*Stojan Sedmak[1], Vencislav Grabulov[2], Dejan Momčilović[2]*

# CHRONOLOGY OF LOST STRUCTURAL INTEGRITY INITIATED FROM MANUFACTURING DEFECTS IN WELDED STRUCTURES

# HRONOLOGIJA GUBITKA INTEGRITETA KONSTRUKCIJA ZBOG GREŠAKA IZRADE ZAVARENIH KONSTRUKCIJA

Adresa autora / Author's address:
[1] Tehnološko-metalurški fakultet Univerziteta u Beogradu
[2] Institut za ispitivanje materijala (IMS), Beograd
vencislav.grabulov@institutims.co.rs

*Abstract*

*At different time periods, loss in structural integrity occurs from initiation of manufacture defects in the welded joint. Failure mechanisms are different and present a new experience always requiring new knowledge and approach in solving the problem. Extended analyses of all failures considered here and derived conclusions are yet not sufficient in completely avoiding manufacture imperfections and assuring in-service integrity of welded structures. Directives, standards and recommendations for manufacturing welded structures significantly contribute in problem solving which they cannot completely eliminate due to the complexity and numerous influencing factors.*

*Izvod*

*U različitim vremenskim periodima dolazilo je do gubitka integriteta zavarenih konstrukcija zbog razvoja proizvodnih grešaka zavarenog spoja. Mehanizmi otkaza su bili različiti i uvek su predstavljali novo iskustvo, koje je zahtevalo nova znanja i drugačiji pristup u rešavanju problema. Opsežne analize svih otkaza razmatranih u radu i izvedeni zaključci ipak nisu dovoljni da se potpuno izbegnu proizvodne nesavršenosti i obezbedi integritet zavarenih konstrukcija u eksploataciji. Direktive, standardi i preporuke za izradu zavarenih konstrukcija značajno doprinose rešavanju problema, ali ga ne mogu potpuno eliminisati zbog složenosti i velikog broja uticajnih faktora.*

## INTRODUCTION

After several decades of successful use of welding processes in practice they could be applied for joining components of responsible and heavy duty structures. The reliability of welded joint quality was limited, and for many years riveted joints had been considered as superior in structural safety, even in the case when sealing was of prime interest, as in pressure vessels or ships. For that, the evidence of welded joint quality of responsible structures has been requested by customers from the very beginning of their manufacture. Non-destructive test method by X rays has been applied to record radiographs of welded joints. Initially, only the contract between the customer and manufacturer defined quality requirements, but extended production by welding dictated to introduce national and international codes, standards and directives. This was necessary since welded structures failed in spite of all measures taken in design, manufacture and inspection. It is to underline that the failure of a welded structure is a very important accident, sometimes with catastrophic consequences.

Four typical spectacular structural failures that took place at different time periods, carefully studied, are selected to demonstrate how serious the problem is.

## FRACTURES OF SHIPS

Many fractured Liberty class ships failed due to steel susceptibility to cracking below nil-ductility transition temperature, as identified in this case study analysis, /1/. Figure 1 shows the breaking in two of a new "Schenectady" Liberty class tanker at outfitting birth on 16th January 1943 at 10:30 PM after successful sea trial. The water was calm and cold (4.5ºC) and –3.5ºC air temperature.

Sudden fracture started near amidships at the junction of fashion plate to the sheer strake at the starboard corner of the bridge superstructure, and ran very fast across the deck and down both sides to the turn of the bilge. Only the bottom plating held, being capable to arrest fast growing crack. Welded ship structures are of more monolithic nature in comparison to previously riveted or bolted ships and it is presumed that this affects fracture behaviour. The failure of

riveted or bolted ship structure is generally an isolated event that has very rarely led to total collapse, but in many cases it occurred in welded ship structures.
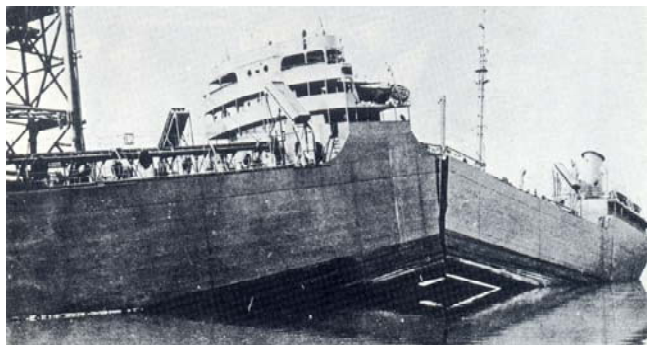


Figure 1. "Schenectady" fractured in two, 16<sup>th</sup> January 1943, /1/.
Slika 1. „Šenektedi" prelomljen na dva dela, 16. januara 1943, /1/

Another example of failed Liberty ship is presented in Fig. 2, /2/. This failure occurred during welding, starting from previous arc-strike (Fig. 3) and ended in instantaneous splitting of the ship.
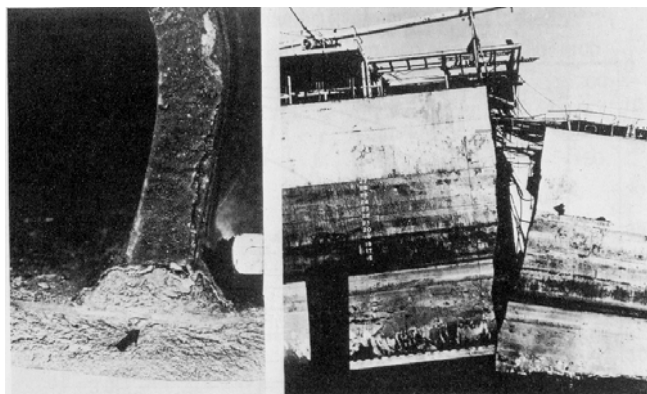


Figure 2. Dock side ship fractured from the arc strike (arrow).
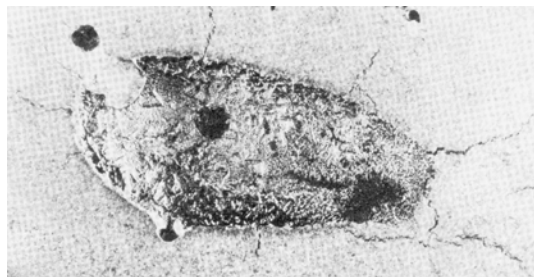Slika 2. Bočna strana preloma od početnog elek. luka (strelica)



Figure 3. View of arc strike. Cracks approximately 2 mm in size developed in hard HAZ surrounding the fusion line.
Slika 3. Izgled mesta početka elek. luka. Prsline oko 2 mm su se razvile u tvrdom HAZ oko linije stapanja

Liberty ships were built in USA for transporting war supplies. They travelled in convoys. Several fractures were reported in 1942. Fractures were sudden and accompanied by loud bang, suggesting enemy attack. After the fracture of "Schenectady" the problem was taken seriously. Another similar failure is the "Esso Manhattan" ship in March 1943 after 7 months service that confirmed the problem. Up to March 1946, 132 serious ship fractures were reported, 99 Liberty ships and 16 T2 tankers, and 319 fractures were reported in 1959. Serious ship fractures were reported also in Europe (Germany, Sweden, Great Britain, Denmark). Out of 28 of these ships, built between 1942 and 1965, six had broken in two.

The continuity of Liberty ship production had been enabled by improvements involved in design, material properties and manufacture, but theoretical and experimental analysis was necessary for final solution. Considering 11 different aspects of fracture, it became clear that the full answer to this very complex problem can be given only after series of full-scale and laboratory tests. Following metallurgical aspects of the problem, step by step, the basic approach was defined by Boyd, /1/: "It would seem that the main factors which were under suspicion, namely welding, workmanship, design, locked up stresses (constrain) and even the quality of the material, have all been exonerated, and that the root of the trouble lies in an elusive property of the material, i.e. its *Notch Sensitivity at Low Temperature*" (unpublished report).

Considered theoretical aspects will be shortly presented, having in mind that the analysis took place from 1943 to about 1950, before principles of Fracture Mechanics were established.

It was found that *strength* is not critical, in spite of the extensively introduced welding for the first time. Ship components were designed in a proper way regarding their thickness and stress, well below specified yield stress.

Anyhow, *experience in design and manufacturing* was limited, because manufacturers without experience were involved in shipbuilding. Yet also some ships produced by recognized shipbuilders failed. It was the case with British tanker "World Concord" in 1954, built in a premier British shipyard 2.5 years ago under strict supervision of both the American Bureau of Shipping and Lloyd's Register of Shipping.

Brutal *manufacturing methods* were under suspicion, since they had been applied for assembling large sections with close fits and in correcting the distortions arising from welding during prefabrication. And yet, this happened also with "Ponagansett" at the quayside in March 1947, made by high qualified welding and inspection personnel. Ship structures produced in this way were *rigid*, in addition to the monolithic welded structure.

*Rigidity* of a failed ship was expressed, not only due to manufacturing method, but also by the effect of monolithic welded structures that are more rigid compared to riveted ships and more sensitive to stresses and strains imposed by the sea. But three typical ships, "Schenectady", "Esso Manhattan" and "Ponagansett" had broken in an essentially static condition. Following the assumption that the over-loaded structure must plastically deform or fracture in a brittle manner, many full scale experiments with ships were performed, indicating that welded ships were actually more flexible than their riveted counterparts.

Inevitable *stress concentration*, supposed as more dangerous in a welded structure than in riveted ships, was reduced by shape improvement, but a large number of T2 ships again, fractured in two pieces. The total fracture of "Ponagansett" originated from arc strike near the auxiliary

attachment, as presented in Fig. 3. Such minor features can not alone produce complete rupture of a structure.

Many experts believed that *residual stresses* are responsible for fracture of ships. It is well known that welding can produce residual stresses, locally reaching yield point which can contribute to fracture added to service stresses. The same holds for reaction stresses, introduced by overall contractions and forces used to put parts together in ship assembly. Due to its friability they could produce extensive fracture. They can contribute to ship fracture, but they are not the main reason. The problem of residual stresses still attracts attention and many investigations are performed so to solve this problem. In the case of thick sections, local heat treatment can be necessary.

*Temperature stresses* arise from nonlinear temperature gradient and are regarded as residual stresses. They can contribute to the initiation and propagation of a crack and fracture of material that is already in brittle condition.

*Welding workmanship*, welding procedure and inspection of fractured ships were at the acceptable level at the time. Few fractures could be attributed to poor welding, although many fractures originated at welds, particularly when they were located in the stress concentration region. In any case, this problem even today attracts attention, /3/. Anyhow, fractures are frequent and extensive in welded structures and they can occur at low nominal stresses. For that it is also necessary today to respect local metallurgical damage of the parent metal, inevitable in welding.

Due to *geometrical size effect* one would expect greater number of brittle fractures in large structures such as ships, bridges, and pressure vessels of substantial thicknesses.

*Fatigue* cracks can occur in considered ships, but the majority of them are detected and repaired before they become extensive and critical in size. So, only in few cases fatigue cracks initiated fractures. In general, fatigue was not considered as an important cause of reported fractures.

*High strain rate* contributes to the brittle response of material, as is the case with ships exposed to loads at sea. But having in mind fractures occurring in calm water, the effect of shock loading is not of prime interest here.

Experiments with large scale specimens have clearly shown that the character of fracture is strongly affected by temperature. Before that, it was thought that the sharpness of the notch was the main controlling factor, but it was found that fracture was entirely ductile even with the sharpest notch until the temperature was reduced below a certain level. The term "transition temperature" was introduced for the temperature at which the mode of fracture changes. Thus, this new material property has been introduced into specification.

The next research was directed to ascertain the notch ductility of steels, to arrive at an applicable criterion and level of acceptability, to elucidate the mechanism of brittle fracture and determine how to control its occurrence. Practical solution for the considered case of ship fractures is given in the Second Technical Progress Report of Ship Structure Committee (1950), in the conclusion, /1/: "Brittle fractures may be initiated in welded ships if the steel used in main hull girder absorbs less that 15 foot-pounds (20 J)

of energy in the standard V-notch Charpy impact test at a temperature of 60ºF (15.5ºC). It is not known, however, how much more notch tough the steel must be in order to remove the danger of brittle fracture. This statement is based on the investigation of failures during the past eight years and consequently is established only for the types of vessels covered by the investigation and the operating conditions that existed for these same vessels."

As the result of correlation between Charpy V impact energy and testing temperature, steel quality up to 20 J temperature became a conventional reference. The $C_v$ test requires specific calibration for different steels.

It was noted that cracks arrested in service when entering a new plate in regions of uniform low stress (Fig. 4). The painted surface had crazed at the arrest point due to higher fracture toughness of new plate, indicating yielding level.
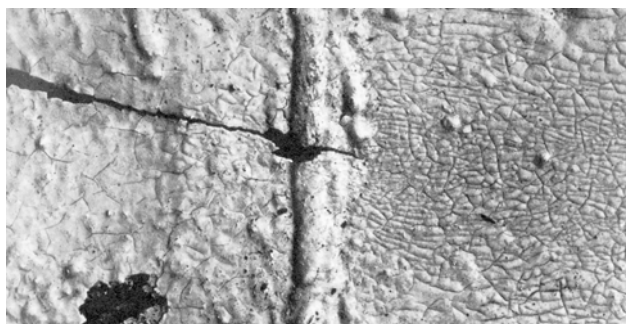


Figure 4. Arrested ship fracture. Crazing of paints indicates yielding in the crack arrest region, /2/.
Slika 4. Opuštanje loma. Razbijanje boje ukazuje na tečenje u oblasti opuštanja prsline, /2/

## BRITTLE FRACTURE OF PRESSURE VESSEL

Failure of a thick-wall pressure vessel, /4/, presented in Fig. 5, is selected as well analysed example. This failure is caused by a crack in HAZ. A large pressure vessel designed for use in an ammonia plant at 350 bar of pressure and 120ºC failed at 340 bar during hydrostatic proof testing, /4/, well below maximum test pressure of 480 bar. The vessel weighed 166 ton, measured 18.2 m in length and 2.0 m outside diameter. It was fabricated from ten Mn-Cr-Ni-Mo steel plates 150 mm thick, which were rolled and welded in ten cylindrical shell sections and three forgings of similar steel. The forgings formed two end lids and a flange for attaching one of the forged end lid to the vessel.

Extensive damage to one end forging and three adjacent shell sections was a failure consequence. Four large pieces were blown from the vessel; the largest, weighing about 2 ton, penetrated the shop wall and travelled a total distance of 46 m. Pieces of the failed vessel are shown in Fig. 6.

Cylindrical shell sections were hot formed with rolling direction of the plates perpendicular to the axis of the vessel. All plates were normalised-and-tempered. Forgings were annealed, normalised and tempered at 645ºC to obtain desired mechanical properties.

Longitudinal seams in the cylindrical shells were electroslag welded, and welds were ground to match the curvature of the shell to reduce stress concentration. Cylindrical shell sections were heated at 900 to 950ºC for 4 h, rounded in the

rolls within that temperature range, and then cooled in still air for examination of the seams.



Figure 5. Large thick-wall pressure vessel – an ammonia converter, fractured because of a crack in HAZ.
Slika 5. Velika posuda pod pritiskom – konvertor amonijaka, polomljen usled prsline u HAZ
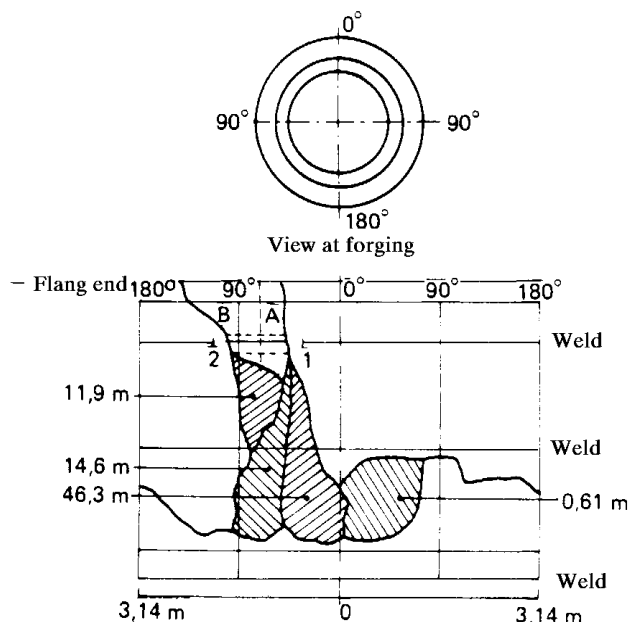


Figure 6. Pieces fractured from the vessel, with distances to which they were thrown.
Slika 6. Delovi loma na posudi, sa rastojanjima do kojih su odbačeni

Circumferential welding was done by submerged arc process with preheating at 200ºC. Each subassembly was stress relieved at 620 to 660ºC for 6 h.

Final joining of three subassemblies followed the same welding procedures except that localised heating for stress relief was employed. During various stages of manufacture, all seams were examined by gamma radiography, automatic and manual ultrasonic test, and magnetic particle inspection. The vessel was closed at the top and water was admitted to the vessel through a fitting in the cover plate until the vent that was within 3 mm of the inside surface at the top showed a full-bore discharge of water, and then it was blanked off. The proof test was specified at 480 bar and ambient temperature above 7ºC. A halt was made at

340 bar, and after 30 s the flange end exploded. The forging was completely through-cracked in two locations, and first two cylindrical sections damaged (Fig. 5).

Metallurgical examination revealed fracture surfaces typical for brittle steel fracture. There were two points of origin, both associated with the circumferential weld between the flange forging and first shell section. The general appearance of fractured surface of flange forging is shown in Fig. 7, with enlarged views of regions of flat facet (major dimensions roughly 9 mm), 14 mm below the outer surface of the vessel, which was partly in the HAZ on the forging side of the circumferential seam weld. A slightly larger facet was found on another surface of the flange, near a fracture origin, 11 mm below the outer surface, also situated partly on the forging side of the HAZ. These facets in the HAZ were fracture-initiation sites. The structure just below facets in the HAZ was a bainite-austenite mixture (hardness 426 to 460 HV). Elsewhere, the structure of the HAZ was coarse bainite (hardness 313 to 363 HV).
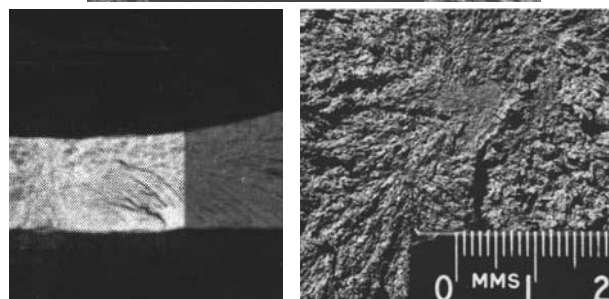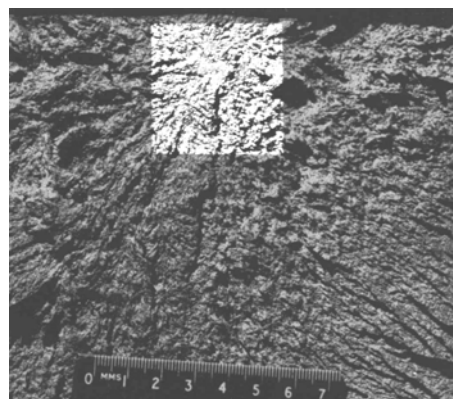


Figure 7. Point of fracture initiation located at the weld, between shell and forged nozzle, /3/.
Slika 7. Mesto inicijacije loma u zavaru, između oklopa i otkovka mlaznice, /3/

The specified chemical composition amid, measured by testing of fractured parts are given in Table 1.

Table 1. Chemical composition of plate, forging and weld metal of fractured parts.
Tabela 1. Hemijski sastav ploče, otkovka i metala šava polomljenih delova

| Element | | C | Si | S | P | Mn | Ni | Cr | Mo | Cu | V |
|---|---|---|---|---|---|---|---|---|---|---|---|
| Plate | Spec. | 0.17 | 0.30 | 0.05 | 0.05 | 1.5 | 0.30 | 0.70 | 0.28 | 0.30 | 0.10 |
| | Meas. | 0.15 | 0.22 | 0.023 | 0.025 | 1.31 | 0.14 | 0.64 | 0.27 | 0.17 | 0.08 |
| Forging | Spec. | 0.17 | 0.30 | 0.04 | 0.035 | 1.5 | 0.30 | 0.70 | 0.28 | 0.30 | 0.10 |
| | Meas. | 0.20 | 0.27 | 0.008 | 0.009 | 1.48 | 0.22 | 0.83 | 0.92 | 0.11 | 0.09 |
| Weld metal | Meas. | 0.08 | 0.31 | 0.020 | 0.025 | 1.05 | 0.12 | 1.34 | 0.83 | | 0.01 |

Examination of a section transverse to the weld showed that the structure of the flange forging contained bands, whereas the plate did not. The structure between the bands (and away from the weld) consisted of ferrite and pearlite with hardness of 180 to 200 HV. In the bands, the structure was upper bainite with hardness of 251 to 265 HV. Specimens cut from this area were austenized at 950°C and quenched in a 10% solution of NaOH in water to produce pure martensite. A Vickers hardness across a band showed average values of 507 on one side of the band, 549 within the band, and 488 on the other side. A microprobe scan across the banded area showed the following differences in chemical composition: 1.56% Mn outside the band, 1.94% within it; 0.70% Cr outside, 0.81% within; and 0.23% Mo outside, 0.35% within. These differences indicated higher hardenability within the bands, with greater susceptibility to cracking, where the bands met the HAZ of the weld.

Hard spots within the forging suggested that during stress relief the vessel had not attained the specified temperature. To check this, HAZ specimens were accordingly heated to a variety of tempering temperatures to observe at what point softening would occur. Vickers hardness tests on hard-spot specimens showed no deviation from the as-failed hardness scatter band for temperatures up to 550°C, but gave lower values after re-tempering at 600°C. A similar behaviour occurred in specimens taken outside the hard spots. It was therefore concluded that stress relief had not been performed at the specified temperature level.

Other studies of microstructure revealed that crack propagation mode was transgranular cleavage which occurred in prior-austenite grains but also in ferrite grains and pearlite colonies. Branching and subsidiary cracks were found, all very close to the main fracture surface. The distance from the fracture to the crack farthest away from it was 0.5 mm.

To further investigate the effect of stress relief on the properties of the vessel, standard Charpy V-notch specimens were prepared from the flange forging, plate, and weld. All specimens were oriented with their length parallel to the circumference of the vessel so that induced fractures would be parallel to the main fracture. Tests were performed on as-received vessel material over the temperature range from 10 to 100°C. Impact values met specifications for the forging and plate, but the weld metal was of inferior impact strength (lower curve, Fig. 8). None of the materials displayed a sufficient capacity for energy absorption (41 J for the forging; 69 J for the plate; and 16 J for the weld) to have arrested the crack growth that led to failure of the pressure vessel. Other weld metal specimens, re-tempered at 650°C for 6 h and tested over the same temperature range, showed an improvement in impact strength at or above 20°C (upper curve, Fig. 8). This confirmed the previous conclusion that stress relief of the vessel was at a lower temperature than specified.

It was possible to conclude that failure of the pressure vessel stemmed from the formation of transverse fabrication cracks in HAZ of the circumferential weld joining the flange forging to the first shell section. The occurrence of cracks was fostered by the presence of bands of alloying-element segregation in the forging that had created hard spots, particularly where they met HAZ. Stress relief of the vessel had been inadequate, leaving residual stresses and hard spots and providing low notch ductility, especially in the weld.

Recommended final normalizing temperature was $Ac_3$ + 50°C , and tempering temperature 650°C, where $Ac_3$ is the temperature for complete transformation of ferrite to austenite. After completion of each seam weld, preheating should be continued for a short distance along the seam. Forging practice should eliminate the banding in the flange.

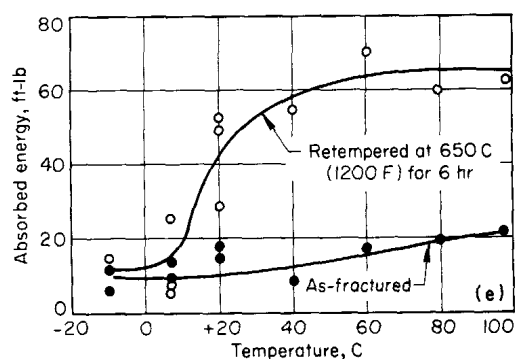However, after repairing action, the pressure vessel had been accepted for service in 1968.



Figure 8. Absorbed energy in Charpy V specimen test: lower curve–weld metal of fractured part; upper curve–after heat treatment at 650°C for 6 h.
Slika 8. Apsorbovana energija Šarpi V epruvete: donja kriva–metal šava polomljenog dela; gornja kriva–nakon termičke obrade na 650°C u trajanju 6 č.

It was decided to define critical flaw size for this alloy and section thickness by fracture mechanics, and to asses the capacity of available non-destructive testing methods for detecting flaws of this size. Fracture mechanics analysis of this fracture had been made in 1976, several years after the event, when necessary tools and formulae were available. The presented data of the performed analysis supplied basic parameters for fracture mechanics analysis, but additional data were necessary, according to formula:

$$K_{Ic} \leq \psi\sigma\sqrt{\pi a_{eff}} \qquad (1)$$

where $K_{Ic}$ stands for plane strain fracture toughness, $\psi$ is the shape factor, $\sigma$ nominal stress perpendicular to crack surface, $a_{eff}$ effective crack length, $\sigma$ and $a_{eff}$ have to be as accurate as possible.

Nominal stress $\sigma$ had to include active stress due to inner pressure and also possible residual stress. Maximum pressure load acts in the circumferential direction, calculated for middle plane diameter (average between outer and inner diameter). Stress concentration in the considered case can be ignored. It is much more complicated to take into account residual stresses due to welding. It is known that residual stresses can not overpass yield strength when stress relieving is not completed. For that, nominal stress $\sigma$ could be calculated according to

$$\sigma = \frac{p \cdot D}{2s} + \sigma_{To} \qquad (2)$$

where $p$ is pressure, $2D = D_i + D_s$ is average diameter ($D_i$ is inner, $D_s$ outer diameter), $s$ is wall thickness, $\sigma_{To}$ is yield strength of parent metal at stress relieving temperature.

Effective crack length $a_{eff}$ equals to a real crack length in the elastic range, for stress well bellow yield strength. When active stress is close to yield strength, the effective crack length has to include developed plastic zone size, e.g. according to Irwin model. From Irwin model, it follows

$$a_{eff} = a\left[1+\left(\frac{\pi\sigma}{4\sigma_T}\right)^2\right] \qquad (3)$$

with $a$ as crack real length, $\sigma_T$ yield strength of plastically deformed part in weld metal.

Material properties, $K_{Ic}$ and $\sigma_T$, have to be known for the application of Eqs. (1) to (3), as well as crack length, $a$, and its shape factor, $\psi$. Material properties, obtained by testing of fractured parts, are given in Table 2. One can conclude that measured strength values (yield strength and tensile strength) are higher than specified. Performed Charpy V tests at room temperature have shown (Table 3) that impact energy values are higher than specified, 34 J.

Table 2. Material properties of fractured parts.
Tabela 2. Osobine materijala polomljenih delova

| Property | Yield strength | Tensile strength | Plane strain fracture toughness |
|---|---|---|---|
| | $\sigma_T$, MPa | $\sigma_M$, MPa | $K_{Ic}$, MPa√m |
| Specified | 370 | 556 | – |
| Plate | 411 | 597 | 120 |
| Forging | 389 | 601 | 110 |
| Weld metal | 761 | 851 | 58 |

Table 3. Charpy V energy, obtained from weldability assessment test.
Tabela 3. Šarpi energija, dobijena ispitivanjem procene zavarljivosti

| Charpy V specimen | Position of notch | | | | | |
|---|---|---|---|---|---|---|
| | Weld metal | HAZ | Fusion line | Weld metal | HAZ | Fusion line |
| Impact energy, J | 40 | 64 | 40 | 43 | 131 | 40 |

From two facets position and size (Fig. 7), although irregular shape, the crack length $2a = 8$ mm is assessed and $\psi = 2/\pi$ is taken as shape factor. Since the weld metal is brittle (Fig. 8), it is possible to calculate the significance of existing cracks. From Eqs. (1) and (3) it follows

$$\sigma = \frac{4\sigma_T}{\pi\sqrt{2}}\left[\sqrt{1+\frac{\pi^3 K_{Ic}^2}{16\sigma_T^2 a}}-1\right]^{1/2} \qquad (4)$$

For weld metal values ($K_{Ic} = 58$ MPa√m, $\sigma_T = 761$ MPa, $a = 4$ mm), the critical stress for crack initiation is obtained: $\sigma = 669$ MPa. The value $\sigma_{To} = 389$ MPa is obtained from Eq. (2), and pressure to produce fracture is $p = 450$ bar, 30% higher compared to real fracture. In order to explain this discrepancy, the real crack is of irregular shape, and not circular, so $\psi$ must be higher than assumed. Also, the applied stress, produced by pressure, is lower than yield strength of weld metal, so the effect of residual stress is higher. The conclusion of fracture mechanics analysis was that even a crack, very small compared to wall thickness can be imitated in a critical situation. It is proved by a crack in the HAZ of forging after welding (Fig. 9), not detected,

and not developed during this failure. The importance to detect defects in pressurized structures is clearly shown.
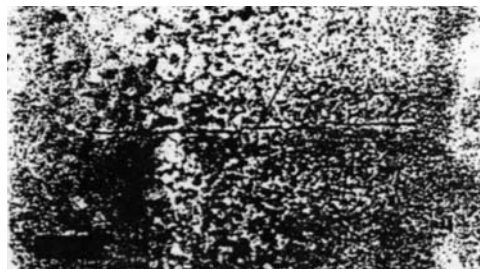


Figure 9. Not detected weld crack in HAZ of forging.
Slika 9. Ne otkrivena prslina pri zavarivanju u HAZ otkovka

FAILURES OF SPHERICAL STORAGE TANKS

Spherical tanks (Fig. 10), 150 to 5000 m³ in volume, up to 20 m in diameter, are used for storing under pressure liquefied natural gases (LNG), ammonia, carbon dioxide, vinil-chloride-monomer (VCM). Transition temperature of applied steels should be between –40° and –70°C. They operate at environmental temperatures. Generally, they consist of segments, upper and lower lids, joined by welding, and are supported by several steel legs, welded to the sphere directly or through holding plates (Fig. 10). The outer side is protected by anticorrosive layers, and sometimes insulation is necessary (e.g. storage of ammonia, ethylene).
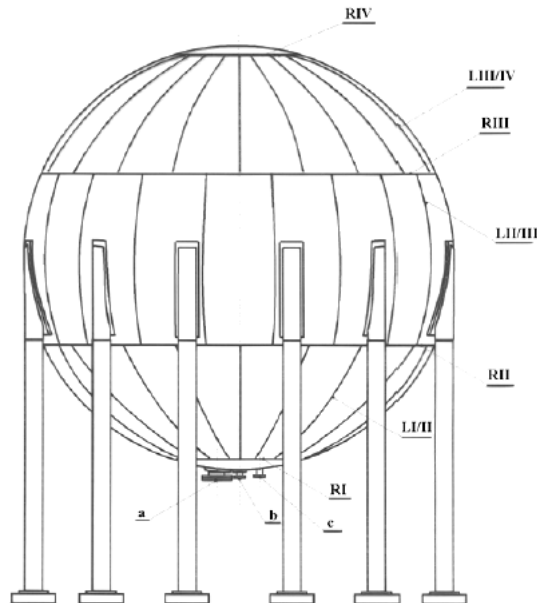


Figure 10. Spherical storage tank.
Slika 10. Sferni rezervoar

At the beginning of the seventies new fine-grained steel TTSt E-47, microalloyed by vanadium, of nominal yield strength 460 MPa, was developed as promising and worldwide applied for manufacturing spherical storage tanks in the processing industry and oil refineries in different countries (also in Czechoslovakia and Yugoslavia). In the eighties, after several years of service, a large number of storage tanks leaked in succession, as reported in Refs. /6, 7/.

During regular inspection after 8 years the damage on spherical storage tanks for natural gas was detected in Czechoslovakia, /6, 8/. A through-wall crack, 50 mm long

caused leakage, cooling and wall freezing. The crack developed along fusion line of metal manual arc (SMAW) welded joint. It was attributed to initiation and growth of an existing cold hydrogen-induced crack (HIC). Since, numerous cracks were detected by ultrasound in other vessels. Number of defects ranged from 24 to 239, with total lengths 11 to 193.5 m per vessel. Further 16 to 183 cracks per vessel were found by magnetic methods. In addition to macroscopic surface cracks, a lot of short cracks were revealed in welded joints, generally on the inner side. Lengths of individual cracks ranged from 10 to 2400 mm, and depths from 1 to 27 mm (half wall thickness). During the removal of cracks by grinding, further cracks, up to 2 mm length were revealed by penetrants in the fusion line regions, not found by other methods.

Cracks occurred by disrespecting specified SMAW in-situ technology. Neither specified preheat was applied nor probably the required procedure used for drying electrodes. High content of diffusible hydrogen enabled crack initiation. Thus, crack nuclei were already present in the tank at the assembly stage and grew to macroscopic size in subsequent operations, e.g. proof pressure tests. These defects in the manufacture were not detected by applied radiographic inspection, insensitive to such microscopic defects.

It turned out that steels of this class are sensitive to cold cracks in HAZ, so cracks initiated in service under applied tensile stress, and in some stored media stress corrosion cracking also occurred. Cracks propagated in SMAW welded joints, through the fusion region, in underbead zone of high hardness, containing up to 90% of martensite. Samples for macro- and microstructural analysis, hardness measurements and chemical analysis, about 10 mm wide, 70 mm long and 12 mm deep, encompassing the crack, were cut in the form of small boats (Fig. 11). The crack initiation region is shown in Fig. 12 with intercrystalline surface as the crack passed through the underbead bainitic-martensitic zone. Small embedded cracks were found in welded joints (Fig. 13). Similar cracks on the surface with oxides were observed in other tanks, indicating that small cracks were present in welded joints even prior to annealing.



Figure 11. Boat form sample with crack
Slika 11. Uzorak oblika čamca sa prslinom

Figure 12. Crack initiation location in the underbead zone.
Slika 12. Mesto inicijacije prsline u oblasti ispod zrna

The repair of so many cracks is not certain, and by repair welding new short cracks can be involved of sizes below NDT equipment sensitivity. Details about repair technology are given by Hrivnak in Refs. /6, 8/.

Many of about 100 tanks in service in Yugoslavia failed due to cracking (Fig. 14). The first failed in 1982 by transversal crack leakage at ambient temperature 20°C under 12 bar pressure, some weeks after regular in-service inspection and proof pressure test at 24 bar (Fig. 15). Careful ultrasonic and magnetic particle tests revealed a significant number of cracks on the inner surface, /7/.
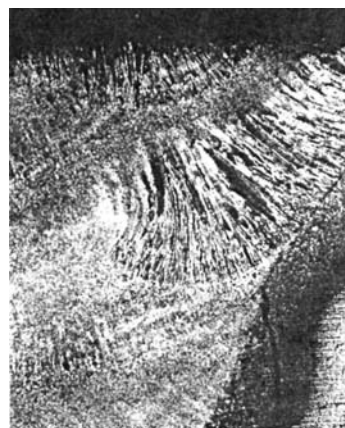


Figure 13. A small embedded crack in the heat-affected-zone
Slika 13. Mala unutrašnja prslina u zoni uticaja toplote
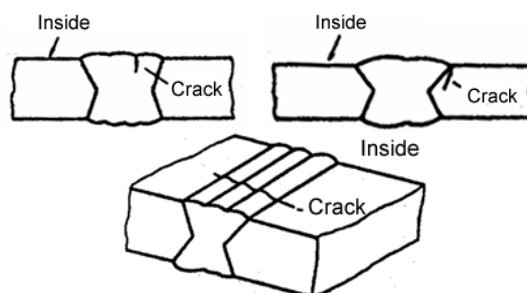


Figure 14. Longitudinal (up) and transverse (down) cracks.
Slika 14. Podužne (gore) i poprečne (dole) prsline
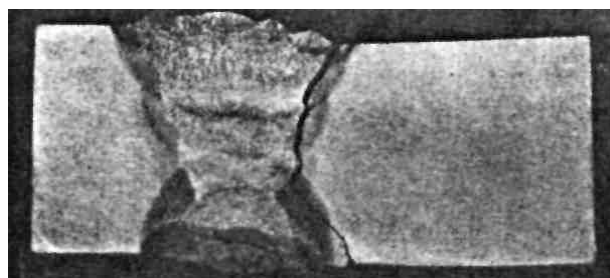


Figure 15. Macrostructure across welded joint with through crack.
Slika 15. Makrostruktura zavarenog spoja sa prolaznom prslinom

Several spherical tanks are in service at HIP-AZOTARA, Pančevo. Regular in-service inspection in 1993 revealed many defects, mostly cracks (Fig. 14) on the inner side of a sphere, /7/. Crack occurrence is particularly expressed in fine-grain microalloyed high strength steels, but cracks were also detected in tanks of carbon steels after long-term service. On one of the tested vessels, cracks were detected at the joint between mantle and lid on the outer side, on one

vessel at the joint between support reinforcement and mantle, and on one sphere the defects in columns.

The proposed causes of crack initiation were cold cracks induced by welding, due to hydrogen content in consumables, disrespecting specified welding technology, stress corrosion and damages in service. Cracks nucleated during vessel service, since in the previous periodic inspections non-allowable defects had not been detected, so confirming incubation period for stress corrosion cracks nucleation, /6/.

Adverse effect of specified proof test (cold-water pressure test) was accentuated. The testing of the tanks before and after the test has clearly shown that proof test in service can cause new cracks at positions of "old", but not new repair welded joints, indicating that the latter were performed under strict control. For that reason, suggestions were addressed to the Boiler Inspection Office, /9, 10/, to reduce test pressure, especially for tests in service. Experience had shown that the pressure vessel repaired by welding should not be subjected to pressure testing, but only periodical ultrasonic test of typical repaired positions from the outer side should be performed, e.g. immediately after repair ("initial" state) and after operating parameters are reached. In these tests, if no crack were detected, the tests should be repeated every 6 to 12 months until the date of regular periodical proof test. Due to significant financial expenses imposed on the tank owners (repeated cycles: testing, repair with testing, testing after the repair, proof test, testing after proof test, periodical inspection), selective approach to testing and repair of vessels prevailed, /10/, so that only the overfills on critical locations (radial welded joints and crossings) should be ground and subjected to ultrasonic testing. If defects are detected at these locations, overfill grinding and ultrasonic testing should be increased up to 100%. In this approach the reliability of testing is not reduced. There is no reason to perform ultrasonic tests when shallow cracks are detected by magnetic particle tests being a frequent case. It is better first to grind cracks and then to perform the ultrasonic test.

*Case study of spherical storage tank failure*

The problem of spherical storage tanks is significant, since a large number are still in service, /11, 12, 13/. To illustrate the situation, one case study is presented here.

During regular inspection of spherical tanks for storage of vinyl chloride monomer (VCM), volume 2000 m$^3$, introduced in 1977, a great number of cracks of different directions and sizes (Fig. 14, Tables 4 and 5) were detected by NDT in 1986-90 and in 1996 on inner wall side, in welded joint regions, /11/. In three cases, the cracks were detected in the parent metal, and their origin is probably the tack welding of temporary holders during sphere assembly. In some cases during grinding it was confirmed that the crack ends in the pore or the inclusion, involved in the manufacture. In all tested spheres, cracks most frequently occurred in radial welded joints (R, Fig. 10), typically in its middle (upper) part, at the liquid and gas phase border.

Spheres are not insulated, the inner pressure depends on ambient temperature (e.g. at +10°C pressure is 1.51 bar, at +35°C it is 4.23 bar). Spherical storage tanks (Fig. 10) are assembled by 24 segments and two lids (bottom and top), of

20 mm wall thickness, and supported by 12 legs. Tanks are welded by longitudinal joints (total 483 m), 50% by submerged arc welding (SAW) and 50% by SMAW, and radial joints (total 120 m) by SMAW process, with consumables corresponding to the applied NIOVAL 47 steel.

The measured mechanical properties of NIOVAL 47 are: yield stress $R_{p0.2} = 470$–506 MPa, tensile strength $R_m = 639$–660 MPa, elongation at fracture $\delta_5 = 25$–27%, impact toughness 120–166 J/cm$^2$ at 0°C.

Chemical analysis: 0.18–0.19%C, 0.44–0.45% Si, 1.42–1.43% Mn, 0.08% V, 0.048–0.055% Nb, 0.012–0.015% P, and 0.009–0.014% S.

Starting from the spherical storage tank assembly and use, following events are recorded:
– November 1976: I proof test (12 bar) – no data.
– September 1977: Spherical storage tank put into service.
– September 1983: II proof test – no leakage recorded.
– April 1987: III proof test – no leakage recorded. After putting into service ultrasonic test was performed on the outer side of two radial welded joints (RI and RII).
– October 1989: IV proof test (special inspection required by Boiler Inspection Office) – no leakage recorded. Following inspector's request, the overfill on the inner side was grinded for non-destructive inspection. A large number of cracks in longitudinal and radial welds of various length and depth were revealed by magnetic and ultrasonic tests. After repair by grinding and welding, no crack indication is found by magnetic and ultrasonic tests.
– March 1990: V proof test (special inspection) – no leakage recorded, again a large number of cracks of various lengths and depths in longitudinal and radial welds are detected by magnetic and ultrasonic tests. After repair, no crack are detected by magnetic and ultrasonic tests.
– Inspection in March 1991 had detected cracks in 331 locations, on inner spherical storage tank side. In Table 4 are given locations, number and directions of cracks, repaired by grinding and welding of notches deeper than 5 mm, found before and after the proof test.

Table 4. Crack location and orientation on spherical storage tank.
Tabela 4. Mesta i orijentacija prslina na sfernom rezervoaru

| Number of crack before (after) proof pressure test | | | |
|---|---|---|---|
| Welded joint* | Longitudinal | Transversal | Total |
| RI | 2 (3) | 9 (2) | 11 (5) |
| RII | 36 (2) | 85 (-) | 121 (2) |
| RIII | 109 (35) | - (12) | 109 (47) |
| RIV | 6 (10) | 5 (-) | 6 (10) |
| LI/II | 33 (106) | 17 (8) | 38 (106) |
| LII/III | 29 (2) | - (-) | 46 (10) |
| LIII/IV | - (-) | 116 (22) | - (-) |
| Total | 215 (158) | 116 (22) | 331 (180) |

(*) Welded joints are marked in Fig. 10.

The proof test prior to service, at the first proof test pressure level, produces new cracks located in joints already in service, but not in the new weld repair joints. Table 5 presents a number of cracks in one sphere during tests in 1989/90 and seven years later, in 1996.

The repair was unexpectedly successful, since:
– The number of cracks detected in 1996 is negligible compared to 1989/90 (10 against 1354, or 0.7%).

– Typical for cracks detected in 1996 is that they are significantly smaller in depth and length compared to average crack dimensions found in 1989/90.
– Experience from previous tests is confirmed by tests in 1996 regarding the location of crack occurrence (mostly on radial welded joints in the middle tank part), crack direction (dominant longitudinal cracks), and most important, proof test by cold water pressure (even with reduced test pressure) induces new cracks, but at locations where repair welding is not performed after previous inspection, that means in old welds.

Table 5. Cracks detected on one storage tank in 1989/90 and 1996.
Tabela 5. Prsline otkrivene na jednom rezervoaru 1989/90 i 1996

| Welded joint | Number of cracks 1989/90 | | | Number of crack 1996 | | |
|---|---|---|---|---|---|---|
| | Long. | Trans. | Total | Long. | Trans. | Total |
| Bottom lid | 7 | 5 | 12 | 0 | 0 | 0 |
| RI | 20 | 5 | 25 | 0 | 0 | 0 |
| RII | 173 | 10 | 183 | 4 | 1 | 5 |
| RIII | 389 | 10 | 399 | 1 | 0 | 1 |
| RIV | 45 | 6 | 51 | 0 | 0 | 0 |
| LI/II | 32 | 18 | 50 | 1+(*) | (*) | 2 |
| LII/III | 157 | 116 | 273 | 1 | 0 | 1 |
| LIII/IV | 45 | 6 | 51 | 0 | 0 | 0 |
| Total | | | 1354 | | | 10 |

(*) One crack inclined toward welded joint

This is confirmed by magnetic particle test before and after cold water proof test on joints R-I and R-III (Fig. 10) and on adjacent vertical joints, where new cracks were found on the old parts of joints, e.g. on welded joint part that was not repaired by welding. Cracks were detected in longitudinal joint L-17 close to R-III joint, inside the notch induced during repair of cracks by grinding in the year 1991, as well as cracks in HAZ on joint L-12 close to R-I, not existing before proof test. On 6 of 10 detected locations, the crack is located inside the notch produced by repair in the year 1991.

Anyhow, repair by welding is not the direct cause of crack occurrence for the following reasons:
– Cracks were revealed on about 80 other notches produced by grinding repair in 1991.
– Repair in 1991 was performed by proper rounding.
– Cracks inside notches in R-I and R-III were not detected before the proof test.

At locations of repair performed by grinding in 1996, after the proof test no indications of crack-like defects were detected.

*Cold water pressure test*

Directives for technical standards for stationary pressure vessels, /10/, prescribed that the regular periodic proof pressure test of vessels should be in general performed at latest before six years in service.

Water test pressure is calculated by using the formula:

$$p_i = 1.3 p_r \tag{5}$$

where $p_i$ is proof test pressure, $p_r$ is design pressure.

It is experienced that after proof test with this pressure in spherical storage tanks, produced of microalloyed steels, the cracks occurred in welded joints on the inner wall side.

The analysis of actual service condition showed that in an extreme situation maximum operating pressure can be attained, but it happened rarely. Safety valves, regularly calibrated to the set of values +10% above maximum pressure, practically do not allow pressures higher than $1.1p_r$ in service. Accordingly, for spherical storage tanks in service, the test pressure should be calculated according to:

$$p_i = 1.1 p_r + p_g \tag{6}$$

where $p_g$ is the error of the safety valve calibration.

Data from Table 5 confirmed that the repair of damaged tanks is possible, and can be economically effective and reasonable. On the other hand, it is clear that initial welding of spherical storage tank is not performed properly and that quality control was not at the required level. The reason for that can be attributed to the properties of new microalloyed steel requiring more attention than plane carbon steel.

The problem is in how to define future proof tests of spheres in service. It has been shown here that the scope of inspection and repair activities is significantly reduced, but the cracks, less frequently, with small depth and length, still occur on non-repaired welded joint parts. Based on experience, the average crack growth rate of 0.5 mm/year is established. Since this is a relatively slow crack growth rate, and if it would be confirmed experimentally by measurements in specified cases (at typical locations, also with intentionally left cracks during service, and knowing the kinetics of stable crack growth for a specified fluid), future tests could be postponed up to next regular inside inspection without periodical inspection, e.g. to the period of 6 years, with the intention that tests after 6 years should be performed in significantly reduced scope in comparison to first and second visual examination.

COLLAPSE OF ROTOR EXCAVATOR

Collapse of rotor excavator (Fig. 16) in an open surface mine occurred due to fracture of lugs on counterweight holder (Figs. 17, 18), and magnified in Fig. 19. The borders between different fracture types are shown in Fig. 20.



Figure 16. Collapse of rotor excavator.
Slika 16. Kolaps rotornog bagera

Figure 17. Fracture of two lugs caused the rotor excavator collapse.
Slika 17. Lom dve uške izazvao je kolaps rotornog bagera

In-situ visual examination was sufficient to recognize fracture scenario. Cracks originated in T welded joints, connecting lug and web. Starting from these cracks, in a determined time period variable service loads produced fatigue cracks on both sites of lug plate (upper part in Figs. 18 and 19). At the end of fatigue crack, stable crack growth in plane stress condition with clearly recognized shear lips started after crack tip blunting and developed final stretch zone (Figs. 19, 20a). On both sides from the T weld, stretch zones developed at the end of stable cracks (ZP, Figs. 19, 20c; CA, Figs. 19, 20b). Final stretch zone in position ZP developed in one stage before final brittle fracture took place. On the other side, at position CA, stretch zone partly

developed after crack blunting, and, after stress redistribution, the crack continued to grow in a stable manner in the second stage up to reaching the final stretch zone, when brittle fracture took place. Most probably, when the cross section area of lug plate exposed to fatigue was reduced to less then one third of initial area, brittle fracture of both lugs occurred, ending in a collapse.



Figure 18. View of fractured assembly: combined fracture of right lug and brittle fracture of lug, left in Fig. 17 (lower part).
Slika 18. Izgled loma sklopa: kombinovani lom desne uške i krti lom uške, levo na sl. 17 (dole)

This preliminary failure scenario was confirmed /15/ by examination using stereo microscope (Figs. 21, 22).
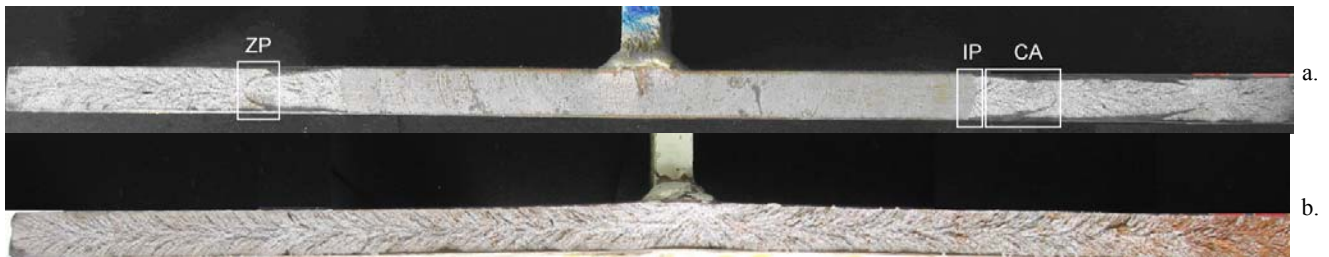


Figure 19. a) Combined fatigue, stable crack growth with shear lips and final brute fracture of upper part.
b) Brittle fracture of lower part, indicating initiation point from both sites (compare to Fig. 18).
Slika 19. a) Kombinovani zamor, stabilan rast prsline sa usnama smicanja i konačni lom gornjeg dela
b) Krti lom donjeg dela, ukazuje na mesto inicijacije za obe lokacije (uporediti sa sl. 18)
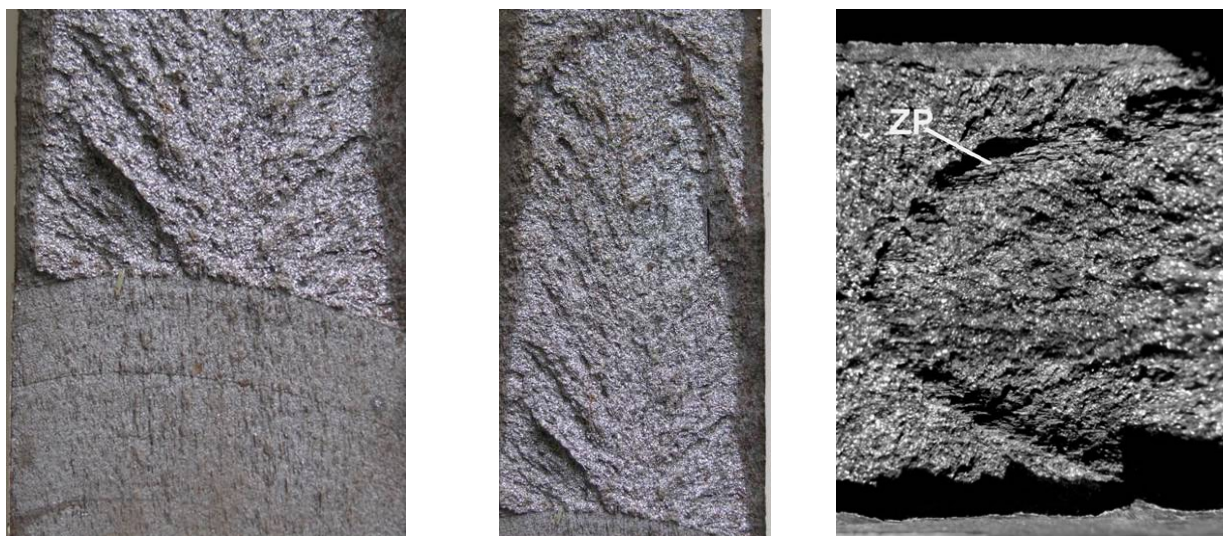


a) Transition from fatigue to stable crack growth, shear lips visible (IP)

b) Extend of crack till formation of first final stretch zone (CA)

c) Second final stretch zone (ZP) before final brute fracture

Figure 20. Typical regions in fracture process, indicated in Fig. 19.
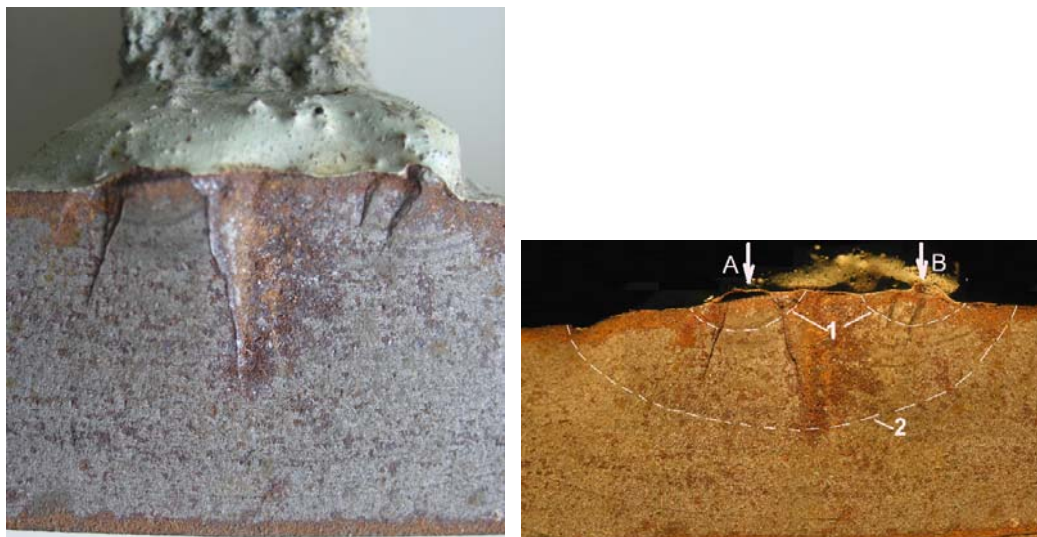Slika 20. Tipične oblasti u procesu loma, vidljivo na sl. 19

Figure 21. Enlarged view of T welded joint (up) and marked initial fatigue crack development (1) followed by final fatigue crack (2).
Slika 21. Uvećan prikaz T spoja (gore) i početak razvoja zamorne prsline (1) praćen konačnom zamornom prslinom (2)

Fatigue fracture initiated in the fusion zone between two weld passes, locations A and B. The size of initiation region approximately equals the thickness of the supporting web (length C, Fig. 22). Ratchet marks LZ were completely in weld metal, with stress concentration due to geometry and weld metal overfill. Beach marks–striations LO determined two dominant locations of crack initiation, marked A and B. Two fatigue cracks developed at these locations (1), after meeting each other and forming one dominant fatigue crack (2).

On the opposite side of weld metal there is lack of penetration, ending in the same region, Fig. 23. At the location of crack initiation microstructural heterogeneity-like border between two passes and the residue of slag was detected. The significance of all imperfections and defects could not be assessed, since welding and inspection documentation was not available. Tested values of impact and fracture toughness of parent metal, steel St. 52.3 (DIN), indicated fracture above nil-ductility transition temperature, corresponding to fracture surface in Fig. 19.
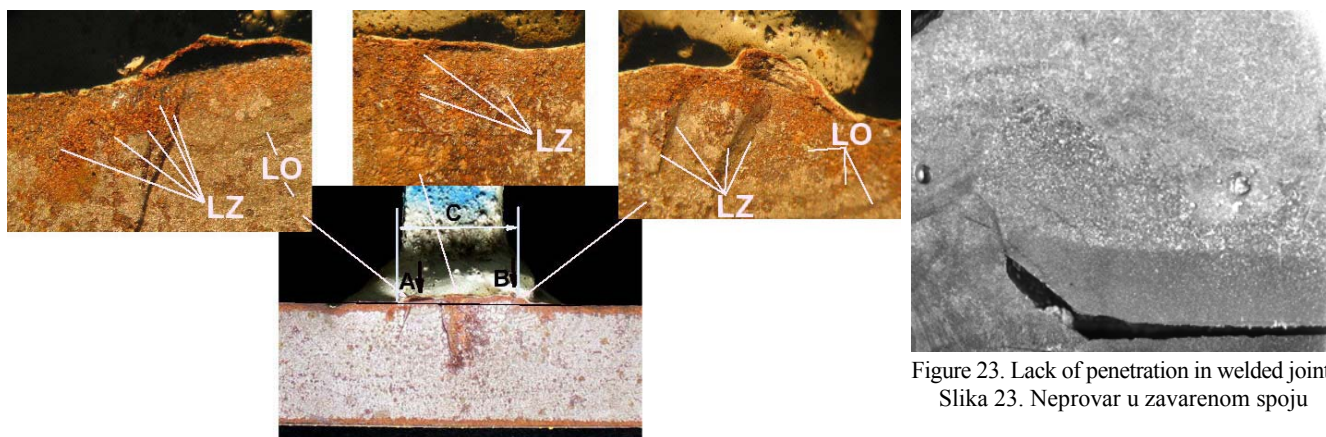


Figure 23. Lack of penetration in welded joint.
Slika 23. Neprovar u zavarenom spoju

Figure 22. Crack initiation: A and B–regions of fatigue crack initiation; LZ–ratchet marks; LO–striations; C–length of crack region.
Slika 22. Inicijacija prsline: A i B–oblasti inicijacije zamorne prsline; LZ–tragovi nazubljivanja; LO–strije; C–dužina oblasti sa prslinom

*The analysis of experienced rotor excavator collapse*

In contrast to three presented cases (ships, pressure vessel, spherical storage tanks) when failure was neither predictable nor understandable, the collapse of rotor excavator was completely clear based on gathered knowledge and experience from previously performed case studies. One can expect cracks in welded joint, fatigue crack growth from existing crack under variable load, further stable crack growth in ductile material and final fracture when components are overloaded. A reasonable question is then why had this collapse occurred. To find the proper answer, the most important influencing factors have to be considered.

The first, not directly connected with constructed excavator, is external load. It is known that operational loading in open mines is variable and random, so in the design and construction this is taken into account through corresponding rules and directives, as it was here the case as well.

Second important influence is quality level of manufactured welded joints. According to performed examination this could be a weak point, responsible for collapse. The probably necessary quality level had not been achieved, both regarding manufacturing and inspection, indicating that requirements for quality assurance were not fulfilled.

The third factor is in-service inspection. Even in the case that in the acceptance process of the structure everything is confirmed regarding documents, it is questionable why periodical inspection did not prescribe NDT of critical welded joint and lug plates. There was sufficient time to repair the structure during the crack growth event, if the crack was detected, and to prevent collapse.

It is not difficult to conclude that ISO 9000 standards were not respected. Thus standards ISO 3834, EN 287 and EN 288 were not properly applied, and most probably independent inspection did not verify NDT results.

DISCUSSION

Significant efforts have been made to prevent structural integrity loss, but this is still an actual problem. The achieved level in theory and experience in design and manufacture of structures, including welded structures, is impressive and respectable. However, we still are witnesses of failures that can be neither predicted nor avoided in practice. The significance of this problem today is extended to new developed structures at micro and nano levels, /15/.

Selected and described failures of welded structures took place in a period of 65 years, with a distance of 20 years between the events. It is to note that the last of them occurred recently (2005). Many recent failures can confirm this statement, like bridge failure in USA in 2007, /16, 17/. Less significant, but a frequent problem is everyday repair in the process industry and oil refineries, /11/. Some features of this problem are worth to be accentuated.
1. Imperfections due to design and manufacturing
2. Final proof of quality before putting in service
3. Properly defined inspection, maintenance and repair
4. Qualification and certification of welding personnel

Simultaneously with welding processes, NDT methods are developed and introduced to document detected imperfections and verify welding quality. Both welding and NDT requirements became inevitable in manufacturing welded structures, based on world-wide accepted standards, codes, regulations and new directives, the European Pressure Equipment Directive (PED) 97/23/EC. However, defect free manufacture of welded structures is costly and probably impossible, /14/, resulting in defect acceptance criteria for welded structures service, based on experience, as presented in the case of pressure vessel and spherical tanks.

In the first three cases, failures were not only unexpected and unforeseeable, but also they could not be explained with the knowledge and experience level at the instances of their occurrence. Significant effort and pretty long time was necessary in these case studies to find out the reasons for failure, and later on to involve gathered cognitions into codes, rules and directives. In the fourth case, rotor excavator collapse, the first visual inspection in-situ was sufficient to explain the failure. The initial crack in welded joint was introduced due to low quality, and they grow by fatigue caused by variable load in rotor excavator operation, then stable crack growth followed, stopped by crack blunting and final brute fracture occurred when the material resistance capacity was completely exhausted. The comparison with ship fracture (first case) has clearly shown similar

origin of failure in poor quality of welded joint. One can conclude that the most important task is how to apply new learned matter in everyday welding practice.

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