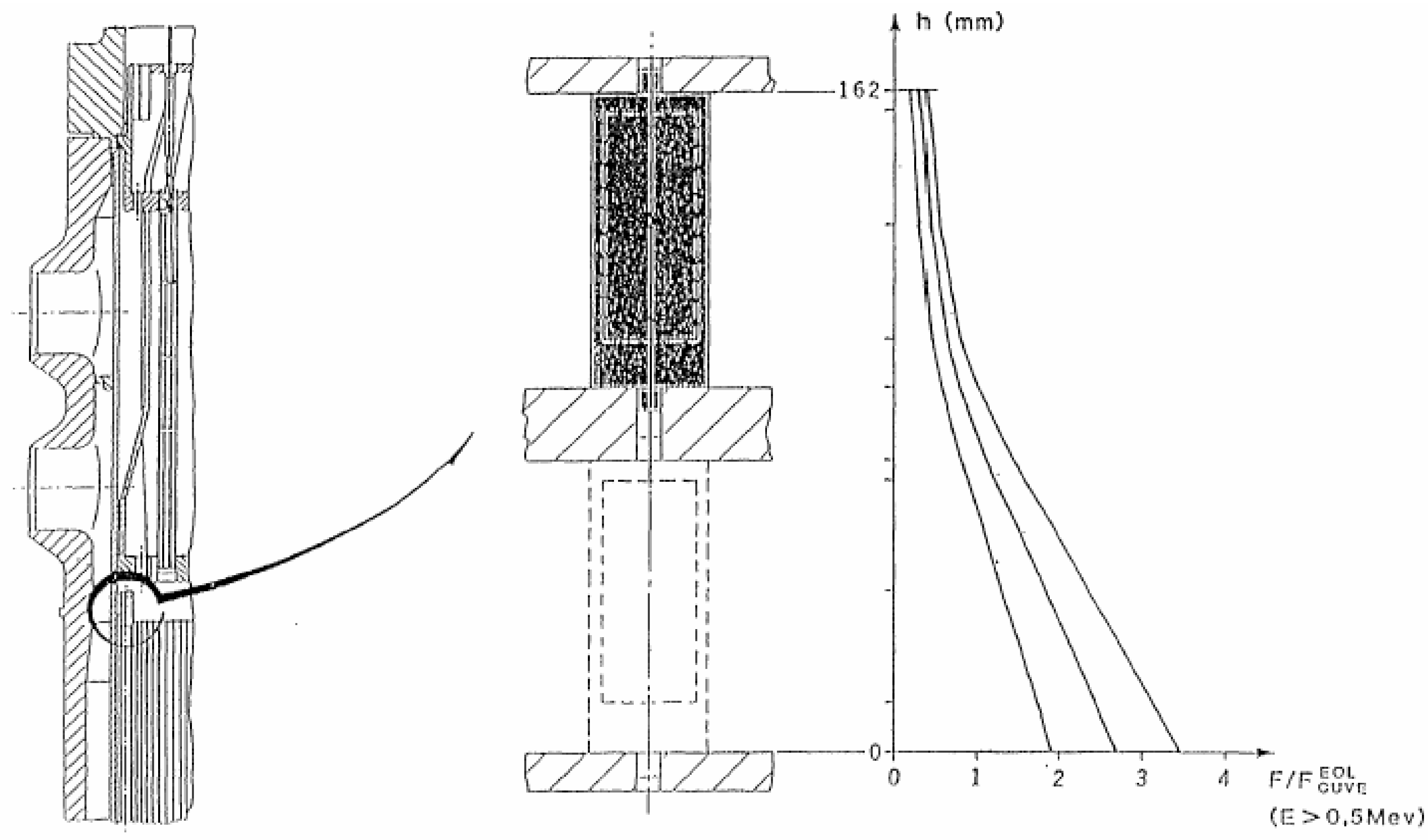


Fig. 49. A typical neutron flux distribution at the locations where the radiation damage surveillance specimens are placed in the WWER-1000 pressure, vessels.

The material surveillance programmes for the three WWER-1000 pressure vessels built in the Czech Republic (two are in operation in Temelin NPP) were modified as follows. (Fig. 50) The specimen specification was expanded to include static fracture specimens of the CT-0.5 type (as defined in ASTM E-399) as well as Charpy pre-cracked specimens for fracture toughness testing. The specimens were grouped in flat boxes rather than the round containers shown in Fig. 46 (maximum two layers of Charpy-type specimens) and dynamic fracture toughness testing. The specimens were grouped in flat boxes rather than the round containers shown in Fig. 46 (maximum two layers of Charpy-type specimens) and located symmetrically in the active core region near the inner reactor vessel wall, at a small distance below the beltline center line – see Fig. 51. This resulted in a lead factor between 1.5 and 2. The planned withdrawal time is 2, 6, 10, 18, while two other specimen sets are used to determine possible annealing efficiencies as well as to evaluate further re-embrittlement after an annealing. Neutron fluence monitoring is assured by activation as well as by fission monitors; wire monitors are also included to monitor the neutron flux field with the whole container to be able to determine neutron fluence in each of specimens. Temperature is monitored by the use of two sets of wire melt monitors- in accordance with ASTM and DIN standards.

Fig. 50. Picture of an opened container with specimens (right) and neutron wire monitors (left) for WWER-1000 RPV Modified Surveillance Programme

A new WWER-1000 surveillance programme was developed in Russia for new WWERs. Main features of this programme are the following:

- Surveillance specimens are compiled into flat containers;
- Irradiated container assemblies are located at the inner surface of RPV;
- Leading factor does not exceed 2;
- Non-uniformity of neutron flax within container does not exceed 20%;
- Specimens for direct fracture toughness Determination are implemented (CT 0.5 specimens).

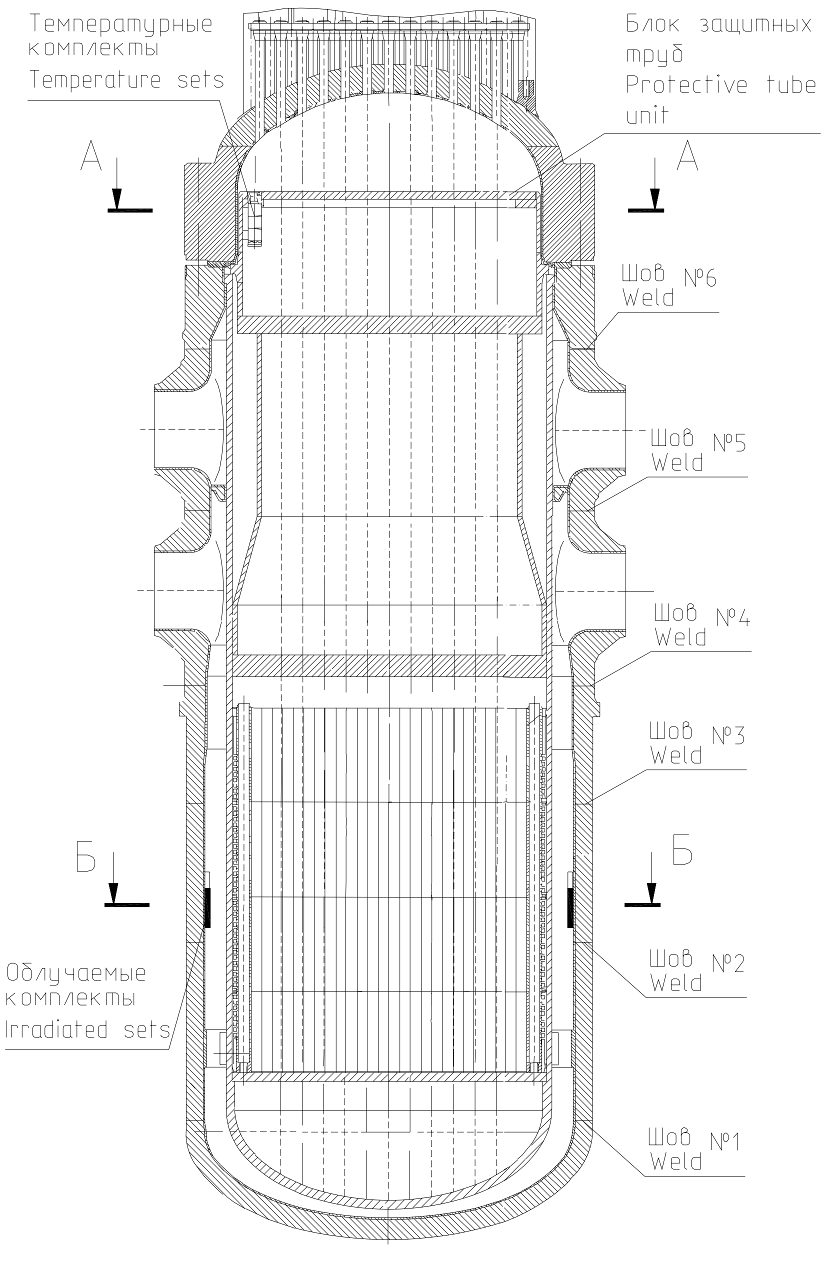


Fig. 51. New WWER surveillance programme.

Destructive examinations

For some WWER-440 type 230 reactors, the initial ductile to brittle transition temperature T_{k0}, as well as the exact chemical composition (phosphorus and copper content) in the weld metal was not measured. Thus, removing material (boat samples) from the pressure vessel is a potential way to obtain these data. However, some problems are connected with such a procedure, mainly:

- it cannot be used for sampling a stainless steel clad vessel from the inside,
- the weld metal is usually covered by a protective surface layer of low-carbon steel electrode material with a thickness up to 5 mm; the surface part of the sample, therefore, must not represent the real weld metal,
- sample dimensions are limited by the lowest allowable wall thickness; therefore, specimens for mechanical testing must be of a subsize type, thus necessitating the development of correlations between standard and subsize Charpy type specimens.
- sampling of the outer surface of the RPV is difficult because of the approximately 40 mm gap between the vessel and the water tank that provides biological shielding.

The following activities were performed in Russia for WWER-440/230 RPVs irradiation embrittlement monitoring:

- taking boat samples (templates) and investigating materials with the use of sub-size specimens ;
- sampling and testing of trepans from Novovoronezh 2 (correlation between sub-size and standard Charpy results
- template irradiation in surveillance channels of WWER-440/213;
- development of new model of re-irradiation embrittlement.

The following problems were solved by using templates:

- Assessment of initial ductile-to-brittle transition temperature T_{K0} with the use of special heat treatment (initial temperature T_{K0} was not determined by the manufacturer);
- Determination of weld metal chemical composition (chemical composition was not determined by the manufacturer);
- Experimental determination of neutron fluence (validation of calculations);
- Determination of ductile-to-brittle transition temperature T_K (after annealing, current value);
- Validation of annealing procedure ;
- Improvement of knowledge on re-embrittlement after annealing;
- Development of a new model of re-irradiation embrittlement (lateral shift approach).

Scheme of templates cut out from Novovoronezh-4 RPV is presented on Fig. 52.

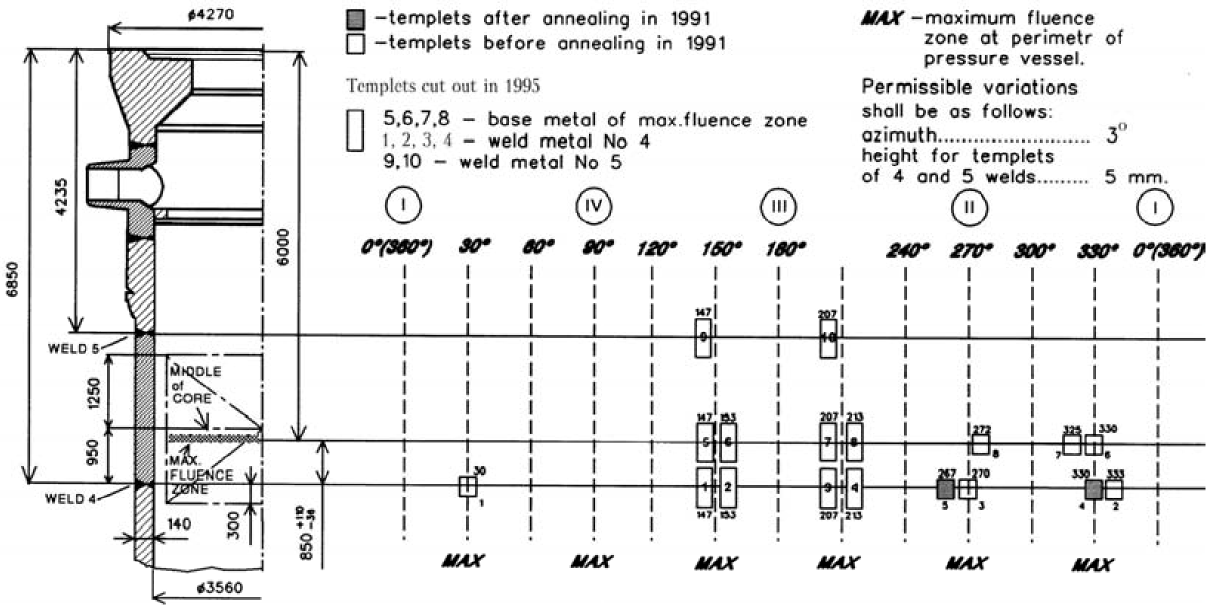


Fig. 52. Scheme of templets cut out from Novovoronezh-4 RPV.

The phosphorus contamination in the weld metal as well as the mechanical properties of the weld metal vary across the RPV wall thickness. (The phosphorus concentration is somewhat higher near the surface due to geometry and solidification effects.) Therefore, it is difficult to decide whether the surface samples provide a conservative or non-conservative estimate of the material properties across the vessel wall thickness. Nevertheless, boat samples have been removed from unclad vessel inner surfaces, namely in Kozloduy Units 1 and 2, Novovoronezh Units 3 and 4, and Greifswald Units 1 and 2. The chemistry of the templates taken from the inside surfaces of the Novovoronezh Unit 3 and Kozloduy Unit 2 RPVs did not show any noticeable variation of chemical composition in the depth direction of the weld. Also, scrape samples were taken from the Kola Units 1 and 2 and Kozloduy Unit 1 RPV inside surfaces for chemical analysis. The measurements on these samples have been reported to be reliable.

Scrapes for chemical analysis were also taken from the outside of the clad vessels at Bohunice Units 1 and 2. However, due to a protective layer of undefined thickness, even a second and third sampling in the same position might contain a small amount of the low carbon steel cover layer. Recently, small boat samples were also removed from the Bohunice outer surfaces. No samples were taken from other clad vessels of this type.

Sampling from the inside of unclad vessels is a very promising method for measuring the residual transition temperature shift (ΔRT_{NDT}) after annealing. Even though subsize specimens must be used, the effectiveness of the annealing can be evaluated with a high degree of reliability using correlations between subsized and normal specimens. This method has been used in several RPVs after annealing - e.g. Novovoronezh Units 3 and 4, Kozloduy Unit 2, and Greifswald Units 1 and 2. The method has also been used for assessment of the re-embrittlement rate in service, for example, at Kozolduy Unit 1.

5.3.5 IAEA RPV surveillance database

The IAEA International Working Group on Lifetime Management of NPPs started the creation of a worldwide database which would store the results from the RPV surveillance programmes. The primary purpose shall be to collect all accessible data from these programmes and specimens and then perform a more general analysis of these results than can be performed using national (or utility) databases, only. The RPV fabrication techniques (including different source of metallic charges etc.) are slightly different in some countries, even though the manufacturing is performed according to the same standards and general requirements. As a result, vessels from each of the manufacturers represent a family, which can be slightly different from the others. Thus, results from one database may not be fully applicable to RPVs from other manufacturers. Creation of this database started in 1996 under the coordination of the aforementioned International Working Group on Lifetime Management of Nuclear Power Plants. Up to now, results from surveillance programmes from 10 countries are included in this database.

The first use of this database is included in the IAEA Co-ordinated Research Project (CRP) on “Evaluation of Radiation Damage of WWER RPV Using the IAEA Database on RPV Materials” with the main goal to preparation of “Guidelines for Prediction of Radiation Embrittlement of Operating WWER-440 Reactor Pressure Vessels”. [132]

Additionally, this database has also a second part that collects data from all IAEA Co-ordinated Research Projects in the field of radiation damage in RPV materials, mainly:

- CRP-1 on "Irradiation Embrittlement of Reactor Pressure Vessel Steels" [133]
- CRP-2 on "Analysis of the Behaviour of Advanced Reactor Pressure Vessel Steels under Neutron Irradiation" [134]
- CRP-3 on “Optimising Reactor Pressure Vessel Surveillance Programmes and their Analysis” [135]
- CRP-4 on "Assuring Structural Integrity of Reactor Pressure Vessels" [136]
- CRP-5 on “Surveillance Programmes Results Application to RPV Integrity Assessment” [137, 138]
- CRP on “Nickel Effects in Radiation Embrittlement of RPV Materials” [139]
- Round-robin Exercise (RRE) on “WWER-440 RPV Weld Metal Irradiation Embrittlement, Annealing and Re-embrittlement”

Access to both parts of databases is controlled by agreement with the IAEA.

5.3.6 Requirements in Japan

Two relevant industrial technical standards, JEAC 4201-2000 and JEAC 4206-2000 were published in 1992 by the Japan Electric Association [140, 45].

JEAC 4201-2000 prescribes the reactor vessel material surveillance programme which is based on NRC 10 CFR Part 50 Appendix G (1995) and Appendix H (1995), and ASTM E185-94; it also includes the Japanese embrittlement predictive equation which is mentioned in Section 6.1.5.

JEAC 4206-2000 provides experimental methods to confirm the integrity of nuclear power plant components against non-ductile failure. These methods include the linear elastic fracture mechanics analysis method and the PTS evaluation method. JEAC4206 is based on NRC 10

CFR Part 50 Appendix G (1995) and Appendix H (1995), and the ASME Boiler and Pressure Vessel Code Section III.

5.4. TRANSIENT AND FATIGUE CYCLE MONITORING

5.4.1. Requirements in the USA

As discussed in Section 4.4, the only RPV components likely to experience significant fatigue damage are the RPV studs. However, fatigue can become a significant degradation mechanism if indications or flaws are detected during the RPV ISI or if consideration is given to extending the operating life of the plant. In the former case, fatigue crack growth becomes important in the assessment and management of the ageing of PWR RPVs. In the latter case, fatigue cycles and loading to address Miner's Rule becomes important. In either case, transient and fatigue cycle monitoring is required..

5.4.2. Requirements in Germany

All German PWRs in operation are equipped with a fatigue monitoring system. On the basis of a plant specific weak point analysis of the NSSS, parameters to be monitored are defined and reported in a fatigue manual. Special emphasis is given to thermal loads such as thermal shocks, thermal stratification, and turbulent mixing phenomena which may occur very locally. These transients have been measured by means of special purpose instrumentation. (Thermocouples were installed on selected cross sections of interest.) In addition, global parameters such as internal pressure, fluid temperature, mass flow, water level, etc., have been measured via existing instrumentation and the data combined with the local parameters.

KTA 3201.4 contains requirements for recurring inspections. Parameters, which affect the fatigue life must be monitored and the resulting fatigue compared to the design margins. Sophisticated software packages are available to recognize fatigue relevant loadings and to perform automatic fatigue evaluations. Thus the software tools not only satisfy the Code requirements but establish a data base for a reliable evaluation of the fatigue status, end of life predictions, or even life extension evaluations. Also, the German Reactor Safety Commission recommends that the fatigue status of every plant be updated after every 10 years of plant operation. The fatigue status and forecast have to be reported within the safety status report to be presented by the utility.

With respect to the RPV this means that the parameters to be monitored include: internal pressure, inlet and outlet loop temperature, and pressure vessel head temperatures at various locations on the outside surface. The reactor power is also monitored. In order to define the actual service condition several other parameters are made available. Following this way, the RPV nozzles, the flange and bolt connections and the RPV head are also monitored.

5.4.3. French requirements and practices

Electricité de France (EDF) implemented a procedure called “transient bookkeeping” when they began operation of their first PWRs and now have a database covering more than 540 reactor-years [141]. This procedure meets a regulatory requirement in the decree of February 26, 1974 and has allowed EDF to confirm that their operating transients are less severe than their design transients. EDF is now studying an automatic device to book-keep complex transients in some nozzles (like charging line or steam generator feedwater nozzles); the general book-keeping of transients, needed for RPV (and all class 1 components) fatigue evaluation, remains a manual procedure done by operators of each plant.

Transient bookkeeping relies on the information collected by the units operating sensors: primary loop temperatures, primary and secondary pressures, auxiliary line temperatures, and in addition some logic signals (valve positions, etc.). Approximately 40 parameters are measured and recorded at a 4-loop PWR. Instrumentation has not been installed to measure local phenomena such as flow stratifications; however, transfer functions have been developed to estimate the fatigue associated with such phenomena. The threshold values for calculating fatigue usage are a change in:

- primary loop temperature: 5°C in 3 hours
- primary pressure: 1 MPa
- secondary pressure: 0.5 MPa
- auxiliary circuit temperature: 20° at 40°C/hour

These thresholds have been estimated very conservatively. The calculations show that for transients equivalent to the detection threshold, the calculated stress variations are far below the endurance limit for the most heavily loaded areas of the main primary system [142].

When a transient is detected, the design Transient File is inspected to find “an envelop transient,” i.e. a transient at least as severe in terms of its contribution to fatigue. It may or may not be a transient of the same functional nature. In general, the operator looks for a transient of the same functional nature and then he checks the “envelope” character by comparing the amplitudes and rate of change of the various parameters [143]. Should this approach fail, the operator will have to develop a new category for the transient file. Each transient event is added to the number of previous events and compared with the design limits. Each year a balance sheet of all the transient consumptions is prepared to assure that the overall fatigue usage is acceptable for all Class 1 components.

6. AGEING ASSESSMENT METHOD

6.1. RADIATION EMBRITTLEMENT ASSESSMENT METHOD

6.1.1. Radiation embrittlement assessment methods in the USA

The rules for the assessment of radiation embrittlement in the USA are given in 10 CFR Part 50, Appendices G and H. Appendix G [21, 22], “Fracture Toughness Requirements” specifies fracture toughness requirements for ferritic materials in the RPV. Appendix H, “ Reactor Vessel Material Surveillance Program Requirements”, provides the rules for monitoring the changes in the fracture toughness properties of the RPV beltline materials due to irradiation embrittlement using a surveillance programme.

Appendix G refers to RT_{NDT}, the nil-ductility temperature as the reference temperature of the RPV material for all conditions. The initial RT_{NDT} for the unirradiated material is defined in NB - 2331 of Section III of the ASME Code [7]. If the measured values (NDT by drop weight test and the 50 ft-lbs (68 J) temperature) required to determine the initial RT_{NDT}, NUREG - 800, “Standard Review Plan” provides a method to for calculating an acceptable estimation of RT_{NDT}.

As state above, Appendix H provides the rules for monitoring the changes in the fracture toughness properties of the RPV beltline materials due to irradiation embrittlement using a surveillance programme. Appendix H refers to ASTM Standard E 185, ”Standard Practice for Light - Water Cooled Nuclear Power Reactor Vessels”. Appendix H requires that for surveillance capsule withdrawal, the test procedures and reporting requirements must meet the requirements given in E 185 - 82 to the extent practicable. Recently, ASTM Committee E 10 - on Nuclear Technology and Applications published revisions to Standard E 185. A new standard E 185, “Standard Practice for Design of Surveillance Programs for Light-Water Moderated Nuclear Power Reactor Vessels” is now published in Volume 03.01, 2004 Annual Book of ASTM Standards. E 185 - 04 covers procedures for designing a surveillance programme. A second standard, ASTM E 2215, “Standard Practice for the Evaluation of Surveillance Capsules from Light-Water Moderated Nuclear Power Reactor Vessels”, is now published in Volume 03.01, 2004 Annual Book of Standards. E 2215 - 04 provides the utility the option of performing fracture toughness testing in accordance with ASTM E 1820, “Standard Test Method for Measure of Fracture Toughness” and ASTM E 1921, “Test Method for Determination of Reference Temperature, T_0, for Ferritic Steels in the Transition Range” [41].

General Criterion 31 of Appendix A to 10 CFR Part 50 requires that the NPP design reflect the uncertainties in determining the effects of irradiation on material properties. Appendices G and H which implement, in part Criterion 31, necessitate the calculation of changes in fracture toughness of reactor vessel materials by radiation embrittlement throughout the service life. The calculation procedures to meet the intent of Criterion 31 are given in Regulatory Guide (R.G.) 1.99, Revision 2 [127]. Appendix G, 10 CFR Part 50 provides the calculation procedures to estimate RT_{PTS} throughout the service of the RPV. The calculation procedures for RT_{PTS} are similar but are not the same calculation procedures given in R.G. 1.99, Revision 2.

In summary, radiation embrittlement assessment methods in the USA requires utilization of 10 CFR Part 50 Appendices G and H, ASTM Standard 185 - 82, and R.G. 1.99 Revision 2.

The RT_{NDT} increases as the material is exposed to fast-neutron radiation. To find the most limiting RT_{NDT} at any time period in the reactor's life, the ΔRT_{NDT} due to the radiation exposure associated with that time period must be added to the original unirradiated RT_{NDT}. The extent of the shift in RT_{NDT} is enhanced by certain chemical elements (such as copper and nickel) present in reactor vessel steels. Regulatory Guide 1.99, Revision 2 is used for the calculation of RT_{NDT} values in the beltline region as follows:

$$RT_{NDT} = \text{Initial } RT_{NDT} + \Delta RT_{NDT} + \text{Margin} \qquad (42)$$

The initial RT_{NDT} is the reference temperature for the unirradiated material as defined in paragraph NB-2331 of Section III of the ASME Boiler and Pressure Vessel Code. If measured values of initial RT_{NDT} for the material in question are not available, generic mean values for that class of material may be used if there are sufficient test results to establish a mean and standard deviation for the class. The margin term accounts for the uncertainty in the initial RT_{NDT} and scatter in the data used to estimate ΔRT_{NDT}.

ΔRT_{NDT} is the mean value of the adjustment in reference temperature caused by irradiation and should be calculated as follows:

$$\Delta RT_{NDT} = [CF] f^{(0.28-0.10 \log f)} \qquad (43)$$

where CF is a chemistry factor which is a function of copper and nickel content, f is the fluence in $10^{23} n/m^2$, and ΔRT_{NDT} has units of Fahrenheit degrees [127].

Master Curve methodology is gaining acceptance throughout the world as an alternative to the RG 1.99, Revision 2 and ASME Code methodology for determining the fracture toughness. However, the validity of the Master Curve methodology (ASTM E 1921) is now in question based upon recent findings at the Oak Ridge National Laboratories (ORNL). The recent results from ORNL are discussed at the end of this Section. In the Master Curve methodology materials are tested in accordance with the test standard ASTM E 1921-97 [144]. This test method determines a reference temperature, T_0, which characterizes the fracture toughness of ferritic steels that experience onset of cleavage cracking at elastic, or elastic-plastic instabilities. Since low alloy RPV steels behave in this manner, the Master Curve methodology is applicable to low alloy RPV steels. ASME Code Cases N-629 and N-631 define a reference temperature, RT_{TO}, which may be used as an alternative to the indexing reference temperature , RT_{NDT}. These code cases indicate define RT_{T0} as:

$$RT_{T0} = T_0 + 35^{o}F \qquad (44)$$

This relationship between RT_{TO} and T_o value was proposed by the ASME task group in order to maintain consistency with the ASME current licensing basis [145]. This 35ºF value was determined as the appropriate add-on to be applied to make RT_{T0} an acceptable replacement of RT_{NDT}. This value also adequately bounded the data from plate HSST02 that is the lowest data in the original K_{IC} ASME database.

The Master Curve methodology has been utilized in the USA to determine the Initial RT_{NDT} and the ΔRT_{NDT}. When the Master Curve methodology was utilized in RPV safety assessments, additional margins were added to account for uncertainties between the material that was tested, the materials in the RPV and bias in the test data. The NRC evaluation of these uncertainties is described in a May 1, 2001 letter to M. Reddemann from J. G. Lamb (This document may be retrieved from the NRC Web-Site using ADAMS Accession No. ML011210180). J.G. Lamb's letter discussed in part the determination of the Initial RT_{NDT} and the ΔRT_{NDT} of materials tested as part of the Kewaunee Nuclear Power Plant Surveillance

Capsule Program. As part of the Kewaunee data and acquisition programme, a second surveillance capsule has been tested. The NRC is currently reviewing the new data and their position on the uncertainties of use of the Master Curve methodology will be published in the near future.

ORNL recently published two reports [146] and [147] that providing test results that showed T_0 obtained from testing of PCVN specimens versus T_0 determined from 1 CT specimens had a disparity of 21 °C. Thus, the question of specimen size as well as geometry becomes an uncertainty.

6.1.2. Radiation embrittlement assessment methods in Germany

The German Nuclear Safety Standards KTA 3201.2 [148] and KTA 3203 [149] require that the USE must remain above 68 J (50 ft-lb) during operation. If a USE value of more than or equal to 68 J cannot be proven by the surveillance programme, further measures have to be undertaken to confirm the safety of the RPV. Such measures shall be defined in accordance with the authorized expert.

The fracture toughness during operation is determined by use of the adjusted reference temperature. The procedure to calculate the fracture toughness curve is given in the KTA 3201.2. The determination of the adjusted reference temperature itself is described in the KTA 3203. The adjusted reference temperature may be determined either according to the reference temperature RT_{NDT} concept or the Master Curve concept.

Up to a fluence of 1×10^{23} n/m², a fixed value of 40 K may be used as adjusted reference temperature for the materials where no data from surveillance sets are available. For fluences of more than 1×10^{23} n/m², the value for the limiting adjusted reference temperature given by KTA 3203 must be proven by surveillance data for all beltline materials from the sets as required in KTA 3203 (see Fig. 41-43 in page 136-138).

6.1.3. Radiation embrittlement assessment methods in France

The French requirements are specified in the 1974 Order and the corresponding rules are presented in RCC-M “Design and Construction Rules for the Mechanical Components of PWR Nuclear Islands” [24, 27] and RSE-M “In-service Inspection Rules for the Mechanical Components of PWR Nuclear Islands” [28, 29] .

Using the surveillance programme results a new shift formula has been proposed for welded joins:

$$\Delta RT_{NDT}\ (°C) = (8+(24+1537(P-0.008)+238(Cu-0.08)+191\ Ni^2(Cu))f^{0.35} \text{ for base metal} \quad (45a)$$

$$\Delta RT_{NDT}\ (°C) = (22+(13+823(P)+148(Cu-0.08)+157\ Ni^2(Cu))f^{0.45} \text{ for welded joins} \quad (45b)$$

$$\Delta RT_{NDT}(°C)=(8+(24+1537(P-0.008)+238(Cu-0.08)+191\ Ni^2(Cu))f^{0.35} \quad (46)$$

where

P is the weight % phosphorus, Cu is the weight % copper, Ni is the weight % nickel, and f is fluence in 10^{23} n/m^2. This equation was specifically developed for the French RPV material and is based on a large number of measurements. Note that this formula is different from the equation specified in USNRC Regulatory Guide 1.99, Rev. 2, and shown above as

Equation 25. Changes in both base and weld metal reference nil-ductility transition temperatures calculated with Equation 46 are compared with measurements in Fig. 53. Only four measured values of ΔRT_{NDT} out of 150 measurements exceed the predicted values less than 10 °C, which suggest that the correlation is conservative.

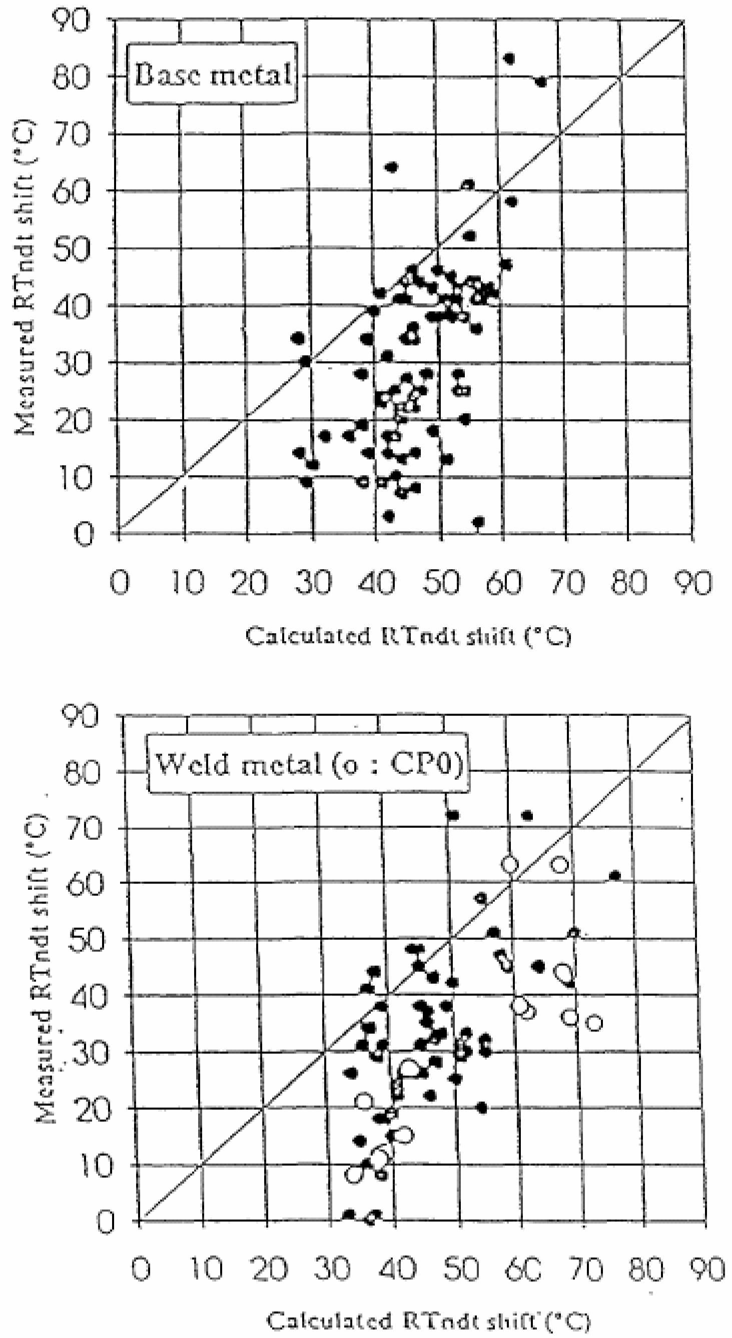


Fig. 53 Comparison of measured and calculated RT_{NDT} shifts in French RPV steel.

6.1.4. WWER radiation embrittlement assessment methods

Even though most of the WWER reactors, including the WWER-1000 and the WWER-440/V 213 types, have RPV surveillance specimen programmes, the Soviet Code [36] is based on calculations. The primary reason for this is because the Code was prepared for RPV design, rather than plant operation. The WWER design basis is discussed in Section 3.5 of this report. Section 3.5.3 discusses the stress analysis procedures including stress categories, stress intensity limits, areas of the RPV to be analysed, and stress analysis methods. Section 3.5.4 discusses design and analysis against brittle fracture including the allowable fracture toughness, postulated defects, stress intensity factors, and transition temperatures and temperature shifts.

There is no valid procedure in Russian codes for the evaluation of surveillance specimen test data and their application to RPV integrity.

The following approach is used in the: VERLIFE [42] Procedure:

Two equivalent approaches are given :

(a) based on the "Master Curve" approach

- transition temperature T_0 is determined in accordance with the [144] for the fluence as close to the calculated fluence for the RPV state under assessment,

- the standard deviation σ of the estimate of T_0 is given by [144]:

$\sigma_1 = \beta / N^{0.5}$ (°C) (47)

Where

N = total number of specimens used to establish the value of T_0,

$\beta = + 18$ °C.

T_0 consider the scatter in the materials, another margin denoted in what follows ΔT_M should be applied. If this value is not available the application of the following values is suggested

$\Delta T_M = 10°C$ for the base material, (48)

$\Delta T_M = 16°C$ for weld metals.

The resulting margin is:

$\sigma=(\sigma_1^2+\Delta T_M^2)^{1/2}$ (49)

The reference temperature used in integrity evaluation, RT_0, is defined as:

$RT_0 = T_0 + \sigma$ (50)

(b) based on the transition temperature approach

- The change in the transition temperature is measured using both Charpy V-notch impact and static fracture toughness tests. The shift in the transition temperature is then compared to the value calculated using the A_F coefficient in the code.

- However, a new radiation embrittlement trend curve for a given RPV material can be constructed if values from at least three different neutron fluences are obtained and statistically evaluated. The mean trend curve should be vertically shifted upward by the value of ΔT_M in accordance with the relation (48). If any experimental point exceeds this adjusted trend curve, the curve should be shifted further until it bounds all data. This upper boundary of the shifts is to be used in assessment of RPV resistance against fast fracture.
- This overall approach is consistent with the definition of the A_F coefficient as the upper bound value of the experimental data.
- Extrapolation can be performed only for fluences not exceeding the maximum experimental value by a factor of two.

Within the IAEA CRP on "Surveillance Programmes Results Application to RPV Integrity Assessment", guidelines for evaluation of surveillance test data and their application to WWER-440 RPVs are under preparation.

6.1.5 Radiation embrittlement assessment methods in Japan

Japanese assessment methods are specified in JEAC4201-2000 [140].

The document provides the Japanese embrittlement predictive equation which is slightly different from the equations provided in R.G. 1.99, Rev. 2 [127] because it takes into account Japanese experimental data.

ΔRT_{NDT} is calculated as follows:

$$\Delta RT_{NDT} = [CF] f^{(0.29-0.04 \log f)} \quad \text{for base metal} \tag{51}$$

$$[CF] = -16 + 1{,}210 \times P + 215 \times Cu + 77\sqrt{Cu \times Ni}$$

$$\Delta RT_{NDT} = [CF] f^{(0.25-0.10 \log f)} \quad \text{for weld metal} \tag{52}$$

$$[CF] = 26 - 24 \times Si - 61 \times Ni + 301\sqrt{Cu \times Ni}$$

where Cu, Ni, P and Si are contents of copper, nickel, phosphorus and silicone, and "f "is the fluence in $10^{23} n/m^2$, and ΔRT_{NDT} has units of Celsius degrees [140].

To calculate ΔRT_{NDT} at any depth (e.g., at 1/4 or 3/4 of the wall thickness), the following formula is used first to attenuate the fluence at the specific depth:

$$f = f_0 \left(e^{-.24a/25.4}\right) \tag{53}$$

where a (in mm) is the depth into the vessel wall measured from the vessel clad/base metal interface and f_0 is the fluence at RPV inner surface. The resultant fluence is then put into Equation (51) along with the chemistry factor to calculate ΔRT_{NDT} at the specific depth.

6.2. THERMAL AGEING ASSESSMENT METHODS

As discussed in Section 4.2, there is no evidence of a significant change in the ΔRT_{NDT} due to thermal embrittlement. Therefore, we do not recommend the use of any thermal ageing assessment methods.

6.3. FATIGUE ASSESSMENT METHODS

6.3.1. Fatigue assessment in the United States of America

Crack initiation

Crack initiation is estimated by determining the fatigue usage at a specific location which results from either actual or design-basis cyclic loads. The time-to-initiation can be predicted only if the applied load sequences and recurrence frequencies are known. If the cycling loading is random, estimates of time to initiation are uncertain.

For a fatigue life evaluation, the data needed are the amplitude and number of stress cycles experienced during a given operating period and the amplitude and number of cycles that lead to crack initiation in laboratory specimens. The sum of the ratios of these quantities gives the cumulative fatigue usage factor. The best source of information for the relatively newer US plants is the certified stress report and the design specification. The certified stress report gives the design-basis cumulative usage factors for vessel components and the Code allowable number of cycles for prescribed events.

The fatigue usage factor is defined according to ASME Code requirements. This value must not exceed 1.0 during the design life of the component. With the conservatism inherent to this calculation, it is presumed that fatigue crack initiation can be prevented by ensuring that the fatigue usage factors remain below the limit of 1.0. The ASME Code fatigue design curves are based on data from smooth-bars tested at room temperature in air. The ASME Code applies a factor of 2 on strain range and a factor of 20 on the number of cycles to the smooth-bar data. The factor of 20 on cycles was intended to account for data scatter, size effect, surface finish and moderate environmental effects. However, current testing of fatigue specimens in reactor coolant environment indicates that the factor of 2 on strain and 20 on cycling may not be sufficient for all loading conditions.

Cyclic crack growth

Once a crack has initiated, either by fatigue or some other mechanism such as SCC, continued application of cyclic stresses can produce subcritical crack growth. The Paris crack growth relationship is used to calculate crack growth:

$$da/dN = C(\Delta K)^n \qquad (54)$$

where

da/dN	=	fatigue crack growth rate (distance/cycle);
ΔK	=	stress intensity factor range = (K_{max} - K_{min});
C, n	=	constants, related to material and environment; and
K_{max}, K_{min}	=	maximum and minimum stress intensity factors during the loading cycle

Crack growth rates, such as those in the ASME Code, are not constant for all ranges of ΔK. There are three regimes. These are: crack growth at low, medium, and high ΔK values. At very low ΔK values, the growth rate diminishes rapidly to vanishingly low levels. A threshold stress intensity factor range (ΔK_{th}) is defined as that below which fatigue damage is highly unlikely.

At the high end of the ΔK range, crack growth increases at a faster rate. This acceleration is partially a result of the increasing size of the plastic zone at the crack tip, which has the effect of increasing the effective stress intensity factor range (ΔK_{eff}). In addition, as the maximum applied stress K_{max} approaches the critical applied stress intensity (K_c), local crack instabilities occur with increasing frequency. Increasing the R ratio (K_{min}/K_{max}) causes an increase in cyclic crack growth rate.

Knowing the history of stress cycle events in conjunction with the appropriate crack growth correlations allows the prediction of crack growth rate in components. Furthermore, information in Section XI of the ASME Code on crack initiation and crack arrest fracture toughness of low alloy steel can be used to calculate the critical crack size of the component, and thus time to failure, or residual life.

The preceding discussions are strictly valid only for metallurgically large cracks (in the literature, the minimum size of a metallurgically large crack ranges from approximately 0.0025 to 0.18 mm (0.0001 to 0.005 in.)). For short cracks (i.e., crack sizes comparable to the size of the high stress field at the tip of the stress raiser at the crack initiation site), the applicability of analyses based on LEFM tends to break down in some instances. Various attempts have been made to address the growth rates of short cracks, but a universally applicable treatment has yet to be established. However, the inspections conducted in accordance with the ASME Code are sufficient to detect crack growth before the acceptance criteria are reached.

It should be noted that for indications found and sized during ISIs such that crack growth evaluation is required, LEFM-based crack growth procedures are adequate and sufficient. Very short cracks for which LEFM-based procedures are not applicable are within acceptance criteria limits for size.

The crack size at the end of a prescribed period of operation can be determined if the cyclic loading sequence is known and a crack growth curve (da/dN versus ΔK), such as that in ASME Section XI, Article A4300, is available.

Fatigue damage management programmes

For component locations and parts with no history of fatigue damage, the current ASME Code Section III, Subsection NB-3000 fatigue design basis can be shown to remain valid throughout the design life; in this case, the original design-basis transients. The total usage factor must be shown to be valid, in terms of the numbers and severity of the loads, for any extended operation and the calculated fatigue usage factor, including any modifications to the design-basis transients to account for actual plant operating transients not enveloped by the original design-basis transients, must be shown to be less than unity. The Section III, Subsection NB fatigue evaluation procedures remain valid for these calculations. If the projected fatigue usage factor for the extended operation exceeds unity, detailed fatigue reanalysis considering actual plant operating transients, including partial cycle counting, in lieu of the original assumed design-basis transients, may be used. The fatigue usage factor limit for this reanalysis remains unity. The Section III, Subsection NB evaluation procedures remain valid for these calculations.

For component locations and parts with a history of fatigue damage, or as an alternative to the analytical verification of the adequacy of the original fatigue design basis throughout the design life, an effective in-service examination programme for managing the effects of potentially significant fatigue damage is needed. Formal inservice examination requirements are provided for each plant in its plant ISI and Inservice Testing programmes and are referenced to an applicable edition of the ASME Code Section XI Rules for ISI of Nuclear Power Plant Components. The plant ISI programme, including any commitments to enhanced or augmented inspections as the result of plant operating experience or regulatory enforcement and any special reassessments of loading and material conditions, provides an acceptable basis for continued operation of a component. The intervals for these examinations, and the requirements for expansion of the number of locations examined if flaws are detected, assure that significant undetected fatigue degradation of components will not occur.

Fatigue reanalysis

If the confirmation of the current fatigue design basis for an extended operation is to be demonstrated, the procedure to be followed is similar to that used during the initial plant design. During the design of plant components, in accordance with NB-3000, a set of design-basis transients was defined. These design-basis transients, as described by temperature, pressure, flow rate, and number of occurrences, were intended to conservatively represent all transients expected during the design life of the plant. The plant Technical Specifications require that major cycles be tracked during service, relative to actual operating transients, to assure satisfaction of fatigue design requirements. However, since details of the Technical Specification transient tracking requirements vary widely from plant to plant, the demonstration that the design-basis transients remain valid for any extended operation, such that the numbers and severity of actual operating transients remain enveloped, is a plant-specific consideration. A variety of methods are available for this demonstration. These include regrouping of design-basis transients, taking credit for partial (versus full) cycle transients, use of actual plant transients rather than design-basis transients, or using a more sophisticated cycle monitoring programme.

The second step in the fatigue design basis confirmation process is demonstrating that the fatigue usage factor calculated for the most critical component location or part remains below unity, as determined by the use of the confirmed design-basis transients extended through the operation. The fatigue analysis procedures of NB-3000 remain valid for these calculations. The ASME Section III rules require that fatigue usage factors calculated for this extended period remain below unity. If this criterion is satisfied, the component is presumed safe (i.e., no fatigue cracks have been initiated).

For components with a reasonably high degree of design margin of safety with regard to fatigue limits, acceptable results for extended life can be demonstrated by conservative evaluation. For more limiting components, a conservative approach may project cumulative fatigue usage factors which approach or exceed a value of 1.0. Unless the excessive conservatism can be removed, more frequent ISIs may be required or, in the worst case, replacement or refurbishment may be recommended far too prematurely.

One way to remove conservatism is to refine the fatigue analysis. The methodology can be enhanced from simple elastic calculations to elastic-plastic or even fully plastic approaches. The definition of loading cycles can also be refined, including regrouping of design basis transients. Credit can be taken for partial versus full design basis transients. Actual plant loading cycles can be used instead of originally assumed design loading cycles. These alternative techniques can be implemented in a manner that is consistent with the ASME Code to show that fatigue damage accumulation will remain within established limits for any extended operation.

Finally, if a refined fatigue analysis is unable to show that the component will remain within the established limits, the component can be examined for detectable fatigue damage and repaired, refurbished or replaced as appropriate.

Section 6.3.1 of the previous TECDOC 1120 indicated that for *components, locations and parts with a history of fatigue damage,* or as an alternative to the analytical verification of the adequacy of the original fatigue design basis throughout the design life, an effective in-service examination programme for managing the effects of potentially significant fatigue damage is needed. The NRC has not endorsed this alternative for extended NPP operation.

Additionally, for extended NPP operation, the NRC requires license renewal applicants to evaluate a sample of critical components, using correlations developed from fatigue testing in reactor coolant environments. NUREG/CR-6260 [150] identifies the minimum number of components selected for evaluation. Formulas for calculating the environmental life correction factors are contained in NUREG/CR-6583 [151] for carbon and low alloy steels and in NUREG/CR-5704 [152] for austenitic stainless steels.

6.3.2. Fatigue assessments in Germany

The procedure as described in the ASME Code for the assessment of crack initiation and cyclic crack growth is basis for the relevant stipulations in the German KTA 3201.2 [148].

6.3.3. Fatigue assessments in France

The RCC-M general rules [24] and (S,N) fatigue curves are similar to the ASME Section III B3000 rules. However, some specific rules have been developed and incorporated into RCC-M to analyse crack-like defects (RCC-M Appendix ZD), studs (use of experimental results), and plastified areas by Ke optimization, which are not in the ASME Code.

6.3.4. WWER fatigue assessments

Fatigue evaluations

The peak stresses are the main concern in the WWER fatigue evaluations. The Code gives specific rules for fatigue calculations and design curves for different materials as well as fatigue strength reduction factors for welded joints and for some operational factors such as radiation and corrosion.

Two methods are allowed in the Code for determining the fatigue:

(a) design curves for a rough estimate,

(b) design formulae for more detailed calculations or when the design curves cannot be satisfied.

Generally, the following safety factors are used:

- pressure vessel materials:

  $n_\sigma = 2,$

  $n_N = 10$

- bolting materials:

  $n_\sigma = 1.5,$

  $n_N = 3$

where the factor n_σ is applied to the stress and the factor n_N is applied to the number of cycles in the same manner as in the ASME Code. It must be noted that design curves as well as design formulae were constructed as lower boundaries of existed experimental data obtained during Qualification Programmes of RPV materials. Moreover, the coefficients $\varphi_w \leq 1$ are incorporated into the calculational formulas. These coefficients conservatively adjust the formulas for the effects of the welded joints on the fatigue life. The stress and number of cycle factors listed above are lower for the bolting materials than for the pressure vessel materials because the bolting is changed out periodically. Also, bolting failure should result in leakage rather than rupture.

Cumulative usage factors D are calculated using linear Miner's law; the maximum allowable value is equal to one.

6.3.5 Fatigue assessment in Japan

General requirement

The fatigue evaluation method and (S,N) fatigue curves stipulated in the METI notification No.501 [43] and JSME Code and Standards "Code for Design and Construction", JSME SNA2-2002 [44] are similar to the ASME Section III B3000 rules.

Environmental fatigue evaluation

In September 2000, Japanese regulatory body, MITI (current METI), published a notification which required electric utilities to perform fatigue evaluation for the plant life management evaluation taking into account environmental effect.

The parameter for evaluating environmental effects is the fatigue life reduction factor for environmental effects, $F_{en.}$ F_{en} represents the reduction in fatigue life resulting from the high-temperature water LWR environment. As shown in Equation 55 below, F_{en} is defined as the fatigue life obtained from fatigue tests under room temperature in air, divided by the fatigue life obtained from fatigue tests under high-temperature LWR water conditions with the same strain amplitude.

$$F_{en} = \frac{N_A}{N_W} \qquad (55)$$

where, N_A is fatigue life under room-temperature atmosphere and N_w is fatigue life under water environment.

The cumulative usage factor allowing for environmental effects, UF_{en}, can be expressed by the following equation, using F_{en}.

$$UF_{en} = \sum_{i=1}^{n} U_i \times F_{en,i} \qquad (56)$$

Where U_i and $F_{en,i}$ represent the cumulative usage factor and fatigue life reduction factor for environmental effects, respectively, for the i-th load set pair (individual transient cycle evaluations) among the n-number of load set pairs (all evaluated transient cycles).

In the above equation, F_{en} is calculated according the following two F_{en} equations for carbon steel/ low alloy steel and for austenitic stainless steel. They are provided in the MITI environmental fatigue evaluation guideline.

F_{en} for carbon steel/ low alloy steel:

$$
\begin{aligned}
&\ln(F_{en}) = -(0.199 \times T^* \times O^* + 0.112) \times S^* \times \dot{\varepsilon}^* \\
&\text{where} \\
&\quad \dot{\varepsilon}^* = 0 \quad (\dot{\varepsilon} > 1.0\%/\text{sec}) \\
&\quad \dot{\varepsilon}^* = \ln(\dot{\varepsilon}) \quad (1.0\%/\text{sec} \geq \dot{\varepsilon} \geq 0.0004\%/\text{sec}) \\
&\quad \dot{\varepsilon}^* = \ln(0.0004) \quad (\dot{\varepsilon} < 0.0004\%/\text{sec}) \\
&\quad T^* = 0.00531 \times T - 0.7396 \quad (T \geq 180℃) \\
&\quad T^* = 0.216 \quad (T < 180℃) \\
&\quad O^* = \ln(DO/0.03) \quad (0.03 \leq DO \leq 0.5\text{ppm}) \\
&\quad O^* = 0 \quad (DO < 0.03\text{ppm}) \\
&\quad O^* = \ln(0.5/0.03) \quad (DO > 0.5\text{ppm}) \\
&\quad S^* = 17.23 \times S + 0.777
\end{aligned}
\tag{57}
$$

F_{en} for austenitic stainless steel:

$$
\begin{aligned}
&\ln(F_{en}) = 1.233 - P \times \ln(\dot{\varepsilon}^*/0.4) \\
&\textit{where} \\
&\quad P = 0.04 \quad (T \leq 100℃) \\
&\quad P = 9.33 \times 10^{-4} \times T - 0.053 \quad (100℃ < T < 325℃) \\
&\quad P = 0.25 \quad (T \geq 325℃) \\
&\quad \dot{\varepsilon}^* = 0.4 \quad (\dot{\varepsilon} \geq 0.4\%/\text{sec}) \\
&\quad \dot{\varepsilon}^* = \dot{\varepsilon} \quad (0.0004\%/\text{sec} \leq \dot{\varepsilon} \leq 0.4\%/\text{sec}) \\
&\quad \dot{\varepsilon}^* = 0.0004 \quad (\dot{\varepsilon} < 0.0004\%/\text{sec})
\end{aligned}
\tag{58}
$$

$\dot{\varepsilon}^*$ is strain rate-dependent parameter, T^* is temperature-dependent factor and O*is dissolved oxygen dependent factor.

The MITI environmental fatigue evaluation guideline mentions that these two equations were acquired from experiment data of PWR coolant environment (DO≤0.005 ppm). F_{en} for BWR coolant environment (DO>0.005 ppm) is under preparation. It is possible to apply the above two F_{en} equations to BWR coolant environment but evaluation results could be a little too conservative.

The MITI environmental fatigue evaluation guideline does not specify a detailed evaluation procedure for evaluating actual plant conditions. “Guidelines on Environmental Fatigue Evaluation for LWR Component” [153] published by Thermal and Nuclear Power Engineering Society in June 2002 provides a detailed procedure and specific and practical techniques. The document is used as a guidance documents for evaluating environmental fatigue for LWR components.

6.4. ASSESSMENT METHODS FOR PWSCC OF ALLOY 600 COMPONENTS

6.4.1. PWSCC assessments in the USA

Section 6.4 of the previous TECDOC 1120 indicated PWSCC crack growth can be calculated with the following empirical equation developed by Scott [154]:

$$da/dt = 2.56e^{-(33{,}000/RT)} (K_I - 9)^{1.16} \quad (59)$$

where da/dt is the PWSCC growth rate, K_I is a crack tip stress intensity factor in MPa $m^{1/2}$, T is temperature in degrees kelvin, and R is the universal gas constant. This model was developed by Scott using data obtained by Smialowski et al. of Ohio State University.

The EPRI PWR Materials Reliability Program (MRP) has compiled an extensive database of crack growth rates on thick-section Alloy 600 materials [155]. Using this data, the MRP, derived the following crack growth rate model at 325°C (617 ° F):

$$da/dt = 1.67 \times 10^{-12} (K_I - 9)^{1.16} \text{ m/s} \quad (60)$$

The MRP and Scott curves are relatively in close agreement, with the Scott curves being lower. It should be noted that the above equations imply a threshold for cracking susceptibility, K_{ISCC} = 9 MPa $m^{1/2}$. This involves assumptions not currently substantiated by actual crack growth rate data for Alloy 600 materials, since no test results for thick-walled materials are available for K values less than about 15 MPa $m^{1/2}$.

6.4.2. PWSCC assessment in France

Between 1991 and 1996, 52 vessel heads have been periodically inspected by contact eddy current [83]. 27 penetrations have been used to derive a crack growth law from 180 field cracks. [156]. Results are coming from hot dome at 300°C and cold dome at 290°C. An activation energy of 130 KJ/mole has been used. two laws are derived from this field experience:

- mean curve at 290°C :

$$da/dt = 0.03 (K-9)^{0.52} \quad (61)$$

- upper bound at 290°C :

$$da/dt = 0.3 (K-9)^{0.10} \quad (62)$$

with da/dt in µm/h and K in MPa $m^{0.5}$.

Fig. 54 presents the variation of crack growth rate with dome temperatures, the temperature effect of 30°C difference, is in the scatter band of the result data.

Fig. 55 presents a comparison of field data and laboratory results that confirm the validity of our proposed equations.

A complementary programme is going on to evaluate the crack growth rate in weld material [83, 156].

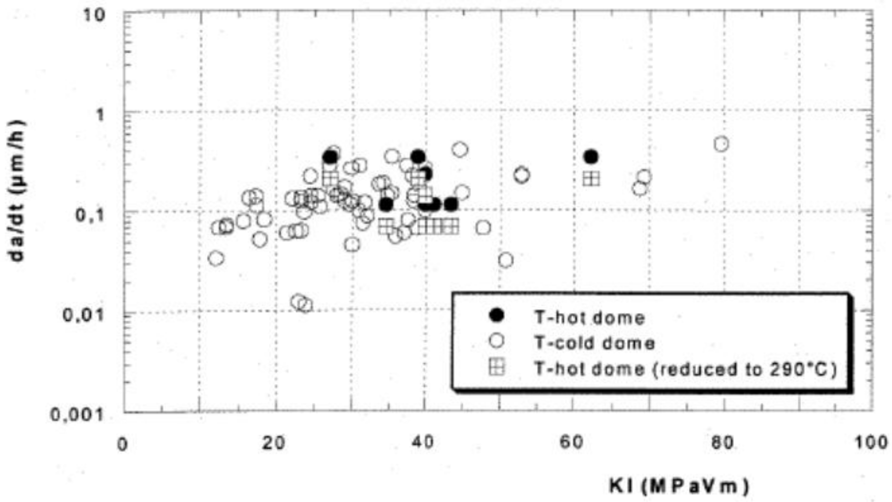


Fig. 54. Comparison of alloy 600 crack growth rate for hot and cold dome of similar plants (reduced at 290°C with Q=130 KJ:mole).

A complementary programme is going on to evaluate the crack growth rate in weld material [156, 157].

Last results confirm that:

- alloy 182 is susceptible to stress corrosion cracking in PWR primary water, only if the applied total stress exceeds the yield stress
- Fig. 56 presents results obtained under load control between 300 and 600 MPa at three different temperatures (330°C, 350°C and 360°C). The results are presented in term of true stress versus time after conversion to the 325°C temperature using an activation energy of 185 kJ/mole
- the stress corrosion cracking threshold is greater for alloy 182 than for alloy 600
- the stress relieve is an important beneficial aspect (all locations in French plants except CRDM nozzle and SG stub weld)
- comparison of results on alloy 182 and alloy 600 shows:
    - the same effects of parameters are observed for temperature, stress intensity factor and cold work effects,
    - specific effects have been observed on alloy 182 for heat treatment, dendrite orientation, heat to heat variation
- the crack growth rate obtains on alloy 182 is in the scatter band of results on alloy 600 and field data, see Fig. 57.

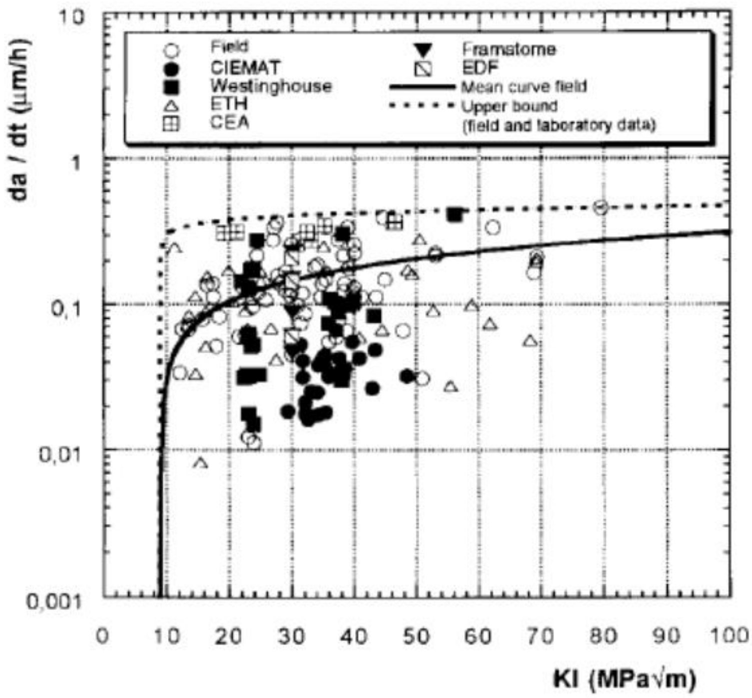


Fig. 55. Comparison of field and laboratory crack growth rate data obtained on alloy 600 (reduced at 290°C with Q=130KJ:mole).

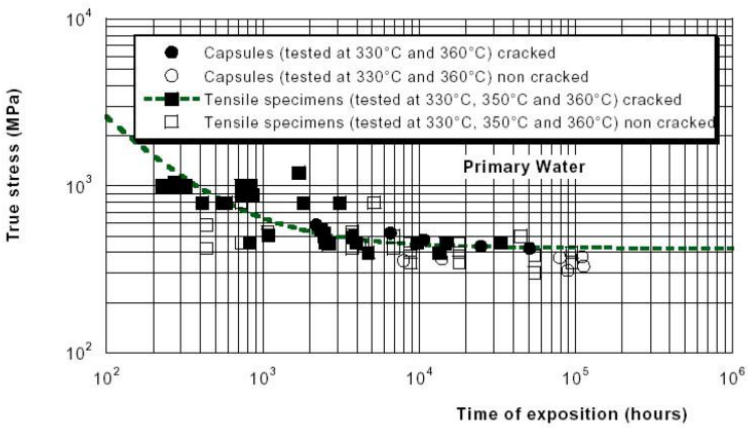


Fig. 56. Crack initiation time versus stress for alloy 182 at 325°C (Q=185kJ/mole).

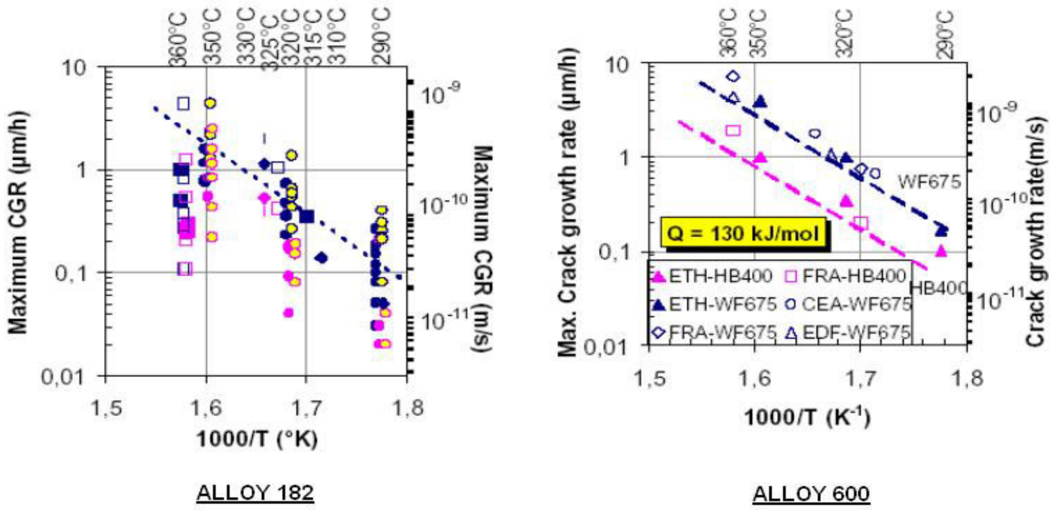


Fig. 57. Comparison of crack growth rates measured in alloys 182 and alloy 600.

6.5. ASSESSMENT METHODS FOR RPV CLOSURE HEAD STUD STRESS CORROSION CRACKING

Once SCC is suspected, detection and sizing of any cracks are required for determining the effects on the RPV closure head studs. ISI by volumetric means, such as ultrasonic testing (UT) is the only way to size SCC indications. Visual examination or dye-penetrant methods may detect SCC flaws but these techniques can only measure the length of the flaw on the surface.

Once flaws are detected and sized in RPV components such as closure head studs, analytical evaluation utilizing fracture mechanics is required to predict life remaining after the initiation of the detected flaw. As with the age related degradation mechanism fatigue, the sub-critical crack growth must be determined to assess and manage SCC in RPV components.

As discussed in Section 3, the ASME Boiler and Pressure Vessel Code, Section XI, Appendix A provides an analytical technique for assessing crack growth during the application of cyclic stresses. However, SCC being corrosion driven does not require cyclic loading for the SCC initiation flaw to grow. Therefore, information is required in terms of delta "a" versus delta "t" (da/dt. change in crack length with time).

In summary, volumetric ISI in conjunction with an analytical evaluation is a requirement for the assessment and management of SCC in the PWR RPV.

6.6. ASSESSMENT METHODS FOR BORIC ACID CORROSION

Section 6.6 of the previous IAEA-TECDOC-1120 indicated:

Boric acid corrosion due to leaking reactor coolant has resulted in wastage of the low alloy steels of the RPV flanges, top closure heads, and RPV studs at a rate of approximately

25 mm/year. Once a boric acid leak is detected, the wastage level of the given ferritic steel component must be determined. An assessment must be made to determine if the minimum design thicknesses for the given component have been violated. If the wasted component design thickness is violated, refurbishment by welding may be required. If the component's design thickness is marginal following detection of boric acid attack, an analytical evaluation is required to assess the component's "fit for service" status.

The rate of boric acid corrosion wastage of the low alloy steel is dependent upon the temperature of the boric acid and the concentration of oxygen and corrosive elements in the boric acid. EPRI in [158] reported corrosion rates for low alloy steel in oxygenated reactor coolant at rates exceeding 25 mm/year.

6.7. FLAW ASSESSMENT METHODS

6.7.1. Flaw assessment methods in the USA

Article IWA-3000, "Standards for Examination Evaluation", requires evaluation of flaws detected during the inservice examination. The acceptance standards for flaws detected during the ISI are given in IWB-3500, "Acceptance Standards"'. Flaws that exceed the allowable indication standards of IWB-3500 can be analysed in accordance with Appendix A "Analysis of Flaws" [159] to determine their acceptability. Appendix A to Section XI uses a procedure based upon the principles of LEFM for analysis of flaw indications detected during ISI. While Section III is a construction code, Section XI provides rules for the integrity of the structure during its service life. The concepts introduced in Appendix G to Section III are carried over to Appendix A to Section XI. Figure 58 shows the functional organization of ASME Section XI. The evaluation procedure can be summarized as follows: set up a simplified model of the observed flaw, calculate stress intensity factors, determine appropriate material properties, determine critical flaw parameters and apply acceptability criteria to the critical flaw parameters.

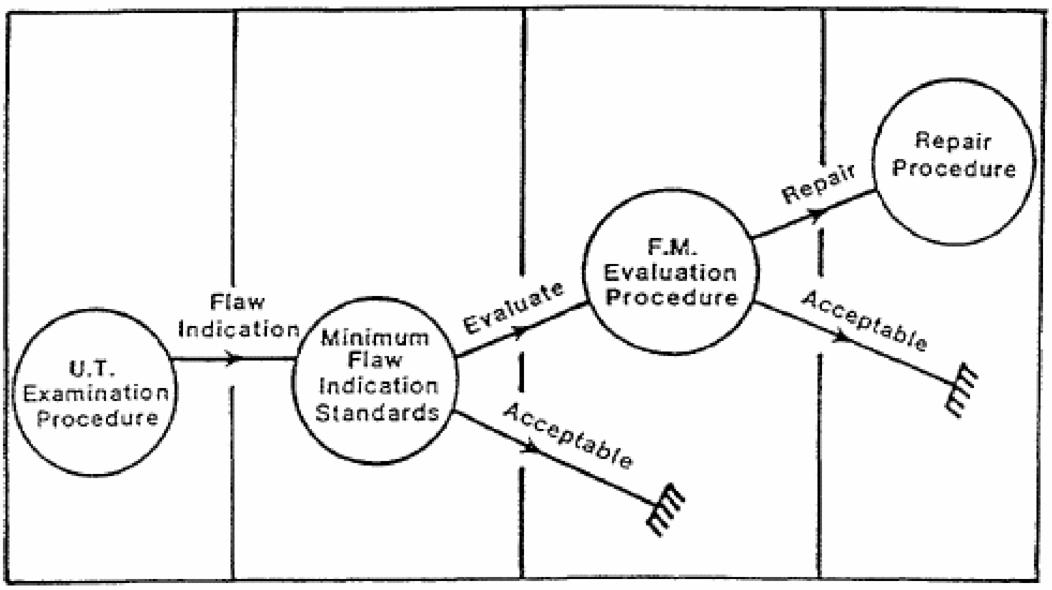


Fig. 58. Functional organization of ASME section XI in-service inspection documents.

Models for flaw analysis are given in A-2000 of Appendix A. Definitions are given covering flaw shape, proximity to closest flaw, orientation and flaw location to permit their application into an analytical model for LEFM.

Methods for K_I determination are given in A-3000 of Appendix A. Article A-3000 defines how the applied stresses at the flaw location can be resolved into membrane and bending stresses with respect to the wall thickness and presents a stress intensity factor expression for the flaw model.

Article A-4000 defines the material properties in terms of the fracture toughness of the given material K_{IC} and K_{Ia} (it should be noted that K_{Ia} is equivalent to K_{IR} of Section III) and in terms of the fatigue crack growth rate. As in Appendix G to Section III, the K_{IC} and K_{Ia} versus temperature curves are indexed using RT_{NDT}. For materials that are subjected to radiation, the degradation of the material fracture toughness due to the radiation must be accounted for. This is done through increasing RT_{NDT} by the appropriate indications from standard Charpy impact toughness tests on surveillance programme specimens.

An upper bound curve for fatigue crack growth data was measured on A 533 Grade B Class 1 and A508 steels and included the effects of temperature, frequency of load application and the pressurized water environment.

Finally, Article A-5000 gives the guidelines for determining the critical flaw parameters. These parameters are used in judging the acceptability of the observed flaw:

a_f = the maximum size of the observed flaw due to fatigue crack growth,

a_{crit} = the minimum critical size of the observed flaw under normal operating conditions,

a_{init} = the minimum critical size for initiation of non-arresting growth of the observed flaw under postulated accident conditions.

After these parameters are determined, they are compared to the acceptance criteria:

$$a_f < 0.1\ a_{crit} \quad \text{or} \quad a_f < 0.5 a_{init} \tag{63}$$

If these criteria are met, the observed flaw need not be repaired.

Evaluation of flaws in reactor pressure vessels with charpy upper-shelf energy (USE) less than 68 J (50fi-lb)

As discussed in Section 3.2, Appendix G to 10 CFR Part 50, "Fracture Toughness Requirements" [21], requires, in part, that reactor vessel beltline materials must maintain an USE of no less than 68 J (50 ft-lbs), unless it is demonstrated in a manner approved by the USNRC that the lower values of USE will provide margins of safety against fracture equivalent to those required by Appendix G to Section in of the ASME Code. In September 1993, the USNRC published draft Regulatory Guide DG-1023, "Evaluation of Reactor Pressure Vessels with Charpy Upper-Shelf Energy Less Than 50 ftlbs" [159]. This Regulatory Guide provides criteria which are acceptable to the USNRC for demonstrating that the margins of safety against ductile fracture are equivalent to those in Appendix G to Section of the ASME Code. The acceptance criteria are to be satisfied for each category of the transients; namely, Levels A and B (normal and upset), Level C (emergency) and Level D (faulted) conditions.

Two criteria must be satisfied for Level A and B conditions, as described below for a postulated semi-elliptical surface flaw with a flaw depth to wall thickness ratio (a/t) equal to 0.25, an aspect ratio or surface length to flaw depth of six to one and oriented along the material of concern. If the base metal is governing, the postulated flaw must be axially oriented. Smaller flaw sizes may be used on an individual case basis if a smaller size of the above postulated flaw can be justified. The expected accumulation pressure is the maximum pressure which satisfies the requirement of ASME Section III, NB-7311 (b). The two criteria are:

(1) The crack driving force must be shown to be less than the material toughness as given below:

$$J_{applied} < J_{0.1} \tag{64}$$

where $J_{applied}$ is the J-integral value calculated for the postulated flaw under pressure and thermal loading where the assumed pressure is 1.15 times expected accumulation pressure, and with thermal loading using the plant specific heatup and cooldown conditions. The parameter $J_{0.1}$ is the J-integral characteristic of the material resistance to ductile tearing ($J_{material}$)> as usually denoted by a J-R curve, at a crack extension of 2.54 mm (0.1 inch).

(2) The flaw must be stable under ductile crack growth as given below:

$$\frac{dJ_{applied}}{da} < \frac{dJ_{material}}{da} \tag{65}$$

(or with the load held constant, $J_{applied}$ must equal $J_{material}$) where $J_{applied}$ is calculated for the postulated flaw under pressure and thermal loading for all service Level A and B conditions and the assumed pressure is 1.25 times expected accumulation pressure, with a thermal loading as defined above.

The J-integral resistance versus crack growth curve used should reflect a conservative bound representative of the vessel material under evaluation.

For Level C conditions when the Charpy USE of any material is less than 68 J (50 ft-lb), postulate interior semi-elliptic surface flaws with their major axis oriented along the material of concern and the flaw plane oriented in the radial direction. Postulate both interior axial and circumferential flaws and use the toughness properties for the corresponding orientation. Consider surface flaws with depths up to one tenth the base metal wall thickness, plus the clad, but with total depth not to exceed 25.4 m (1.0 inch) and with aspect ratios of six to one surface length to flaw depth. Similar flaw sizes may be used on an individual case basis if a smaller size can be justified. For these evaluations, two criteria must be satisfied, as described below:

(1) The crack driving force must be shown to be less than the material toughness as given below:

$$J_{applied} < J_{0.1} \tag{66}$$

where $J_{applied}$ is the J-integral value calculated for the postulated flaw in the beltline region of the reactor vessel under the governing level C condition. $J_{0.1}$ is the J-integral characteristic of the material resistance to ductile tearing ($J_{material}$), as usually denoted by a J-R curve test, at a crack extension of 2.54 mm (0. 1 inch).

(2) The flaw must also be stable under ductile crack growth as given below:

$$\frac{dJ_{applied}}{da} < \frac{dJ_{material}}{da} \tag{67}$$

(or with the load held constant, $J_{applied}$ must equal $J_{material}$) where $J_{applied}$ is calculated for the postulated flaw under the governing level C condition. The J-integral resistance versus crack growth curve shall be a conservative representation of the vessel material under evaluation.

For Level D conditions when the Charpy USE of any material is less than 68 J (50 ft-lb), postulate interior semi-elliptic surface flaws with their major axis oriented along the weld of concern and the flaw plane oriented in the radial direction with aspect ratio of six to one. Postulate both interior axial and circumferential flaws and use the toughness properties for the corresponding orientation. Consider postulated surface flaws with depths up to one tenth the base metal wall thickness, plus the clad, but with total depth not to exceed 25.4 m (1.0 inch) and with aspect ratios of six to one surface length to depth. Smaller flaw sizes may be used on an individual case basis if a smaller size can be justified. For these evaluations, the following criterion must be met.

The postulated flaw must be stable under ductile crack growth as given below:

$$\frac{dJ_{applied}}{da} < \frac{dJ_{material}}{da} \tag{68}$$

(or with the load held constant, $J_{applied}$ must equal $J_{material}$) where $J_{applied}$ is calculated for the postulated flaw under the governing level D condition. The material property to be used for this assessment is the best estimate J-R curve.

6.7.2. Flaw assessment methods in Germany

Indications found during ISI have to be considered as being cracks and have to be evaluated on basis of linier elastic fracture mechanics evaluations. Conservatively, the crack has to be treated as a surface crack with an aspect ratio of:

$$a/2c = 1/6 \tag{69}$$

The maximum allowable defect size is defined by the criteria:

$$K_{Imax} = K_{Ic}/1.5 \tag{70}$$

Elasto-plastic fracture mechanics approaches and other advanced methods are only applied and accepted in individual cases. General stipulations for their implementation into the KTA Code are under preparation. Specific requirements for vessels with a Charpy USE less than 68J (50 ft-lb) are not presented, as there are no RPVs operating in Germany to which this criteria would apply within their design life.

6.7.3. Flaw assessment methods in France

A complete set of rules has been developed and published in RSEM [29, 159] including flaw geometry standards, fatigue crack growth and rupture analysis guidelines, fracture mechanics parameter evaluation guidelines, material properties, etc. All the acceptance criteria are based on elasto-plastic fracture mechanic methods with specific safety factors for brittle and ductile behaviour that are completely finalized. As an example, the proposed criteria for the end-of-life flaw are:

for Level A:	$T < RT_{NDT} + 50°C$	$K_{CP}\ (1.2C_A, 1.3a_f) < K_{IC}/1.5$	(71)
	$T > RT_{NDT} + 50°C$	$J\ (1.2C_A, 1.3a_f + \Delta a) < J_{\Delta a}/l.5$	
for Level C:	$T < RT_{NDT} + 50°C$	$K_{CP}\ (1.1C_C, a_f) < K_{IC}/1.4$	(72)
	$T > RT_{NDT} + 50°C$	$J\ (l..lC_C, a_f + \Delta a) < J_{\Delta a}/1.5$	
for Level D:	$T<RT_{NDT} + 50°C$	$K_{CP}\ (C_D, a_f) < K_{IC}/1.2$	(73)
	$T > RTNDT + 50°C$	$J\ (C_D, a_f + \Delta a) < J_{\Delta a}/l\ .2$	
	and a limited tearing crack growth or a crack arrest through the thickness of the vessel		

where C_A, C_C and C_D are the Level A, C and D loads; a_f is the end-of-life depth of the defect;

Δa is the stable tearing crack growth rate, K_{CP} is the elastic stress intensity factor plus plastic zone correction factor; and $J_{\Delta a}$ is the toughness from the J resistance curve of the material.

6.7.4. WWER flaw assessment methods

Flaw assessment method is also described in the VERLIFE Procedure [42], Appendix X-XII. Schematization of the defects found during the ISI and calculation of the stress intensity factors is performed in a manner which is similar to the Russian approach (Appendix X). Then, these sizes are compared with Tables of allowable defect sizes, given in Appendix XI. If flaws are larger than allowed in the Appendix XI, then their evaluation should be performed. Then, only linear elastic fracture mechanics methods are applied with safety factors identical to the ASME Code, i.e.

$\boldsymbol{n_a = 2}$ is applied to the depth a of schematised defect.

This defect is then extended by a value of potential crack growth as a result of repeated loading and corrosion environment, if any.

Crack growth rates as a result of repeated loading can be calculated using the following formulae:

Steels 15Kh2MFA and 15Kh2MFAA and their welding joints:

(a) *air:*

$$da/dN = 1.0 \times 10^{-21}\ [\Delta K_I]^{14.4} \qquad \text{for} \qquad \Delta K_I < 6.5\ \text{MPa.m}^{0.5} \qquad (74)$$

$= 1.7 \times 10^{-11} [\Delta K_I]^{2.66}$ for $\Delta K_I > 6.5$ MPa.m$^{0.5}$ (75)

(b) *water,* $R < 0.25$:

$da/dN = 8.1 \times 10^{-16} [\Delta K_I]^{4.13}$ for $\Delta K_I < 30$ MPa.m$^{0.5}$ (76)

$= 1.1 \times 10^{-11} [\Delta K_I]^{0.16}$ for $\Delta K_I > 30$ MPa.m$^{0.5}$ (77)

(c) *water,* $R > 0.65$:

$da/dN = 3.0 \times 10^{-14} [\Delta K_I]^{9.26}$ for $\Delta K_I < 7.4$ MPa.m$^{0.5}$ (78)

$= 6.0 \times 10^{-7} [\Delta K_I]^{0.85}$ for $\Delta K_I > 7.4$ MPa.m$^{0.5}$ (79)

Steels 15Kh2NMFA and 15Kh2NMFAA and their welding joints:

(a) *air:*

$da/dN = 1.0 \times 10^{-21} [\Delta K_I]^{14.4}$ for $\Delta K_I < 6.5$ MPa.m$^{0.5}$ (80)

$= 1.7 \times 10^{-11} [\Delta K_I]^{2.66}$ for $\Delta K_I > 6.5$ MPa.m$^{0.5}$ (81)

(b) *water,* $R < 0.25$:

$da/dN = 3.2 \times 10^{-22} [\Delta K_I]^{10.98}$ for $\Delta K_I < 18$ MPa.m$^{0.5}$ (82)

$= 3.0 \times 10^{-14} [\Delta K_I]^{4.62}$ for $\Delta K_I > 18$ MPa.m$^{0.5}$ (83)

(c) *water,* $R > 0.65$:

$da/dN = 2.1 \times 10^{-19} [\Delta K_I]^{11.61}$ for $\Delta K_I < 12$ MPa.m$^{0.5}$ (84)

$= 1.3 \times 10^{-10} [\Delta K_I]^{2.18}$ for $\Delta K_I > 12$ MPa.m$^{0.5}$ (85)

Austenitic steels of 08Kh18N10T type and their welding joints:

(a) *air:*

$da/dN = 1.0 \times 10^{-21} [\Delta K_I]^{14.4}$ for $\Delta K_I < 7.1$ MPa.m$^{0.5}$ (86)

$= 1.1 \times 10^{-10} [\Delta K_I]^{2.51}$ for $\Delta K_I > 7.1$ MPa.m$^{0.5}$ (87)

(b) *water,* $R < 0.25$:

$da/dN = 9.8 \times 10^{-21} [\Delta K_I]^{11.88}$ for $\Delta K_I < 14$ MPa.m$^{0.5}$ (88)

$= 4.7 \times 10^{-9} [\Delta K_I]^{1.68}$ for $\Delta K_I > 14$ MPa.m$^{0.5}$ (89)

Final sizes of such defect are then compared with postulated defects used in the assessment of RPV resistance against non-ductile failure. If such final size of the defect is smaller than the postulated one, then the defect is allowable. If its size is larger, it is necessary to perform the whole PTS calculations and evaluation but with this defect instead of postulated one.

The entire ASME, Section XI approach is used in Finland for their defect allowability evaluations, except they use the WWER material data.

6.7.5. Flaw assessment methods in Japan

NISA endorsed the JSME Code on Fitness-for-Services for Nuclear Power Plants, JSME S NA1-2002 [104] for flaw assessment. This JSME code is based on ASME Code Section XI.

7. AGEING MITIGATION METHODS

Section 4 of this report describes the age related degradation mechanisms that could impair the safety performance of an RPV during its service life. For four of these mechanisms (radiation embrittlement, fatigue, stress corrosion cracking and corrosion) mitigation methods are available to control the rate of ageing degradation and/or to correct the effects of these ageing mechanisms; thermal ageing and temper embrittlement are not addressed in this section since they are considered not to be significant.

7.1. RADIATION EMBRITTLEMENT

The radiation embrittlement can be mitigated by either flux reductions (operational methods aimed at managing ageing mechanism) or by thermal annealing of the RPV (maintenance method aimed at managing ageing effects). Flux reductions can be achieved by either fuel management or shielding the RPV from neutron exposure.

Managing ageing mechanism

7.1.1. Fuel management

The neutron flux (hence fluence) can be reduced by initiating a fuel management programme early in the life of a given plant. Such fuel management is carried out by implementing a low neutron leakage core (LLC). A LLC is a core that utilizes either spent fuel elements or dummy (stainless steel) fuel elements on the periphery of the core which reflect neutrons back into the core or absorb them rather than allowing them to bombard the RPV wall. LLCs can result in a reduction in power and/or increase in cost to the NPP owner.

Most of the western PWRs and all of the WWER plants have implemented LLC management programmes using spent fuel elements on the periphery of the core, but generally only after some period of operation. LLCs have been effective in reducing the re-embrittlement of the WWER-440/V-230 RPVs after thermal annealing.

A more drastic reduction of neutron flux can be achieved by inserting shielding dummy elements into the periphery of an active core, for example into the corners of the WWER active core hexagons. Dummy elements were inserted into most of the WWER- 440/V-230 reactors in the middle of the 1980s. Dummy fuel elements were also used in some of the WWER-440/V-213 plants with RPVs with relatively high impurity content (e.g. Loviisa, Rovno). 32 dummy elements are usually inserted into the core periphery. They cause not only a significant flux reduction but also a shifting of the maximum neutron flux by an angle of about 15° relative to both sides of the hexagon corners. Thus 12 new peak values of neutron flux are created on the pressure vessel wall. The original peak flux is decreased by a factor of 4.5 and the "new" peak flux is decreased by a factor of close to 2.5 — see Fig. 59. Thus, the cumulative effect of flux reduction must be calculated for both locations. Again, this method is most effective when applied during the first years of operation or just after a thermal annealing. The use of dummy elements usually results in a significantly different neutron balance in the core. The radial gradient is increased and thus the power distribution is disturbed in such a way that the peak power may exceed certain limits. Thus, a reduction in the fuel cycle length or a reduction of the reactor output are often necessary.

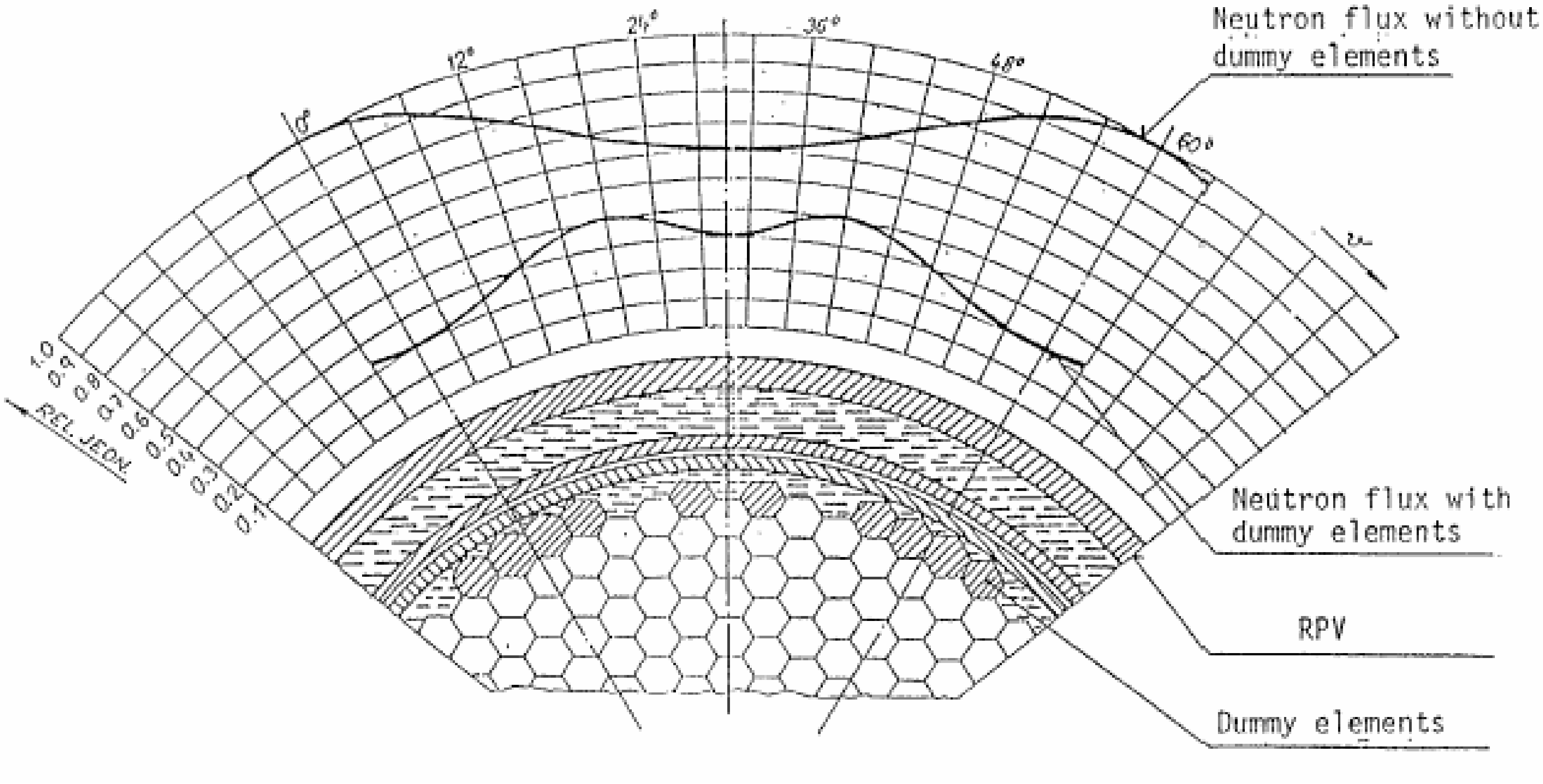


Fig. 59. WWER Flux distributions in low leakage cores.

7.1.2. RPV shielding

Flux (hence fluence) can also be reduced by further shielding the RPV wall from neutron bombardment. The reactor internals, the core barrel and thermal shield provides design basis shielding of the RPV. However, if it is judged that the design basis neutron exposure will result in significant radiation damage such that limitations are placed on the heating up and cooling down of the plant and/or accident conditions such as PTS becomes a potential safety issue, additional shielding is required. Shielding of the RPV wall from neutron exposure can be accomplished by increasing the thickness of the thermal pads that exist on the thermal shield at locations where the fiuence is high or by placing shielding on the RPV wall. There are a number of alloys or elements that can provide shielding of the RPV wall by absorbing the high energy neutrons. Probably, the most effective shielding material is tungsten. However, other materials such as Inconel, Zirconium oxide, stainless steel, Beryllium and Titanium Hydride should also be considered for RPV neutron shielding.

Managing ageing effects

7.1.3. Thermal annealing

Once a RPV is degraded by radiation embrittlement (e.g. significant increase in Charpy ductile-brittle transition temperature or reduction of fracture toughness), thermal annealing of the RPV is the only way to recover the RPV material toughness properties. Thermal annealing is a method by which the RPV (with all internals removed) is heated up to some temperature by use of an external heat source (electrical heaters, hot air), held for a given period and slowly cooled. The restoration of material toughness through post-irradiation thermal annealing treatment of RPVs has received considerable attention recently, due to the fact that a number of operating plants in the USA and elsewhere are approaching the PTS screening criteria during their normal license period, with several more approaching it during their license renewal period.

Experience in the USA

Thermal annealing is not without precedent; in the mid-1960s, the US Army SM-1A reactor reached a point where thermal annealing of the RPV was required after only a few years of operation because of sensitive material and a low operating temperature of 220°C (430°F). In the early 1980s, the Westinghouse Electric Corporation performed a study to examine the feasibility of thermal annealing of commercial RPVs and developed an optional, in situ, thermal annealing methodology that maximizes the fracture toughness recovery, minimizes re-exposure sensitivity and minimizes reactor downtime when thermal annealing becomes necessary. It was concluded from this study that excellent recovery of all properties could be achieved by annealing at a temperature of some 450°C (850°F) or higher for 168 hours. Such an annealing was predicted to result in a significant ductile-brittle transition temperature recovery. Further embrittlement under irradiation after the annealing was also predicted to continue at the rate that would have been expected had no annealing been performed. System limitations were identified for both wet and dry annealing methods. Several drawbacks were identified for the lower temperature wet thermal annealing that reduced its practicality. Therefore, a conceptual dry procedure was developed for thermal annealing embrittled RPVs. A follow-up study for EPRI showed that applying this procedure to two different plants resulted in acceptable stress, temperature and dimensions of the vessel and associated components.

The surveillance materials were irradiated to fluences up to 3 × 10^{23} n/m^2 (neutrons with energies less than 1 MeV) and at temperatures of about 290°C, which are typical of western RPVs. A good recovery of all of the mechanical properties was observed when the thermal annealing temperature was about 450°C for about 168 hours (1 week). And, the reembrittlement rates upon subsequent re-irradiation were similar to the embrittlement rates observed prior to the thermal anneal. The dominant factors which influence the degree of recovery of the properties of the irradiated RPV steels are the annealing temperature relative to the irradiation (service) temperature, the time at the annealing temperature, the impurity and alloying element levels, and the type of product (plate, forging, weldment, etc.) [161].

In 1986, the ASTM published a guide for in-service annealing of water cooled nuclear reactor vessels [162] which basically follows procedures developed by Westinghouse.

The USNRC has issued revisions to 10 CFR 50.61 and 10 CFR 50 Appendices G and H, new section 10 CFR 50.66 (the thermal annealing rule) and new Regulatory Guide 1.162 [163] to address RPV thermal annealing. The modification to 10 CFR 50.61 explicitly cites thermal annealing as a method for mitigating the effects of neutron irradiation, thereby reducing RT_{PTS}. The thermal annealing rule (10 CFR 50.66) addresses the critical engineering and metallurgical aspects of thermal annealing. The Regulatory Guide 1.162 on thermal annealing describes the format and content of the required report for thermal annealing.

10 CFR 50.66 requires a thermal annealing report which must be submitted at least three years prior to the proposed date of the annealing operation. The content of the report must include:

- Thermal annealing operating plan;
- An inspection and test programme to requalify the annealed RPV;
- A programme for demonstrating that the recovery of the fracture toughness and the reembrittlement rate are adequate to permit subsequent safe operation of the RPV for the period specified in the application; and
- A safety evaluation identifying any unreviewed safety questions and technical specification changes.

The thermal annealing operating plan will provide the following:

- Background on the plant operation and surveillance programme results;
- Description of the RPV, including dimensions and beltline materials;
- Description of the equipment, components and structures that could be affected by the annealing operation to demonstrate that these will not be degraded by the annealing operation;
- Results from thermal and stress analyses to establish time and temperature profiles of the vessel and attached piping, and to specify limiting conditions of temperature, stress and strain, and heatup and cooldown rates;
- Proposed specific annealing parameters, in particular the annealing temperature and time, and heatup and cooldown rates, and the bounding time and temperature

parameters that define the envelope of permissible annealing conditions to indicate conformance with the operating plan;

- Description of the methods, equipment, instrumentation and procedures proposed for the annealing operation;

- As low as reasonably achievable (ALARA) considerations for occupational exposure during the process; and

- Projected recovery and re-embrittlement trends for the RPV beltline materials.

Upon completion of the anneal and prior to restart of the NPP, licensee must certify to the NRC that the thermal annealing was performed in accordance with the approved application required by 10 CFR 50.66. The licensee's certification must establish the period for which the RPV will satisfy the requirements of 10 CFR 50.61 and Appendix G. The licensee must provide:

- The post-anneal RTNDT and Charpy USE values of the RPV materials for use in subsequent reactor operation;

- The projected re-embrittlement trends for both RT_{NDT} and Charpy USE; and

- The projected values of RT_{PTS} and Charpy USE at the end of the proposed period of operation addressed in the application.

If the licensee cannot certify that the thermal annealing was performed in accordance with the approved application, the licensee shall submit a justification for subsequent operation for approval by the USNRC.

In 1994, the US Department of Energy (DOE) initiated a programme to demonstrate the feasibility of thermal annealing western-type RPVs to temperatures of about 454° C (850°F) without causing structural damage to the vessel, piping, supports, or other major components of the NSSS. A team led by the Westinghouse Electric Corporation, and including ASME, EPRI, and certain US nuclear utilities, successfully performed a demonstration thermal annealing of the Marble Hill RPV as part of this programme. (Marble Hill is a Westinghouse type PWR which was nearly completed but never operated.)

WWER experience

A high radiation embrittlement rate was found in most of the WWER-440 /V-230 RPVs (and some of the WWER-440/V-213 RPVs, e.g. Loviisa) at a point of time which was to late to ensure the planned reactor lifetime, i.e. 30 years. The only mitigation method was found to be thermal annealing of the affected RPVs. Following the publication of the Westinghouse conceptual procedure for dry thermal annealing an embrittled RPV, the Russians (and recently, the Czechs) undertook the thermal annealing of several highly irradiated WWER-440 RPVs. To date, at least 15 vessel thermal annealings have been realized. The WWER experience, along with the results of relevant laboratory scale research with western RPV material irradiated in materials test reactors and material removed from commercial RPV surveillance programmes, are consistent and indicate that an annealing temperature at least 150°C more than the irradiation temperature is required for at least 100 to 168 hours to obtain a significant benefit. The selection of the temperature regime for annealing type 15Kh2MFA

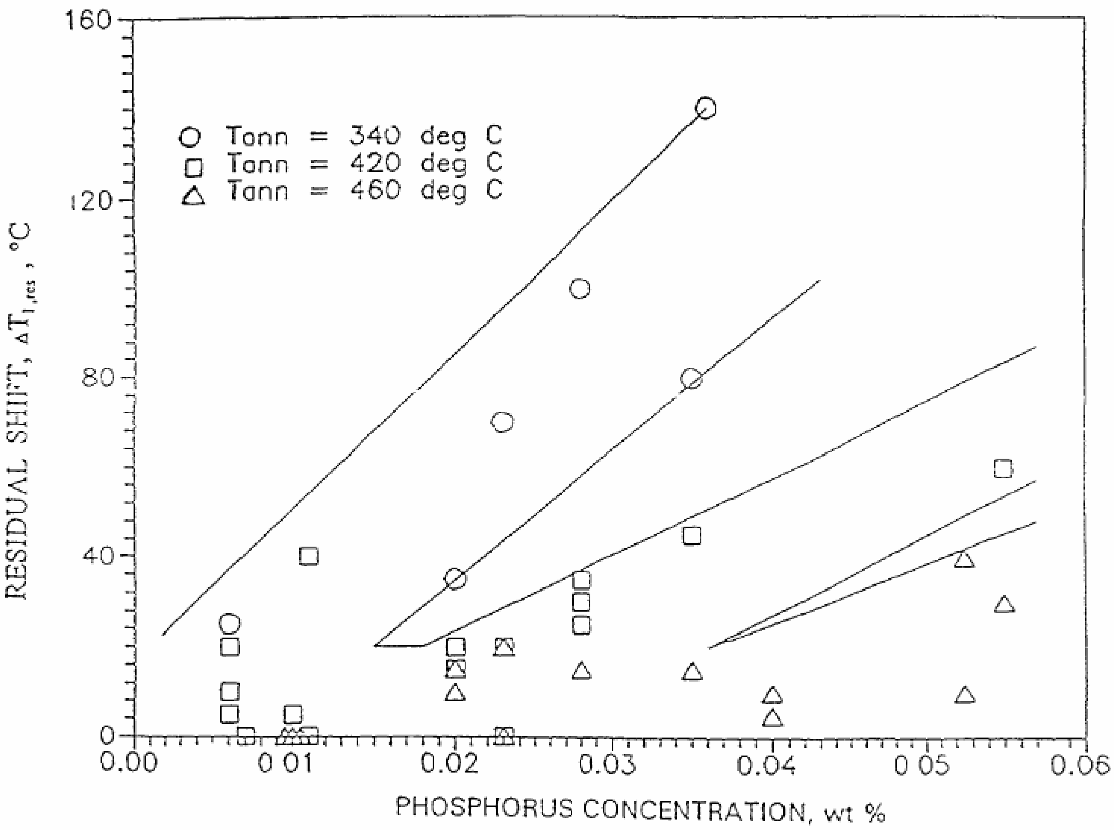


Fig. 60. Residual transition temperature shift as a function of phosphorus content in 15 Kh 2 MFA steel.

steel (and its weldments) was based on a large amount of experimental work, which has been done by the various organizations involved, considering:

- optimization of the recovery of the ductile to brittle transition temperature shift, and;
- evaluation of margins against the occurrence of temper embnttlement of base and weld metals.

An annealing regime with a temperature above 460°C (the latest version is 475°C) during a hold period of at least 10 hours (168 hours in previous annealings) results in acceptable mechanical property recovery and a residual embrittlement which does not depend on neutron fluence (in the studied range) but mainly on phosphorus concentration — see Fig 60. The data available, obtained both from radiation experiments as well as from templates cut-out directly from the vessels, indicate that the residual transition temperature shift, ΔT_{res} is below + 20°C for steels with less than 0.04 mass % phosphorus. It appears that for these steels a margin of 20°C conservatively covers the possible deviations. However, this cannot be claimed for material with larger phosphorus contents without further validation — see Fig 60.

An open question still remains concerning the so-called re-embrittlement rate, which is the rate of radiation embrittlement after annealing. Two main models are used conservative and lateral shifts, respectively. Many results show that after annealing at temperatures not lower than 425°C, this re-embrittlement rate is well characterized by a "lateral shift" as is shown in Fig 61.

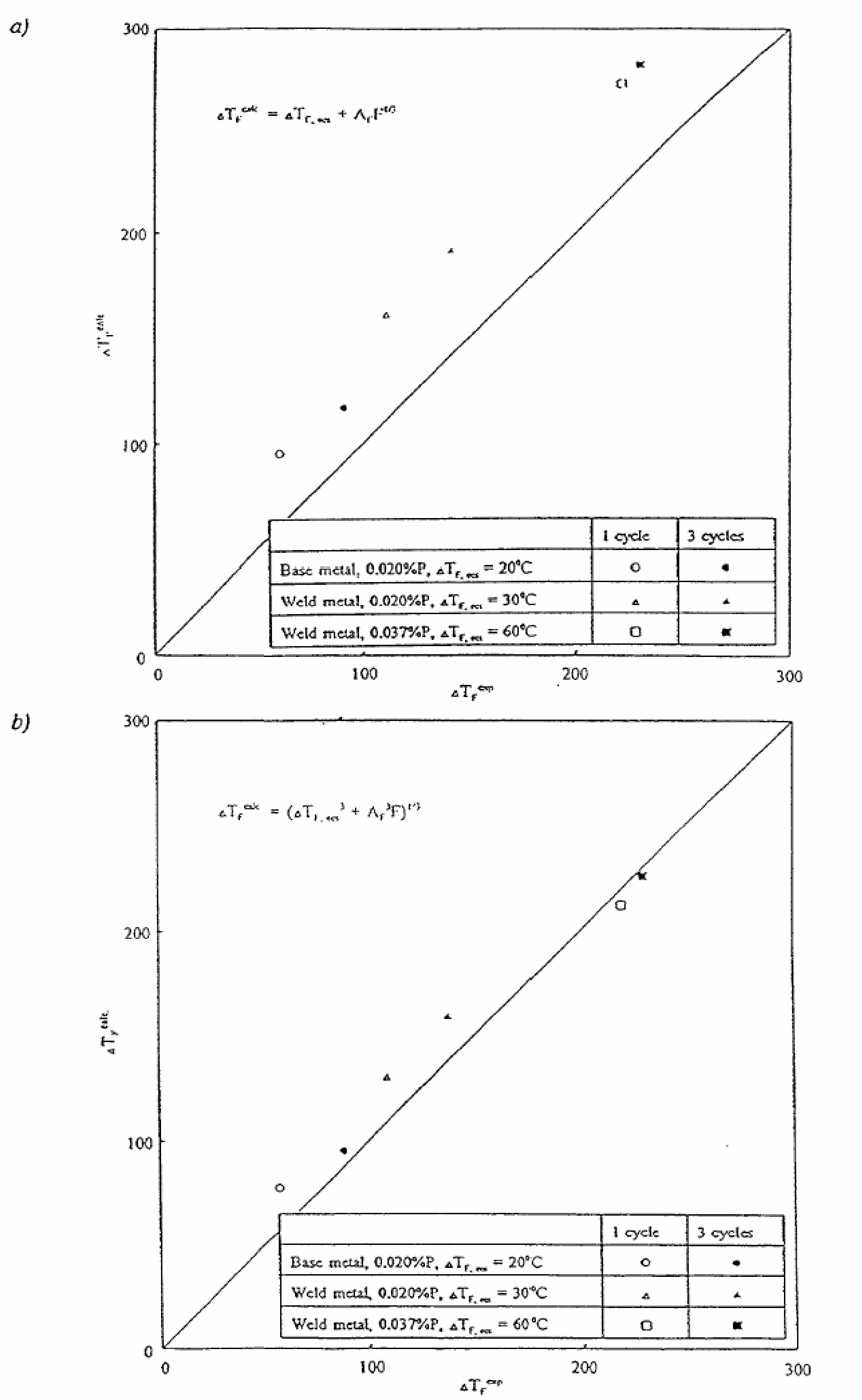


Fig. 61 Predicted vs. measured transition temperature shift due to re-irradiation up to three cycles (anneal-irradiate) for 15Kh2MFA base and -weld metal T_{Irr} = 260°C, T_{anneal} = 425°C. specimen size 5 × 5 × 27.5 mm. graph a) shows the conservative shift approach; graph b) shows the lateral shift approach.

The thermal annealing of a RPV requires the installation of monitoring and control devices and the development of procedures. The transition region at high temperatures has to be defined with respect to temperature limitations on specific components and limits on the secondary stresses in those components. A special annealing device, consisting of electrical heaters divided into sections, is inserted into the empty RPV. These heaters are controlled by thermocouples on the inner RPV wall and the required temperature gradient in the azimuthal as well as in the axial directions is achieved not only at the annealing temperature but also during the slow prescribed cooldown rate. A mock-up experiment on a model RPV of real dimensions is sometimes necessary, for example such an experiment was conducted at the ŠKODA plant as a necessary step before annealing the RPVs at Bohunice and more recently at Loviisa.

Depending on the presence of cladding (which limits access to the inner surface), two ways exist for determining the residual transition temperature shift after annealing, either:

- evaluation of delta T_{res} as a function of phosphorus content according to the existing database. In this case the knowledge of the chemical composition and T_{k0} is of importance, or;

- evaluation of T_k after annealing by testing subsize Charpy specimens from templates cut from the inner surface. In this case, the use of a correlation between subsize and standard specimens results is required.

Both methods have been used, but uncertainties remain.

The large uncertainties, considerable data scatter and lack of data on material irradiated at conditions close to that of the vessel wall could be resolved by further investigations on decommissioned RPVs. The methodology for the Novovoronezh Unit 2 plant and others could be complemented by investigations on the shutdown Greifswald plant, which is typical of other WWER-440/V-230 plants in terms of operating conditions and material sensitivity to radiation embrittlement.

Instrumented hardness measurements on the cladding are recommended (they are realized in Bohunice and Dukovany plants as a part of LSI) for the evaluation of mechanical properties of the cladding. Instrumented hardness measurement at the outer surface cannot lead to an accurate assessment due to material uncertainties as discussed above.

7.2. STRESS CORROSION CRACKING OF CRDM PENETRATIONS

7.2.1. Coolant additives

The most promising coolant additive is zinc, which as been shown to reduce the radiation activity of the primary coolant as well as increase the resistance of Alloy 600 material to PWSCC. The zinc interacts with chromium in the oxide film on the Alloy 600 components and forms a more protective (stable) oxide coating, which delays initiation of PWSCC [164]. With the addition of 20 ppb of zinc, the PWSCC initiation time for Alloy 600 reverse U-bend specimens is increased by a factor of 2.8, and, with 120 ppb of zinc, the initiation time is increased by a factor of 10 [165]. With the addition of 20 ppb of zinc and a crack-tip stress intensity in the range of 40 to 50 MPa$\sqrt{m}$ (36 to 45 ksi$\sqrt{inch}$) the PWSCC crack growth rates are reduced by about a factor of 3.3. EPRI and the Westinghouse Owners' Group implemented zinc addition in June 1994 at Farley Unit 2 for field demonstration. The duration of this demonstration is about 39 months [166]. The zinc is being added in the form of zinc acetate, which has a high solubility in the PWR coolant at operating temperature. Adding zinc is expected to mitigate PWSCC in both new and old plants. However, it may take longer for zinc to be incorporated into the oxide film present in an older plant because the film is likely to be thicker and more stable.

By the end of August 2003, there were 14 plants through out the world operating with zinc addition to the primary coolant water. Table 35 identifies the 14 plants operating with zinc addition.

TABLE 35. PWR PLANTS OPERATING WITH ZINC ADDITIVE TO COOLANT WATER

Farley Unit 2 (10)*
Farley Unit 1 (16)
Diablo Canyon Unit 1 (9)
Diablo Canyon Unit 2 (9)
Palisades (14)
Brigham (28)
Bibles A (19)
Bibles B (17)
Angora Unit 2 (1)
Sequoia Unit 1 (12)
Sequoia Unit 2 (11)
Beaver Valley Unit 1 (15)
Callaway (13)
Ft. Calhoun (21)

*(first cycle of zinc addition)

7.2.2. Reduced upper head temperatures

The reactor upper head temperatures can be lowered somewhat by making minor modifications to the internals of certain RPVs to increase the bypass flow. This has been tried in France, but the results were not entirely satisfactory in terms of crack growth rate to ensure 40 years of safe operation. Fig. 54 of paragraph 6.4.2 compares results of similar plants with cold or hot dome with different operational temperatures.

In Japan, the coolant temperature of the vessel head was lowered to the cold-leg temperature by increasing the bypass flow to the vessel head in 11 PWRs, which had enough margin in the primary coolant flow rate so as to increase the bypass flow to the vessel head.

7.2.3. Surface treatments

There are several different inside surface treatments being considered for mitigating Alloy 600 CRDM nozzle cracking, including special grinding, nickel plating and peening. Grinding techniques are being developed in France and Japan to remove the surface layer where cracks might have initiated, but remain undetected, and then produce compressive stresses on the regenerated surface [167]. Nickel plating can protect the treated surfaces from the PWR coolant, stop existing cracks from propagating and repair small cracks. Nickel plating has been qualified for steam generator tubes and has been applied to about 1100 tubes in Belgium and Sweden in the last 8 years. Al of these tubes, except for the first few, are still in service, whereas unplanted sister tubes are degrading [168]. The nickel plating does not provide structural strength for the CRDM nozzle. Peening with shot or other methods replaces high tensile residual stresses on the surface with compressive stresses. It has been used to prevent PWSCC initiation in steam generator tubes. However, shot peening is not effective if cracks already exist.

7.2.4. Stress improvement methods

Porowski et al. [169] have proposed a mechanical stress improvement method that redistributes the residual stresses in the nozzle and produces a layer of compressive stresses on the inside surface of the nozzle. The method consists of applying a compressive axial load at the nozzle ends, which are accessible. Analysis of the application of this method shows that the imposed axial compressive stresses interact with the residual tensile stresses on the inside surface, and the resulting plastic flow removes the residual tensile stresses from the sites on the nozzle inside surface near the partial penetration weld. The analysis results also show that the residual stresses on the inside surface are reduced, and the surface becomes near stress-free after removal of the applied axial load. This method has not been implemented on CRDM but can be considered. It was implemented only some dissimilar metal welds.

7.2.5. Alloy 600 head penetration repairs

Two options exist for the repair of Alloy 600 RPV head penetrations which contain SCCs. The first method involves grinding out the SCC and filling the resulting cavity with a suitable weld metal. The welding process should be such that residual stresses are minimized. Following the welding process, grinding is again performed to contour the surface of the weld repair to that of the head penetration. The weld filling material is usually Alloy 182.

The second method to repair head penetrations with stress corrosion cracks is to insert a thin liner (tube) of thermally treated Alloy 690 (TT) or austenitic stainless steel into the degraded head penetration. The head penetration in question is then pressurized and the liner will expand onto the head penetration tube and seal the crack.

EDF and FRAMATOME have developed some repair processes like grinding of the inner surface, with and without repair by welding. Repair by cutting and replacement of part of the CRDM nozzle has been studied. None of them has been used for the moment in France due to dosimetry, the number of nozzles concerned and difficulties in repairing the downhill nozzles.

7.2.6 Head penetration replacement

Head penetration replacement can take the form of either replacing the RPV closure head with a new closure head or replacing each head penetration; the new head penetration should be made from material other than Alloy 600. In several plants, where replacement of existing RPV closure heads has occurred, thermally treated Alloy 690 has been chosen as the material of construction for penetrations in replacing Alloy 600. Test results and limited field experience associated with other Alloy 690 components exposed to PWR primary coolant indicate that Alloy 690 material is not susceptible to PWSCC damage. In addition, new weld materials, Alloy 52 and 152, have been used in place of Alloy 82 and 182. The new materials have better resistance to PWSCC.

Based upon the fact that a large number of reactor pressure vessel head penetrations were exhibiting PWSCC, 29 plants in the USA have elected to replace reactor vessel heads rather than repair the damage penetrations (White el al). Table 36 presents the status as of September 2003 of reactor pressure vessel head replacement in the USA. Recognizing that Alloy 600 was susceptible to PWSCC, the replacement reactor pressure vessel heads utilized Alloy 690 instead of Alloy 600. Alloy 690 is considered to be less susceptible to PWSCC than Alloy 600. In the USA, over fifty percent of the operating PWR utilize Alloy 690 for head penetrations.

EDF has decided to replace all the reactor vessel heads of the 50 plants concerned for economical reasons: cost of inspection, cost and difficulties of repair, uncertainties on the shutdown time of plant affected by degradation. To-day 41 reactor vessel heads has been replaced with Alloy 690 penetrations. 9 others will be replaced in the next six to eight years in order to finish all the replacements before 2010.

Currently there are 23 operating PWR plants in Japan. Of these twenty-three PWR plants, 22 of the plants had RPV heads with CRDM with Alloy 600 thermal treated (TT-600) and one plant has that with Alloy 690 thermal treated 690 (TT-690). Japanese PWR utilities addressed the issue of RPV head penetration cracking in two ways. The first way is to lower the coolant temperature in the RPV head for eleven of the 22 plants. The margin in primary coolant flow rate was too small to increase the bypass flow to the RPV head in 11 plants to lower the temperature in the head. The RPV heads for these 11 plants were replaced with new heads that utilized Alloy 690 thermal treated (690-TT).

TABLE 36 (WHITE ET AL.) ANNOUNCED HEAD REPLACEMENT PLANS AS OF SEPTEMBER 2003

Status	***Year***	***No.***	***Plant***
Already Replaced	2002	1	Davis-Besse
		2	North Anna Unit 2
		3	North Anna Unit 2
		4	Oconee Unit 3
		5	Surry Unit 1
Replacing Next Refueling Outage	2003	6	Crystal River Unit 3
		7	Ginna
		8	Oconee Unit 1
		9	Surry Unit 2
		10	TMI Unit 1
	2004	11	Oconee Unit 2
		12	Farley Unit 1
		13	Kewaunee
		14	Turkey Point Unit 3
	2005	15	Millstone Unit 2
		16	Point Beach Unit 2
		17	Turkey Point Unit 4
		18	ANO Unit 1
		19	Farley Unit 2
		20	Point Beach Unit 1
		21	H.B. Robinson Unit 2
	2006	22	St. Lucie Unit 2
		23	Beaver Valley Unit 1
		24	Calvert Cliffs Unit 1
		25	St. Lucie Unit 1
		26	Cook Unit 1
		27	Fort Calhoun
	2007	28	Calvert Cliffs Unit 2
		29	Cook Unit 2

8. REACTOR PRESSURE VESSEL AGEING MANAGEMENT PROGRAMME

A systematic reactor pressure vessel ageing management programme is needed at all nuclear power plants to ensure the integrity of RPVs throughout their service life. IAEA-TECDOC-1120 issued in 1999 identified radiation embrittlement as the only safety significant ageing mechanism of concern for both the Western design reactor pressure vessels and the WWER reactor pressure vessels. Operating experience accumulated since 1999 has shown that PWSCC of Alloy 600 vessel penetrations, safe-ends and dissimilar metal welds and the associated boric acid corrosion of low alloy vessel heads are also safety significant ageing mechanisms as well as economic concerns for Western design RPVs. In addition, environmental fatigue has been identified in Japan and USA as a safety significant ageing mechanism that should be evaluated in connection with long term operation/ life extension of NPPs. Other age-related mechanisms such as thermal ageing and temper embrittlement of the reactor pressure vessel materials, while not considered safety significant by themselves, can increase the safety significance of the radiation embrittlement of both the Western and WWER reactor pressure vessels. Notable developments since the first issue of the TECDOC-1120 include also implementation of the “Master Curve” methodology for safety assessment/ prediction of fracture toughness of WWER RPVs. The preceding sections of this report dealt with important elements of an RPV ageing management programme whose objective is to maintain the integrity of the RPV at an NPP throughout its service life. This section describes how these elements are integrated within a plant specific RPV ageing management programme utilizing a systematic ageing management process which is an adaptation of Deming’s Plan-Do-Check-Act cycle to ageing management (Fig.62); it includes new ageing management actions developed in response to significant operating events that have occurred between 1995 and 2006. Such an ageing management programme should be implemented in accordance with guidance prepared by an interdisciplinary RPV ageing management team organized at a corporate or owners group level. For guidance on the organizational aspects of a plant ageing management programme and interdisciplinary ageing management teams refer to IAEA Safety Report “Implementation and Review of Nuclear Power Plant Ageing Management Programme.”

A comprehensive understanding of an RPV, its ageing degradation, and the effects of the degradation on the ability of the RPV to perform its design *and safety* functions is the fundamental basis of an ageing management programme. This understanding is derived from a knowledge of the design basis (including applicable codes, and regulatory requirements); the design and fabrication (including the materials properties and specified service conditions); the operation and maintenance history (including commissioning and surveillance); the inspection results; and generic operating experience and research results. Sections 1.1, 2, 3 and 4 contain information on important aspects of the understanding of RPVs and their ageing.

In order to maintain the integrity of an RPV, it is necessary to control within defined limits the age-related degradations of the RPV. Effective ageing degradation control is achieved through the systematic ageing management process consisting of the following ageing management tasks, based on understanding of RPV ageing:

- operation within operating guidelines aimed at minimizing the rate of degradation - *managing ageing mechanisms* (Sections 8.1.3 and 7);

- inspection and monitoring consistent with requirements aimed at timely detection and characterization of any degradation (Section 5);

- assessment of the observed degradation in accordance with appropriate guidelines to determine integrity (Section 6) and;

- maintenance (repair or parts replacement) to correct unacceptable degradation - *managing ageing effects or a good time frame* (Section 7).

An RPV ageing management programme co-ordinates programmes and activities contributing to the above ageing management tasks in order to detect and mitigate ageing degradation before the RPV safety margins are compromised. This programme reflects the level of understanding of the RPV ageing, the available technology, the regulatory/licensing requirements, and plant life management considerations/objectives. Timely feedback of experience is essential in order to provide for ongoing improvement in the understanding of the RPV ageing degradation and in the effectiveness of the ageing management programme. The main features of an RPV ageing management programme, including the role and interfaces of relevant programmes and activities in the ageing management process, are shown in Fig.62, and discussed in Section 8.1 below. Application guidance is provided in Section 8.2.

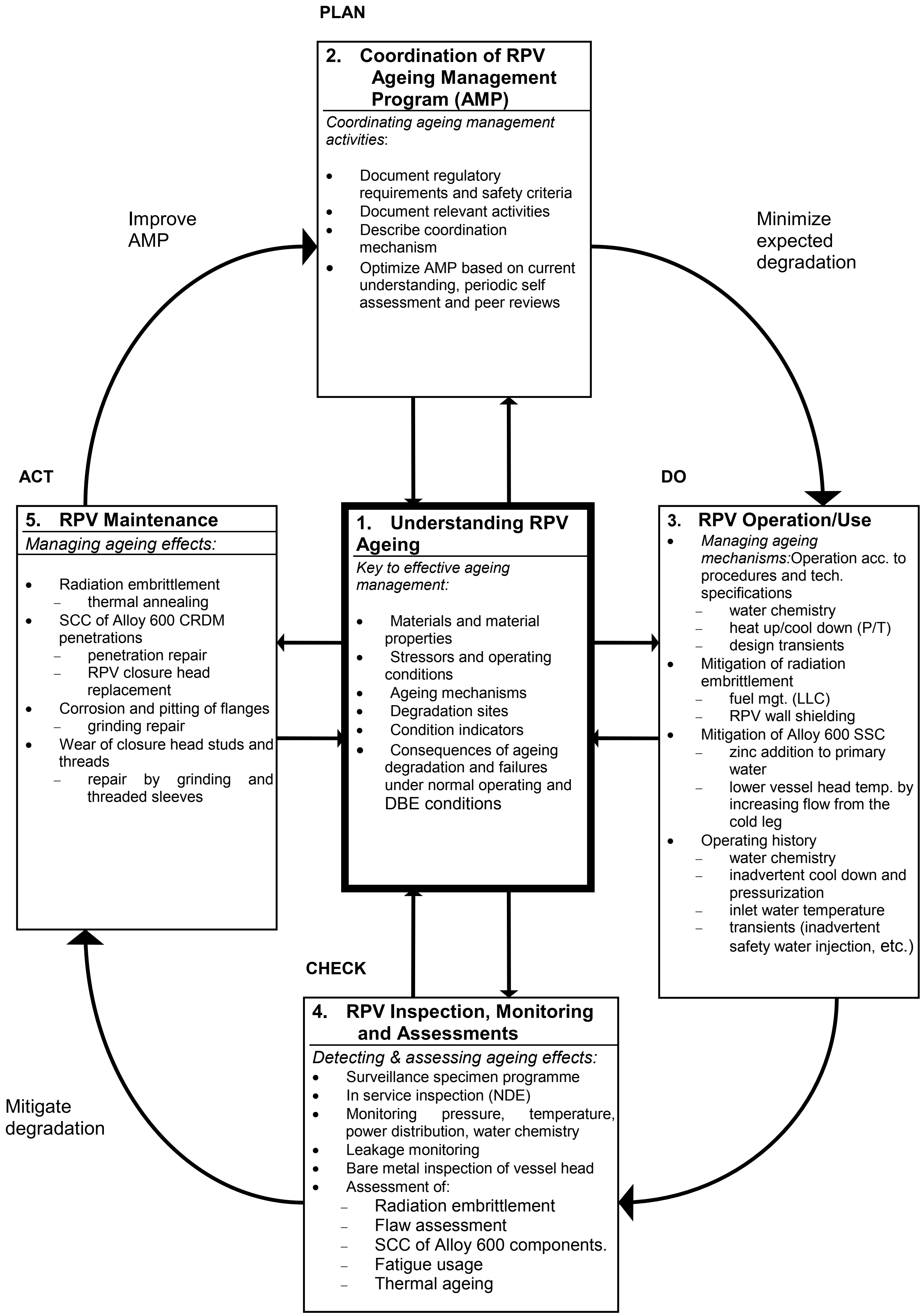


Fig. 62. Key elements of a PWR Pressure Vessel Ageing Management Programme utilizing the systematic ageing management process.

8.1. KEY ELEMENTS OF RPV AGEING MANAGEMENT PROGRAMME

8.1.1 Understanding RPV ageing

Understanding RPV ageing is the key to effective management of RPV ageing, i.e., it is the key to: coordinating ageing management activities within a systematic ageing management programme, managing ageing mechanisms through prudent operating procedures and practices (in accordance with procedures and technical specifications); detecting and assessing ageing effects through effective inspection, monitoring, and assessment methods; and managing ageing effects using proven maintenance methods. This understanding consists of: a knowledge of RPV materials and material properties; stressors and operating conditions; likely degradation sites and ageing mechanisms; condition indicators and data needed for assessment and management of RPV ageing; and effects of ageing on safety margins.

The understanding of RPV ageing is derived from the RPV baseline data, the operating and maintenance histories, and external experiences. This understanding should be updated on an ongoing basis to provide a sound basis for the improvement of the ageing management programme consistent with operating, inspection, monitoring, assessment and maintenance methods and practices.

The RPV baseline data consists of the performance requirements, the design basis (including codes, standards, regulatory requirements), the original design, the manufacturer's data (including materials data), and the commissioning data (including inaugural inspection data). The RPV operating history includes the pressure-temperature records, system chemistry records, records on material radiation embrittlement from the surveillance programme, and the ISI results. The RPV maintenance history includes the inspection records and assessment reports, design modifications, and type and timing of maintenance performed. Retrievable up-to-date records of this information are needed for making comparisons with applicable external experience.

External experience consists of the operating and maintenance experience of (a) RPVs of similar design, materials of construction, and fabrication; (b) RPVs operated with similar operating histories, even if the RPV designs are different; and (c) relevant research results. It should be noted that effective comparisons or correlations with external experience require a detailed knowledge of the RPV design and operation.

External experience can also be used when considering the most appropriate inspection method, maintenance procedure and technology.

8.1.2 Coordination of RPV ageing management programme

Existing programmes relating to the management of RPV ageing include operations, surveillance and maintenance programmes as well as operating experience feedback, research and development and technical support programmes. Experience shows that ageing management effectiveness can be improved by coordinating relevant programmes and activities within an ageing management programme utilizing the systematic ageing management process. Safety authorities increasingly require licensees to implement such ageing management programmes for selected SSCs important to safety. The co-ordination of an RPV ageing management programme includes the documentation of applicable regulatory requirements and safety criteria, and of relevant programmes and activities and their respective roles in the ageing management process as well as a description of mechanisms used for programme coordination and continuous improvement. The continuous ageing management programme improvement or optimization is based on current understanding of RPV ageing and on results of periodic self-assessments and peer reviews.

8.1.3 RPV operation

NPP operation has a significant influence on the rate of degradation of plant systems, structures and components. Exposure of RPV to operating conditions (e.g. temperature, pressure, fast neutron dose rate, water chemistry) outside prescribed operational limits could lead to accelerated ageing and premature degradation. Since operating practices influence RPV operating conditions, NPP operations staff has an important role within the ageing management programme to minimize age related degradation of the RPV by maintaining operating conditions within operational limits that are prescribed to avoid accelerated ageing of RPV components during operation. Examples of such operating practices are:

- fuel loading scheme to control the rate of radiation embrittlement,
- operation within the prescribed pressure and temperature range during start-up and shut-down to avoid a risk of over-pressure relating to the material fracture toughness,
- lower temperature of the vessel head by increasing the bypass flow from the cold leg to the vessel head if there is a sufficient margin in the primary coolant flow rate (see Sec. 7.2.2),
- Zinc addition to mitigate PWSCC of Alloy 600 components,
- defining appropriate operator actions for the case of a possible PTS event to avoid critical transients,
- performing maintenance according to procedures designed to avoid contamination of RPV components with boric acid or other reagents containing halogens,
- on-line monitoring and record keeping of operational data necessary for predicting ageing degradation and defining appropriate ageing management actions.

Operation and maintenance in accordance with procedures of plant systems that influence RPV operational conditions (not only the primary system but also the auxiliary systems like water purification and injection systems), including the testing of the RPV and its components, and record keeping of operational data (incl. transients) are essential for an effective ageing management of the RPV and a possible plant life extension. Specific operational actions used to manage RPV-significant ageing mechanisms are described in Section 7.

8.1.4 RPV inspection, monitoring and assessment

Inspection and monitoring

The RPV inspection and monitoring activities are designed to detect and characterize significant component degradation before the RPV safety margins are compromised. Together with an understanding of the RPV ageing degradation, the results of the RPV inspections provide a basis for decisions regarding the type and timing of maintenance actions and decisions regarding changes in operating conditions to manage detected ageing effects.

Current inspection and monitoring requirements and techniques for RPVs are described in Section 5. Inspection and monitoring of RPV degradation falls in two categories: (1) inservice inspection on different locations of the RPV and surveillance capsule testing, and (2) monitoring of pressures and temperatures, water chemistry, transients (relative to fatigue), RPV leakage, and power distributions. Results of the ISI are used for flaw tolerance assessments while the surveillance capsule test results are used as input for the assessment of the radiation embrittlement. Monitoring of the power distributions provides input to the calculation of the RPV fluence from the neutron dosimeters encapsulated in the surveillance capsules. Monitoring temperature and pressure also provides input for the assessment of radiation embrittlement. Transient monitoring provides realistic values of thermal stresses as opposed to design basis thermal stress values for fatigue assessments. Finally, monitoring for leakage provides for the recognition of potential PTS transients or CRDM leakage.

It is important to know the accuracy, sensitivity, reliability and adequacy of the non-destructive methods used for the particular type of suspected degradation. The performance of the inspection methods must be demonstrated in order to rely on the results, particularly in cases where the results are used in integrity assessments. Inspection methods capable of detecting and sizing expected degradation are therefore selected from those proven by relevant operating experience.

All locations where Alloy 600 or Alloys 82 and 182 are utilized, especially locations that are not stress relieved, should have attention.

Integrity assessment

The main safety function of an RPV is to act as a barrier between the radioactive primary side and the non-radioactive outside environment. Safety margins are part of the design and licensing requirements of a NPP to ensure the integrity of the RPV under both normal and accident conditions. An integrity assessment is used to assess the capability of the RPV to perform the required safety function, within the specified margins of safety, during the entire operating interval until the next scheduled inspection.

Integrity assessments have used a variety of methods in response to the particular conditions and circumstances present at the time of the assessment. Section 6 of this report describes the assessment methods used, including a Master Curve methodology which has been utilized as an alternative for WWER RPVs. Included in the RPV integrity assessments are radiation damage trend curves for comparison with surveillance capsule test results to assess radiation embrittlement and utilization of the ISI results along with fatigue crack growth models and fracture mechanics technologies to assess the flaw tolerance of the RPV. In addition, assessments are required of other potential ageing related degradations that may have both safety and economic impact on the ageing management programme. These include assessment of the fatigue usage factors utilizing information/data from the on-line transient monitoring system, assessments of the stress corrosion cracking susceptibility of the Alloy 600 components (including bottom mounted instrumentation nozzles and nozzle to safe end welds), environmental fatigue assessments relating to NPP long term operation/life extension, and thermal ageing assessments.

8.1.5 RPV maintenance

Maintenance actions that can be used to manage ageing effects detected by inspection and monitoring methods in different parts of an RPV are described in Section 7. Decisions on the type and timing of the maintenance actions are based on an assessment of the observed ageing effects, available decision criteria, and understanding of the applicable ageing mechanism(s), and the effectiveness of available maintenance technologies.

During an outage in which maintenance of the RPV is scheduled there are various options for addressing materials degradation due to radiation:

- fuel management to decrease the rate of fluence on the RPV,
- installing shielding to either the reactor vessel wall or neutron pads, and
- thermal annealing to recover the RPV materials fracture toughness.

Based upon the NDE of the top and bottom closure heads, a number of options are available for addressing PWSCC:

- replacement of the top closure head with Alloy 690 instead of Alloy 600 penetrations
- repair of small flaws/cracks
- replacement of CRDM penetrations utilizing Alloy 690 instead of Alloy 600.

If flaws/cracks are observed during NDE of the bottom head, the penetrations can be repaired or replaced.

Testing of the RPV surveillance capsules should be carried out in accordance with the removal schedule given in the RPV Technical Specifications. Results from the testing of the surveillance capsule specimens will provide guidance as to the implementation of the actions provided above.

Maintenance of the surfaces of the closure flanges may be required if corrosion or pitting occurs due to damaged O-rings. If corrosion or pitting is observed, the surfaces of the closure flanges may be repaired by grinding off any corrosion products or pitting.

Wear of the closure head studs and threads is also occasionally observed. The degradation of the closure studs and threads by wear requires that the closure holes be machined out and new threaded sleeves be inserted into the stud holes. The maintenance of the closure head studs and threads should be scheduled based on previous inspections for wear.

A re-insertion of complementary capsules will be an important action to follow radiation embrittlement of material over the initial design life of 40 years to the expected life of 60 years.

8.2. APPLICATION GUIDANCE

The RPV ageing management programme should address both safety and reliability/ economic aspects of RPV ageing to ensure both the integrity and serviceability of the RPV during its design life and any extended life. The following subsections provide guidance on dealing with the relevant age related degradation mechanisms.

8.2.1 Reactor pressure vessel radiation embrittlement

Radiation embrittlement of the RPV is a safety concern. All RPV materials are radiation embrittlement sensitive to some degree. The ageing management programme activities which address radiation embrittlement can be identified as follows.

(a) Utilization of the radiation embrittlement databases/trend curves, to predict the degree of radiation embrittlement for a given RPV.

(b) Most, if not all of the operating plants today have a RPV Materials Surveillance Programme where RPV materials surveillance capsules are inserted into the vessel. The Western RPV surveillance capsules are located at the beltline regions of the RPVs, thereby providing a monitoring of the radiation sensitivity of the RPV materials. Some WWER surveillance capsules are located outside of the beltline region, and therefore, require methodology to assess the radiation sensitivity of beltline materials from the data obtained outside of the beltline region. The WWER-440 Type 230 plants were not supplied with a RPV material surveillance programme. Also, a few Western RPVs depend on sister plants for their material surveillance data.

(c) Low leakage core (LLC) fuel management programme to reduce the rate of radiation embrittlement

(d) Additional RPV wall shielding to reduce the rate of radiation embrittlement.

(e) Application of thermal annealing of a reactor pressure vessel fabricated from radiation sensitive material is always an option. If the reactor pressure vessel materials are highly sensitive to radiation damage, the ageing management programme should evaluate the response of the surveillance capsule materials to thermal annealing and develop a plan for thermal annealing the reactor pressure vessel.

8.2.2 Stress corrosion cracking of Alloy-600 components

The ageing degradation of the Alloy-600 RPV penetrations, especially the stress corrosion cracking of the CRDM penetrations discussed in Section 4.5.1, is both a safety and an economic concern. Therefore, the ageing management programme should address this issue from both an economic and safety perspective. The ageing management programme should include:

(a) An ISI programme for the Alloy-600 penetrations to ensure timely detection of any Alloy-600 penetration cracking. Such a programme should include NDE (UT, eddy current, or dye penetrant testing) appropriate to the susceptibility of a specific RPV head to PWSCC as well as bare metal visual examination of 100% of the head surface. RPV heads of high and medium susceptibility should be inspected during every refuelling outage. (For more details see Section 5.1 and 5.2.)

(b) A flaw evaluation handbook should be prepared if reportable indications are found that exceed the given acceptance criteria identified by the ASME Code or other governing regulatory agency; or plant-specific criteria should be developed and documented in a flaw evaluation handbook to determine if continued operation is acceptable or repair or replacement is warranted.

(c) The ageing management programme should also have in place repair procedures and/or contingency plans for reactor pressure vessel head replacement.

A similar process has been developed for all other alloy 600 locations, e.g. BMI, safe end welds and radial keys.

8.2.3 Thermal ageing of reactor pressure vessel materials

As discussed in Section 4.2, thermal ageing of the RPV material is not considered to be a safety or economic concern since the available published data does not indicate a large increase in the NDTT or RT_{NDT}. However, even a relatively small increase in the NDTT or RT_{NDT} of 20°C may in combination with the irradiation damage at the beltline region of the RPV (measured by the radiation damage surveillance programme), it can be a safety issue if a flaw is located in a region outside of the beltline region and can be affected by life extension to 60 years. Therefore, the ageing management programme should address thermal ageing as follows:

(a) ISI of the regions outside the RPV beltline weld should be periodically carried out to ensure timely detection of any flaws. Current inspection requirements only require that weldments and a limited distance in the base metal be inspected. However, the critical location for flaw instability may not be in the region that is covered by the ISI. Therefore, the activities discussed below should be implemented.

(b) If a flaw (reportable indication exceeding ASME allowable) is detected during ISI, as discussed above, the flaw should be evaluated in accordance with the ASME Section XI Code or the prevailing Code (like ASTM) or Regulatory Rules of the given country. For flaws detected within the RPV beltline region, the effect of thermal ageing is accounted for in the results from the post-irradiation testing of surveillance capsule specimens (not necessary for 60 years of operation). For flaws detected in regions outside the RPV beltline, the effect of thermal ageing must be taken into consideration. An estimate of the increasing RT_{NDT} due to thermal ageing should be made from published data for the material of interest or if the given RPV surveillance programme contains thermal ageing specimens outside the RPV, the results from the testing of these specimens should be taken into consideration. The increase in RT_{NDT} from the above methods must be included in the required fracture mechanics assessment of any flaws detected during the ISI.

8.2.4 Fatigue

The assessment in the ageing management programme of fatigue crack initiation caused by cyclic loadings should be carried out by either the use of delta stress (S) versus number of cycles (N) curves given in the ASME Section III B3000 rules or similar curves in the given country's code or regulatory rules. If a flaw is detected during ISI and the sizing is such that a fracture mechanics analysis is required, fatigue crack growth of the flaw must be considered according to the ASME code or the JSME code.

(a) Analytical Method — Miner's Rule is an analytical method which can be used to assess the possibility of fatigue crack initiation in RPVs. ASME Section III, NB-3222.4, specifies the use of Miner's Rule for calculating fatigue damage in structural components, as do the codes in a number of other countries. The use of Miner's Rule requires that the cyclic stresses and the number of cycles are known. The cyclic stresses and number of cycles are given in the RPV stress report. These values are determined from the NSSS vendor estimate of the type and number of transients. Use of Miner's Rule results in the determination of a cumulative usage factor, U, which is the total number of expected cycles at a given stress level divided by the allowable number of cycles at that stress level. The allowable number of cycles at any stress level can be determined from the stress versus number of cycles (S/N) design curve for the material of interest in the code. When more than one stress level is expected (which is usually the case), the cumulative usage factor is the summation of the ratio at each stress level. The cumulative usage factor shall not exceed 1.0 for any part of the RPV, and cumulative usage factors should be calculated for all the key components of the RPV including the closure head, nozzles, penetrations, studs, and beltline region. In connection with NPP long term operation/ life extension, licensees in Japan and USA are required to perform environmental fatigue evaluation to determine the reduction in fatigue life resulting from high temperature primary coolant environment.

(b) Transient Monitoring — The NSSS vendors' input to the stress report as to the number and type of transients can be overly conservative. Transient monitoring can be used to obtain more accurate estimates of both the total number of cycles and the stress ranges. For RPVs that went into operation prior to installing a transient monitoring system, a review of past operating records must be made to determine the number and type of transients prior to the installation of the monitors. Transient monitoring systems are a very valuable tool in determining the life of a RPV and should be part of the ageing management programme.

(c) Evaluation of ISI Results — As discussed in Sections 3, 5 and 6 of this document, each country has specific ISI requirements. If a flaw is detected in the RPV during ISI and if the size of the flaw requires that a fracture mechanics analysis be performed to demonstrate the integrity of the component, then a fatigue analysis must also be performed. The fatigue analysis considers the growth of the flaw or crack in fracture mechanics terms using a correlation between the cyclic crack growth rate, da/dN, and the stress intensity range, ΔK. The growth of the flaw can be determined using the methodology given in Appendix A to ASME Section XI, or similar methodology like ASTM and the JSME code. Flaw Evaluation Handbooks can be obtained from the NSSS vendors that can be used as a plant specific tool to assess the growth of a flaw over the design life of the RPV, as well as to determine the critical flaw size for instability. The ageing management programme should include either a Flaw Evaluation Handbook or be prepared to perform a fracture mechanics analysis if and when a flaw is detected during ISI.

(d) Microstructural Analysis of a Flaw — If a flaw is detected during ISI, consideration should be given to removing the flaw by taking a boat sample that contains the flaw and performing a microstructural analysis to determine if striations are evident on the surface of the flaw. Striations on the surface of a flaw means that the initiation of the flaw or growth was due to fatigue. If it is determined that a flaw was initiated by fatigue, then one should question the fatigue analysis performed prior to service. Removal of a flaw following ISI is not normally performed once a NPP has gone into operation because Code or Regulatory approved fracture mechanics methodologies are available to assess the growth and critical size of flaws. However, the ageing management programme should consider removal and metallographic evaluation as an option. If a flaw is removed for microstructural assessment, care must be taken to minimize residual stresses resulting from a repair of the cavity.

8.2.5 Wear

Degradation due to wear may occur during maintenance operations concerned with opening and closing of the RPV head. Wear can occur in the filets of the RPV bolts (studs). And, the RPV O-ring and the surfaces of the RPV flanges may also be degraded or damaged during the opening and closing operations. The RPV bolts (studs), the surface of the flanges, and the O-ring should be inspected for evidence of degradation or wear. Corrosion visible on the outside of the RPV due to reactor coolant leakage from the head bolts or studs, a damaged O-Ring or scared flanges provides indication of such a wear/degradation**.** Visual inspection of components of the RPV that may be subjected to wear should be part of the ageing management programme.

8.2.6 Boric acid corrosion

Boric acid corrosion due to leaking reactor coolant has resulted in wastage of the low alloy steels of the RPV flanges, top closure heads, and RPV studs. Visual inspections should be therefore performed during each refuelling outage to identify potential boric acid leaks from not only the Alloy 600 penetrations but also from pressure retaining components above RPV head. Once a boric acid leak is detected, the wastage level of affected ferritic steel components must be determined.

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Consultants Meetings

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