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Numerical approach for crack-arrest prediction by FEM

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RESPONDING TO MARKET DEMANDS, strength and toughness of line pipe steels were raised as outcome of research and development within the steel industry over the last decades. At the same time well established tools predicting a required minimum toughness to achieve arrest of long-running ductile fracture in pipeline systems more and more lost their reliability. They have been developed around the 1970's and involve Charpy energy as the key input parameter representing material resistance against crack propagation. As a first pragmatic solution safety factors were applied to a predicted minimum Charpy energy level. Nevertheless, for certain higher grade steels, mostly in combination with elevated operating pressures, uncertainties still do exist. Currently major efforts are put on the development of alternative methods for the control of long running ductile fracture. Within that scope, an FEM based numerical approach on long distance ductile crack-growth in line pipes has been developed at SZMF. It involves characteristic material values determined in laboratory testing which give input to an energy-based cohesive zone model (CZM) representing ductile material damage. The CZM parameters are determined and verified on basis of modified DWT tests and then applied to a finite-element pipe model. The FEM pipe model covers dynamic crack propagation and is used to determine a fracture velocity curve for a BTCM based prediction of crack arrest.

FEM pipe modeling of running ductile fracture – essential constituent parts

As the process of local damage at the crack-tip region and crack propagation itself is difficult to follow in situ, rather practical analysis in full-scale tests is limited to more global measures. These typically are a time record, current crack positions at certain increments by timing wires, pressure data at some positions along the test line, which, at the best, includes an additional pressure profile recording around the circumference at a particular position, and a final crack length. A distinct relation between current local pressure at the crack and its corresponding propagation velocity or even a real measured pressure at arrest can seldom be provided. But this data is essential in properly verifying the results from employed prediction tools quantifying a required material resistance to achieve arrest.

Now with the aim to closer understand the process of long-running ductile fracture in pipelines and to identify its influencing parameters, numerical approaches do play a viable role. All of the above mentioned constituents are also of relevance if numerical modeling of pipe rupture is intended. To which extent shall be discussed in the following, but without making any claim for the discussion to be complete.

Characterising material resistance

Generally it is clear that crack velocity in long running ductile fracture is related to material's toughness. For certain grades and dimensions analytical solutions are able to give good predictions for the possibility to achieve arrest after an admissible length of propagation. The widely used tool for prediction of a required Charpy toughness for crack arrest is the Battelle two-curve method (BTCM) [1]. The applicability is based on a sound data base which was used for calibration of the model back in the 1970's. But obviously, the potential of Charpy toughness as the applied measure for material resistance to crack propagation is limited. This holds true most of all with regard to modern line pipe steels, especially in combination with large diameter pipes and high operating pressures as well as in demanding applications, e.g. rich gas or CO_2 transportation. Also in regular fracture mechanics assessments a Charpy energy value is the least accurate level of toughness quantification. And the situation may not improve when it comes from rather quasi-static conditions to dynamic propagation scenarios. Hence, one of the main questions is the one for the relevant measure of toughness representing material resistance in running ductile fracture.

To link physical measures with material resistance to crack propagation in the numerical approach a damage model is to be implemented. Forerunners of micromechanical damage models are local approaches in fracture [2]. Especially at the time when computer based analysis did come up and allowed for analysis of the stress-strain field in the near crack-tip region, additional local parameters relevant for crack growth were identified. Initially it were far-field characteristic values, like stress intensity factor K or crack-tip loading J which were used to quantify the loading at a crack-tip. Nowadays, various damage models do exist. Depending on the applied damage model, different characteristic values have to be determined and calibrated in order to reflect specific material behavior.

Coming from fracture mechanics applications, the accuracy of damage models initially was focused on millimeter dimensions of crack growth. Whereas in the early years of damage modeling it were rather simple approaches with only few parameters, damage models became more and more complex over the following years, particularly for ductile failure [3], [4], [5], [6]. This development is of course driven by the desire to increase accuracy of crack-growth prediction for different cases of application. For thick wall components also through thickness gradients along the crack front due to local effects were to be predicted. And as knowledge extends and computational power increases, the pretensions are raised at the same time. Focus for damage models nowadays lies on fully covering the morphology of a fracture surface, i.e. shear lips and dimples and so on. Also combining damage models for both ductile and brittle failure, e.g. to cover the transition region between upper and lower shelf behavior, is one of the remaining major challenges under process [7], [8].

But the improvement of accuracy mostly also comes along with an increasing amount of parameters which have to be determined by small-scale laboratory testing. Commonly, various stress states have to be covered at which ultimate combinations of stress and strain to failure are being determined. Each of these tests generally requires an elaborate subsequent numerical analysis in order to determine the parameters reflecting material damage. Or an extensive LOM and SEM analysis may be required which is followed by an iterative numerical calibration process. Cross sectional dimensions of the involved specimens often are only a few millimeters large. The intention is that the occurrence of material damage begins as a local incident. Hence, also the numerical evaluation of parameters needs to be performed very locally. But that may lead to additional challenges especially for applications in the field of thick-wall components. Then typically gradients in microstructure as well as in mechanical properties are to be expected or local incidents, e.g. like separations and splittings, are influencing the stress state during crack propagation. As the latter is majorly influenced by the out-of-plane constraint and the evolving through thickness stresses, specimen size for parameter determination will naturally be of relevance. The general occurrence of splittings and separations, but also their overall content ratio with reference to fracture surface will be size dependent. The involved local change in constraint will influence the applicability of the damage model calibrated with small-size specimens to the full-size component.

Local damage models require local data evaluation. Therefore, meshing in these numerical models has also to be done with suitable (small) element sizes. Generally these parameters then tend to be sensitive to mesh size; which means that meshing in the relevant section of a large component has to be done in a similar scale as for the specimens involved in parameter evaluation. Only then the parameters determined in small-scale testing can be applied also to larger scale structures. As a consequence, mesh size is often termed to be an additional model parameter. Depending on component dimensions this may become a very relevant issue with regard to calculation time. With the aim to overcome this drawback also non-local enhancements have been developed, e.g. [9].

In summary, it can be stated that for numerical simulation of ductile failure many different damage models do exist. They mainly differ in terms of complexity and accuracy and often have sort of a most intended case of applicability. Now it is up to the engineer to decide which tool is best applicable for a certain purpose. A theme supporting to draw a decision could be: As simple as possible, as accurate as needed. And the former refers not only to computational costs, but also to expenses in terms of small-scale testing and model calibration. It is the question for a sufficient close approximation to reality, which of course is case dependent. A comparison of results from three different damage models applied for numerical simulation of ductile crack propagation in line pipes is presented e.g. in [10].

Defining component stressing

The intended purpose of a line pipe system is to transfer liquid or gaseous media over a certain distance. Hence, a primary component stressing is always internal pressure. As operating conditions are specified, this part of component stressing is well defined and can be covered easily during the design. This situation changes as soon as structural integrity of the component is lost and depressurization takes place. Then component stressing has to be determined locally. This holds especially for the case of running ductile fracture in high pressure gas pipelines. Then local stressing in the near crack-tip region is of major importance as crack driving force is governed by the transient pressure distribution close to and behind the moving crack-tip. Therefore it is first of all essential to know the depressurization behavior of the media. But as both current crack-tip position and global pressure level in front of it are time dependent, there is however no straightforward solution to fully characterize the local stressing. And it becomes even more complex as not only the plain pressure profile in longitudinal pipe direction behind the crack is of relevance, but also in circumferential position along the inner pipe wall.

One approach to solve this problem is the use of computational fluid dynamics (CFD) tools. The results from CFD simulations can be transferred to pressure profiles which are then included in solid mechanics simulations, e.g. by finite element methods (FEM). This can be done in a coupled or uncoupled manner. In case of the latter, it supposed to be the deformed structure of course which is to be considered in CFD. As the deformation behavior of the pipe itself is depending on the acting pressure, it will be an iterative process.

Another rather practical approach is relying on recordings by physical measurement. Pressure profiles can be measured in the course of full-scale burst testing. Therefore, parts of the test line are equipped with pressure transducers. As particularly the distribution of pressure along the pipe circumference is of interest, this means substantial efforts. Measurements then are limited to few or often even just one position in longitudinal direction. In between it has to be interpolated (or extrapolated) and assumed that the shape of local pressure decay does not change significantly during global depressurization. The measured pressure profile can be used to fit a mathematical equation expressing the local, transient pressure distribution. This equation can be easily implemented in a numerical FEM model. Remaining challenge then is to define the pressure decay with reference to the current location of the moving crack-tip.

Incorporating backfill effects

When component stressing and material resistance are defined, another influencing factor to be considered is the type of backfill. For buried pipelines it is the soil which is involved if the pipe is deforming during fracture propagation. For an offshore pipeline it is obviously the external pressure by the surrounding water which has to be considered. In both cases backfill has a beneficial effect in terms of component resistance to crack propagation, i.e. the fracture propagation velocity will be lower. In other words, the sustainable arrest pressure will be higher compared to a bare pipe with no backfill. Again different approaches are conceivable to cope with backfill effects.

Omitting mass inertia effects, one way could be to include an external pressure profile on the outside pipe wall. For the case of soil backfill it would be two different portions which have to be considered, namely earth pressure at rest and passive earth pressure. The former is related to deadweight of the soil. The latter becomes relevant as soon as the pipe cross-section opens and tries to expand.

Furthermore, the surroundings of the pipe can be included in the numerical model either in detail or in an idealized manner, e.g. as spring elements. In case of the former, a continuum is build around the pipe and mechanical properties are defined to cover the effect of backfill. For sea backfill supposable even CFD related approaches are feasible.

FEM approach followed by SZMF

After a rather common discussion on relevant issues in numerical approaches to crack arrest prediction, the solutions followed by SZMF are now explained in detail.

General idea of the BTCM is to compare pressure dependent velocities, i.e. depressurization velocity on the one hand and fracture velocity on the other hand. The potential to achieve crack arrest is indicated if, for any given pressure level during depressurization, the fracture velocity is lower than or equal to the depressurization velocity, see **Figure 2.1**. The larger the gap between the two graphs, the earlier crack arrest is to be expected.

Determination of the decompression velocity curve is based on fluid dynamics considerations. It is known to be a quite reliable approximation of reality. The fracture velocity curve is determined by an equation mainly containing pressure, pipe dimensions, material strength, Young's modulus, and Charpy toughness.

The idea now is to substitute fracture velocity as determined by the analytical equation with results from FEM simulations. Hence, maintaining the original idea of crack arrest prediction, the material resistance will now be determined numerically. Unlike to other numerical approaches in that field, the intention here is not to cover a full pipeline as coupling of fracture propagation and decompression velocity is not implemented. Basis of the approach is a simplification on the stressing side. And this simplification is the application of a constant inner pressure in front of the crack tip. Main underlying assumption is that a unique relation between a certain pressure level and the corresponding attained fracture velocity does exist. With the aim to determine a full crack velocity curve, this of course means that several numerical simulations have to be made in order to derive a proper material resistance curve. Each simulation result represents one single point along the curve indicating pressure and corresponding crack velocity.

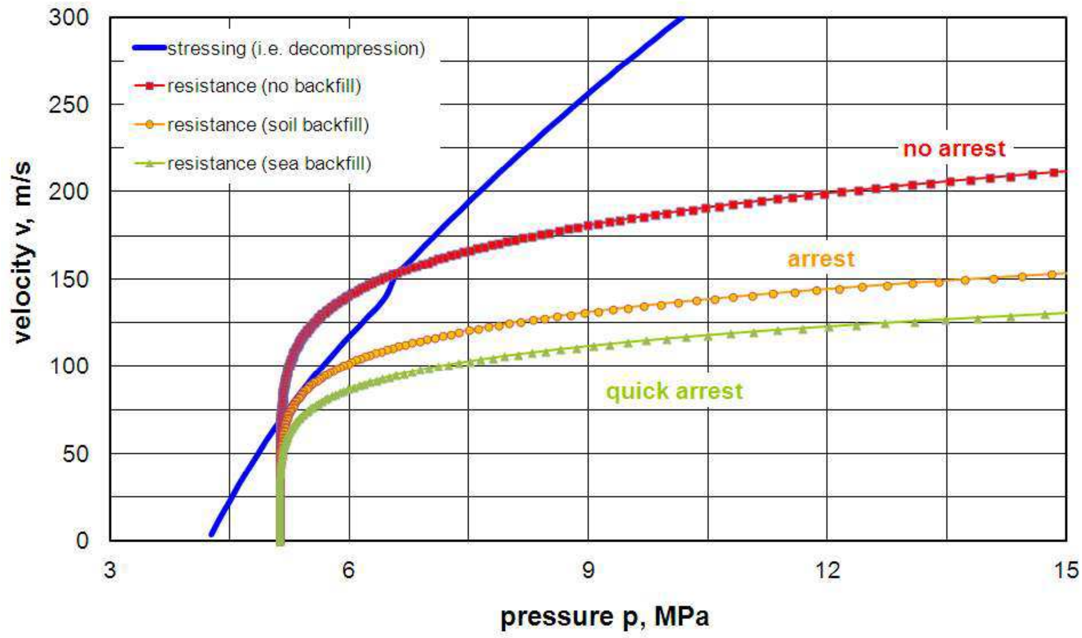


Figure 2.1: Example result of a BTCM prediction for different backfill conditions.

The pipe model – dimensions and numerical aspects

As global depressurization is not covered, the required length of the pipe model to achieve steady state is quite limited. The assigned total length is 6.5 times outer diameter. Within this length, there is first a starter notch of one time outer diameter length promoting pipe opening and initial crack propagation. A section of 5 times outer diameter length follows over which material resistance to crack propagation is covered by a damage model. Finally, the remaining length of half outer diameter is designed as a closed end section. The closed end section is required as otherwise crack velocity starts to accelerate again randomly towards the end of the pipe. A schematic view on the FEM model is shown in **Figure 2.2**. Symmetry in longitudinal direction is taken into account

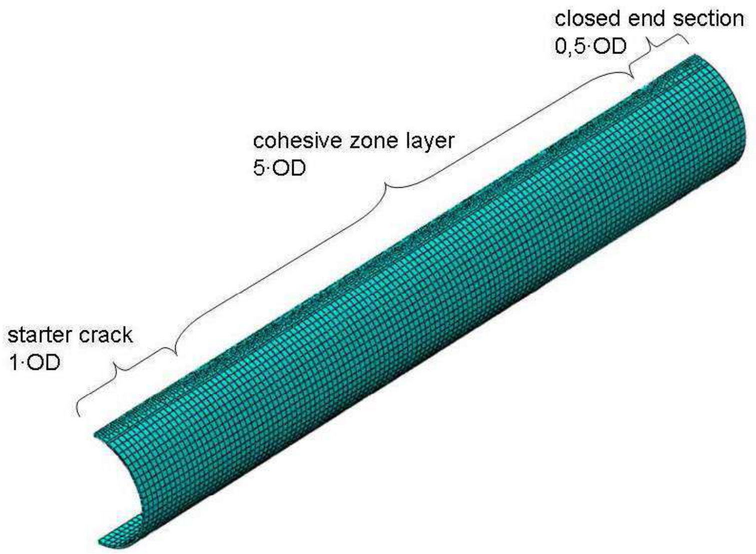


Figure 2.2: FEM pipe model.

In total the model contains 15.346 elements. Element length along crack propagation path currently is 10 mm. The simulation is done with the commercial code SIMULIA ABAQUS from Dassault Systemes, version 6.12. Element type is C3D8 linear. The applied analysis procedure is *implicit dynamic* which also includes mass inertia effects.

Material resistance - the applied damage model

For any numerical simulation of crack propagation, the damage model links local physical material behavior with global component failure. It is essential and of most importance as it particularly defines mechanical material behavior at the ultimate state of stressing. Nevertheless, the calibration has to be covered by small-scale laboratory testing. And of course, most favorably these shall be rather simple tests and preferable with just a limited amount of testing. At the end, it is best if required model parameters can be traced back to either mechanical quantities defining an ultimate state of stressing in small-scale testing or certain characteristic values. The quality of the damage model decides about the value of the results from numerical simulation. The more local the model parameters are, the more sensitive are often the obtained result with regard to mesh size and other numerical issues.

A major advantage in the case of running ductile fracture is that the path of crack propagation is well known. And also the physical issue of crack initiation is not of relevance. The scenario does focus on an already existing through-wall crack advancing in longitudinal pipe direction. Furthermore, the requirements concerning crack shape prediction are less stringent e.g. as in classical fracture mechanics approaches. The morphology of the fracture surface is not of relevance, as long as the involved energy dissipation is quantified properly. And concerning accuracy of crack length prediction, it is typically not about millimeters, mostly not even centimeters of ductile crack growth, but rather on a decimeter scale.

Considering the above, the decision was drawn in favor for a phenomenological based model which is rather simple and contains only a limited amount of parameters. In particular, it is a cohesive zone model (CZM) with a traction separation law as shown in **Figure 2.3**. It is explicitly designed to reflect ductile material damage by means of energy dissipation and contains just three parameters. These parameters are cohesive strength T_0, cohesive energy Γ_0 and critical separation δ_0. Two out of these three parameters have to be known to fully characterize material damage. Additional characteristic points along the curve, δ_1 defining the separation when T_0 is reached, and δ_2 defining the separation at the beginning of strength degradation, are linked to certain fractions of δ_0. Cohesive strength T_0 is determined in a numerical simulation of a tensile test with a notched round bar specimen and is linked to the yield strength. The cohesive energy Γ_0 is quantified by a characteristic value of crack-tip loading J. Typically that is the J-integral at initiation. This parameter is determined in a fracture mechanics test, either via analytical solutions on basis of a global load displacement plot or in a subsequent numerical analysis. Here it is a standard DWT specimen with pre-fatigue crack at a depth ratio of 0.3 which is employed [11], [12]. This ratio is chosen on basis of a constraint analysis comparing the $DWT_{(a/W=0.3)}$ specimen to a pipe with longitudinal through thickness crack at certain levels of crack-tip loading J.

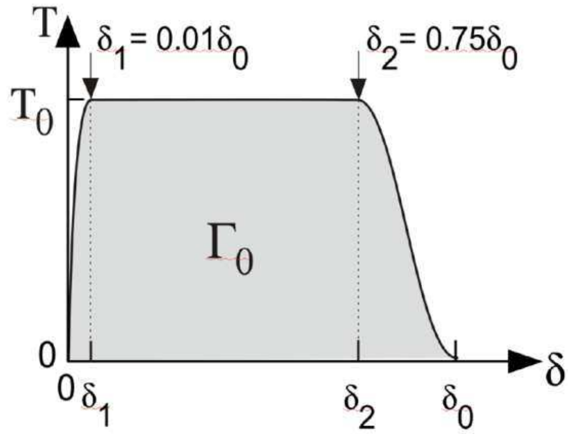


Figure 2.3: Traction-separation law for ductile materials [14].

The J-integral is a non-local value representing a measure for a local ultimate state of stressing at the crack-tip. Determined as a far-field value, it does include any plastic deformation occurring at the crack-tip region. Hence, other than within local damage models, the numerical evaluation of has to be done way outside the process zone, which makes the determination less sensitive to numerical issues like mesh size and element type [13]. For validation of a determined parameter set, reference is made to the global load displacement plot of $DWT_{(a/W=0.3)}$ tests. Therefore, any local microstructure based influences on material toughness, e.g. inhomogeneities or splittings and separations occurring during crack propagation, are implicitly covered. Also mesh size dependency is limited and can be coped with easily by gently adjusting the model parameters. Initial calibration is done at a sub-millimeter level of element size, whereas for application to the pipe model a recalibration is done with a more coarse mesh. All in all this may contribute to the fact that the here applied version of CZM has proven to be a reliable and stable damage model enabling to cover large amount of crack propagation. It was developed by the Helmholtz-Zentrum in Geesthacht (formerly GKSS Forschungszentrum), Germany, and is implemented in ABAQUS as a user-defined subroutine.

Generally, the CZM is a simplified representation of ductile material damage in the process zone ahead of a crack-tip. Referring to the left part in **Figure 2.4**, void nucleation and growth are translated into energy dissipation by the traction separation law. Energy dissipation is maintained until the critical separation of δ_0 is reached. Beyond δ_0 the cohesive element is collapsed, energy dissipation is zero and the crack opens up at the size of the element. The crack propagates along the cohesive interface which separates two neighboring solid sections of material as indicated by the red line in the upper right sketch of Figure 2.4.

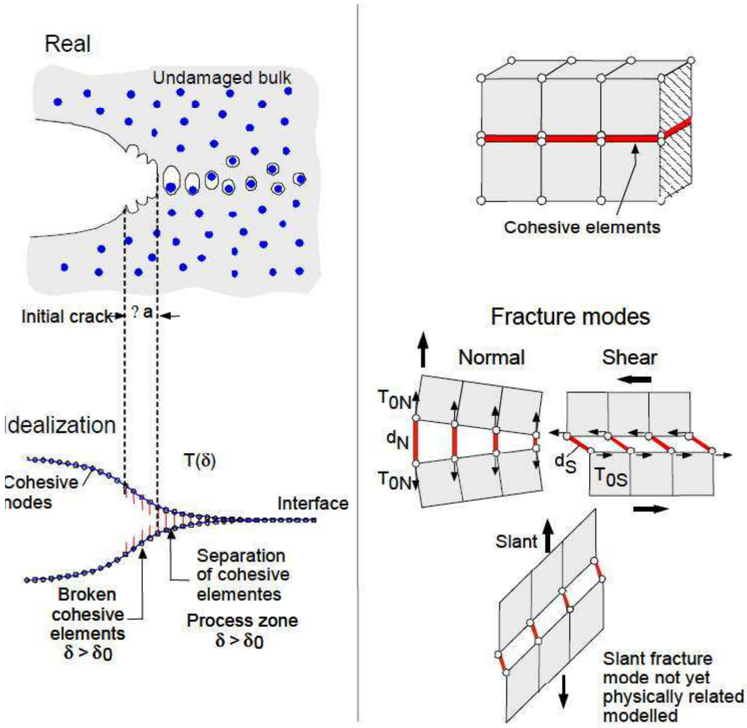


Figure 2.4: Physical damage process of ductile failure and its simplified representation by a cohesive zone interface [15].

Being a simplified approach, the capability of the CZM with regard to fracture modes is limited. As shown on the right side of Figure 2.4, physically only normal and pure shear fracture modes are explicitly covered yet. Nevertheless, on basis of dissipated energy assumptions during fracture propagation, other modes generally can be governed analogously by parameter adjustment with reference to global records of appropriate test results. Even if then under acceptance of potential drawbacks with regard to physical accuracy.

The applied version of CZM contains an extension also considering triaxiality and strain-rate effects [16]. As to be seen in the left side diagram of **Figure 2.5**, generally both T_0 and Γ_0 are triaxiality dependent measures. But focusing on triaxiality levels below two, it is most of all Γ_0 which is influenced. And looking at the diagram on the right side of Figure 2.5 showing strain-rate sensitivity, it is found that mainly T_0 reveals to be rate sensitive. The effect of strain-rate on Γ_0 is very limited.

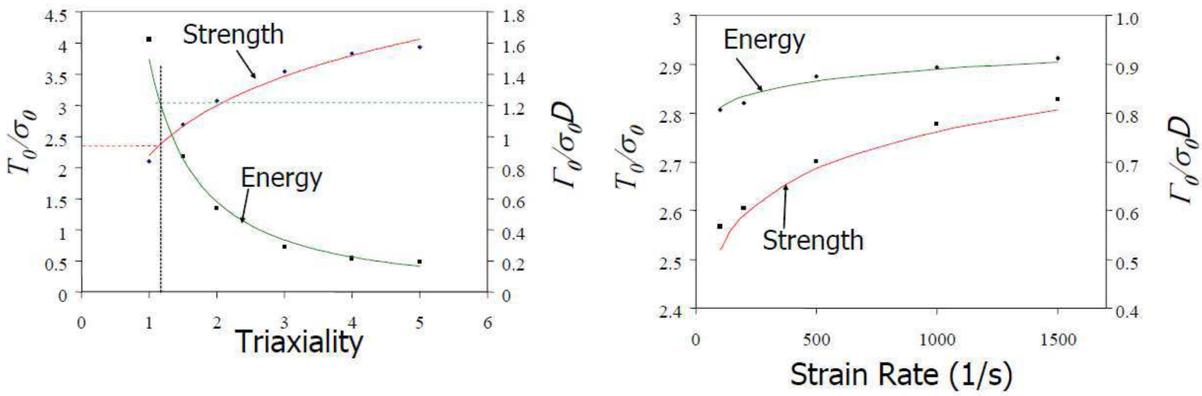


Figure 2.5: Effect of triaxiality and strain-rate on cohesive strength and energy [17].

The extension for triaxiality and strain-rate dependence has yet not been used for the here presented simulation of pipe fracture. These effects are covered via a simplified approach by lifting T_0 and Γ_0 as well as the underlying flow curve by a certain amount. Verification of the modified parameters is done on basis of results from $DWT_{(a/W=0.3)}$ testing at different impact velocities.

Component stressing - defining local pressure decay

Component stressing is one of the most crucial parts within numerical simulation of running ductile fracture. The process of local depressurization in the crack-tip region is highly dynamic. Besides the preceding pressure waves in front of the crack which represent the global pressure decay, it is the local pressure distribution behind the crack along the flaps which is to be defined. Alongside the underlying damage model, stressing is the major measure for the response of the numerical model in terms of pipe rupture. Any uncertainty in the assigned component stressing will have a straight impact on the quality of the numerical result. To which extend has to be evaluated in dedicated parameter studies.

Commonly, global pressure decay in terms of a pressure velocity curve is defined by fluid dynamics approaches. Codes like GASDECOM [18] are able to predict this curve at an adequate level of accuracy. But local pressure decay on the flaps is not generally known. An example result of a measurement during full-scale testing is presented in **Figure 2.6**. This data has been used to derive an equation $p_{(x,\theta)}$ defining pressure in dependence on distance to current crack-tip location x and circumferential position θ.

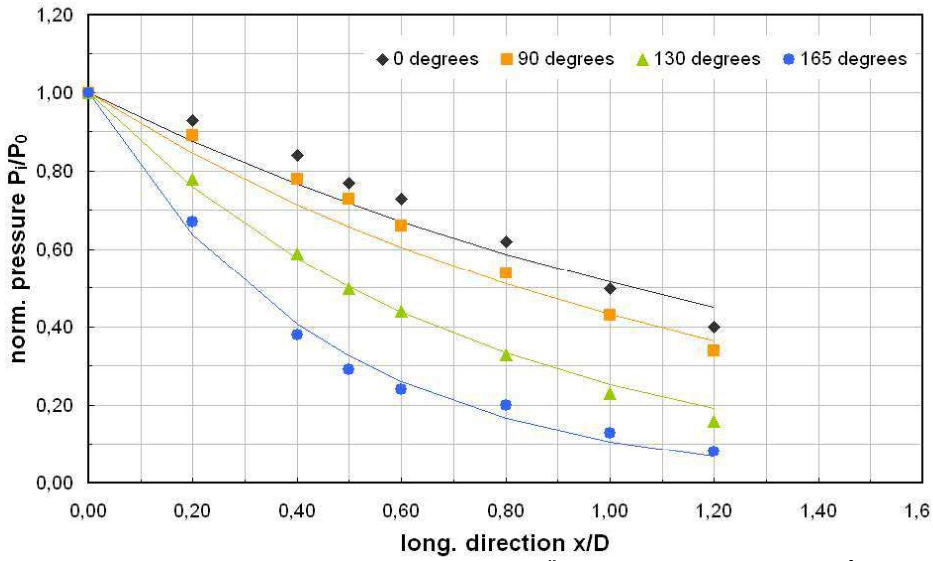


Figure 2.6: Measured pressure along the circumference of a 56" pipe [20]; measured from 0° at pipe bottom to 165° close to crack face.

A three-dimensional plot of equation $p_{(x,\theta)}$ is shown in **Figure 2.7**. Extend of the pressure decay in longitudinal pipe direction x is defined over a certain length with respect to outer pipe diameter D. Circumferential direction is s starting at the opposite side of the crack. Beginning of pressure decay is the current crack-tip position, which is of course time dependent. Therefore, the position of the pressure decay is linked to the current position of the crack-tip and hence is moving along the pipe as fracture propagates.

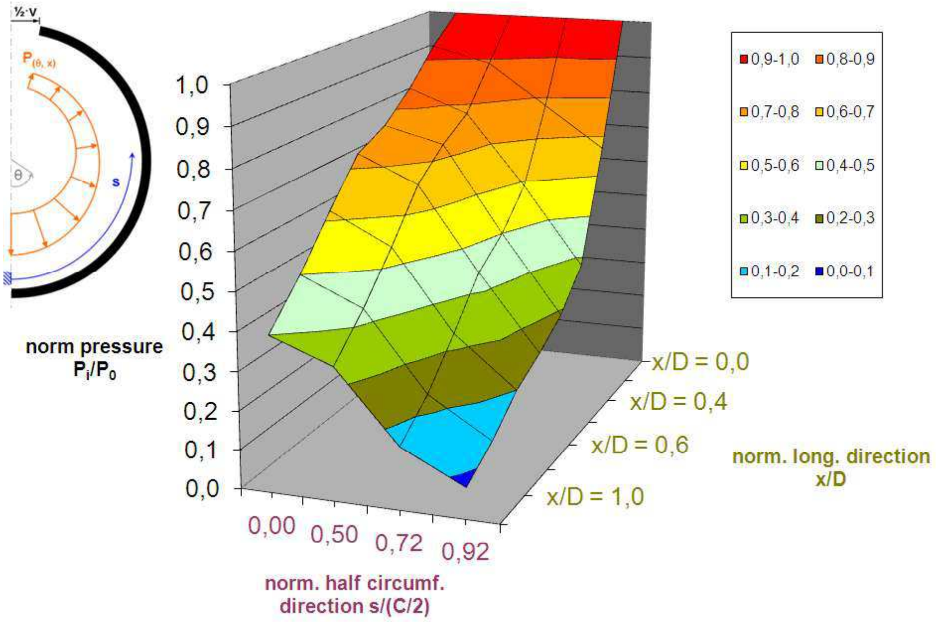


Figure 2.7: 3D plot of the applied pressure decay along the flaps; x is longitudinal, s is circumferential pipe direction.

Procedure for FEM simulation

As the process of running ductile fracture is highly complex, any further admissible simplification within a numerical approach is alleviating both in terms of modeling efforts and calculation time. A main issue for numerical modeling is to incorporate the global pressure decay. It is essential to know the pressure level in front of the crack-tip to be able to quantify the local stressing. But both, current pressure level at and position of the crack are transient values. Best option to overcome this issue would be that the current pressure level in front of the crack simply is known. This idea leads straight to the here applied solution: A certain constant pressure in the pipe section in front of the crack is maintained throughout the simulation. Global pressure decay is omitted, only local pressure decay along the flaps behind the crack-tip is considered. The underlying assumption is that a unique relation between crack velocity and pressure does exist. Now running several simulations at different pressure levels, a numerical resistance curve in terms of crack velocity depending on pressure is determined in an indirect and iterative process. This numerical crack velocity curve is used for a visual prediction of crack arrest as within the classic BTCM approach.

The sequence of the FEM simulation is as follows: In the first step normal displacements along the longitudinal symmetry faces of the pipe are locked, and a constant inner pressure is applied. In the second step, the boundary condition along crack propagation path is deleted; the pipe opens along the starter crack section and resistance against crack propagation is defined via the cohesive elements only, see also Figure 2.2. After initiation the crack accelerates and trends towards a maximum value. Acceleration and maximum crack velocity are both depending on the level of applied pressure.

FEM results – numerical crack velocity curve

Example results from numerical simulations of running ductile fracture at four different pressure levels are shown in **Figure 3.1**. Normalised crack length on the abscissa is related to the cohesive zone section of the pipe model. At 100 % operating pressure p_0, crack velocity trends towards an average value of 254 m/s. At 65 % p_0 the average crack velocity is still at around 228 m/s. At 35 % p_0 the stressing is high enough to open the starter crack, deform the pipe and start crack initiation. But crack arrest happens already after a short distance. Also for 40 % p_0 crack arrest occurs. Hence, for both values the crack velocity is zero.

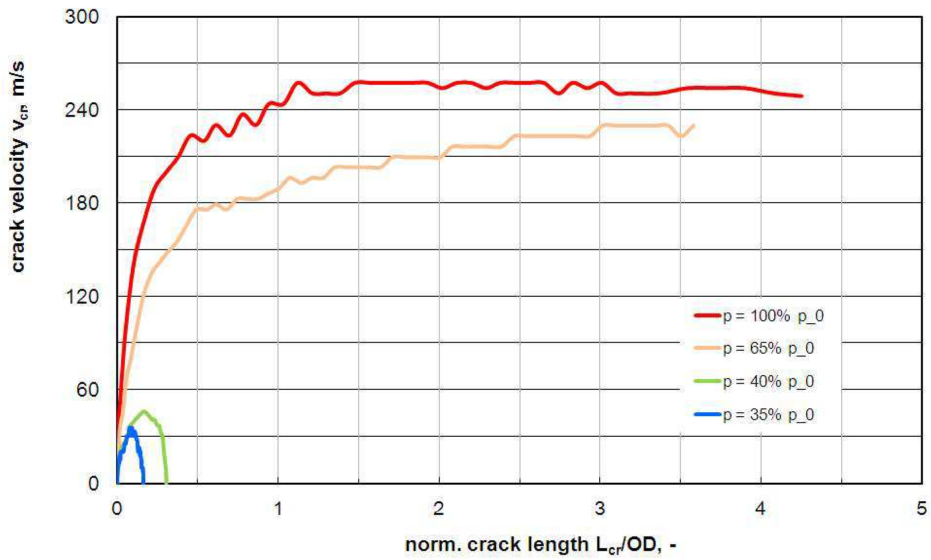


Figure 3.1: Crack velocity evolution in FEM model.

In order to determine a proper material resistance curve for a BTCM prediction, several numerical simulations are required, each defining a pair of values of decompressed pressure and corresponding crack velocity. The numerical crack velocity curve is finally substituting the original analytical solution, see **Figure 3.2**. The typical characteristic shape of the resistance curve is very well represented. For this particular case the quantities of crack velocity are at a reasonable level. The predicted crack velocity close to operating pressure does match quite well with values of around 220 m/s as measured in the first pipe after initiation of a full-scale burst test with similar dimensions, material and stressing, but in backfill condition [19]. Furthermore the shown result indicates a quick arrest, which also coincides with the result from the corresponding full-scale test where arrest was achieved within the first pipe after the initiation pipe.

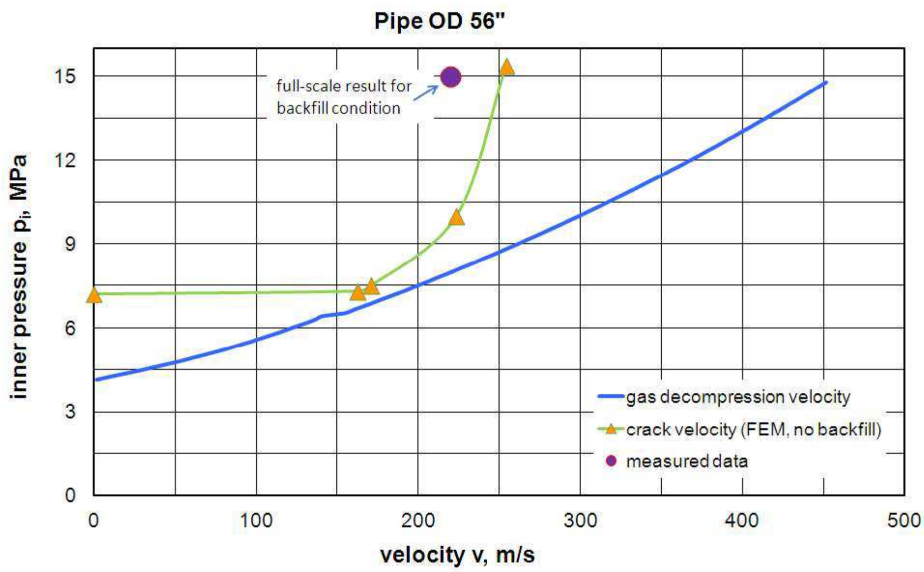


Figure 3.2: BTCM prediction with numerical crack velocity curve (no BF) and data point from full-scale testing (BF) [19].

A plot of the deformed pipe model is given in **Figure 3.3**. Colors indicate the level of plastic straining which is highest in the red zone along the neighboring regions of the section containing cohesive elements.

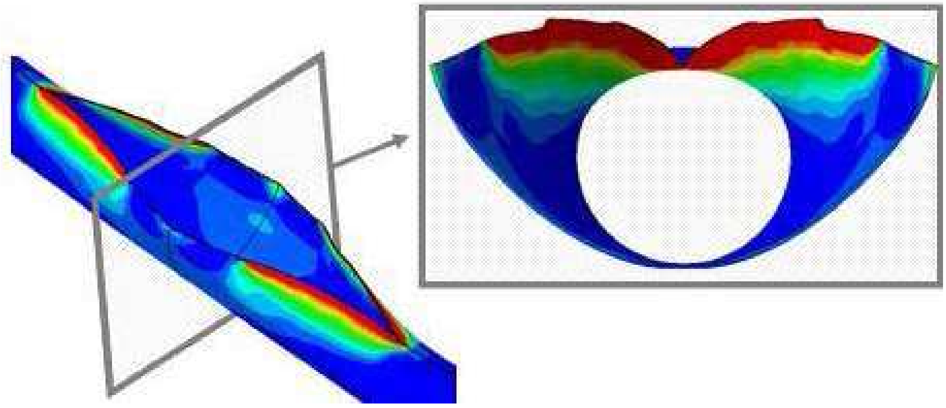


Figure 3.3: Plastic strain distribution in deformed pipe model.

Another important feature in Figure 3.3 is the wave formation along the crack propagation path due to plastic deformation. It is a characteristic feature of running ductile fracture also to be seen in full-scale testing. Intensity of this pattern was found to be highly sensitive to the assigned length of pressure decay along the flaps in the numerical model.

Parameter study - pressure decay length

As mentioned earlier, stressing is one of the main influencing parameters for crack propagation in a pipe. That relates not only to the level of pressure, but most of all to the distribution of pressure on the flaps behind the crack-tip. It was found that the length over which pressure decay happens plays an important role also for the shape of the deformed pipe body in the numerical simulation. That was the motivation to conduct a parameter study investigating the relation between pressure decay length and crack velocity as well as pipe deformation. The equation to define the pressure decay is maintained as given in Figure 2.7, only the length over which the decay happens is varied.

Two different levels of pressure are considered, namely 15.4 MPa and 10 MPa, which is similar to 100 % and 65 % operating pressure. The results are given in **Figure 4.1**. Pressure decay length on the abscissa is normalised by outer diameter of the pipe. The most striking result is that crack velocity comes down to zero if pressure decay length on the flaps tends towards a minimum value. And this minimum value increases as pressure decreases: It is 0.2 OD at 15.4 MPa and 0.6 OD at 10 MPa.

Furthermore the results in Figure 4.1 do indicate that a certain ultimate length of pressure decay does exist, beyond which crack velocity becomes independent of the length of decay expansion. Obviously, a normalised length of 1.2 seems to represent a proper value for the considered case. Then crack velocity does not further increase if the length of pressure decay is increased. This result assists in defining a proper dimension in terms of minimum length for the pressure decay in the numerical model. And it does also coincide with the measured pressure profile as shown in Figure 2.6.

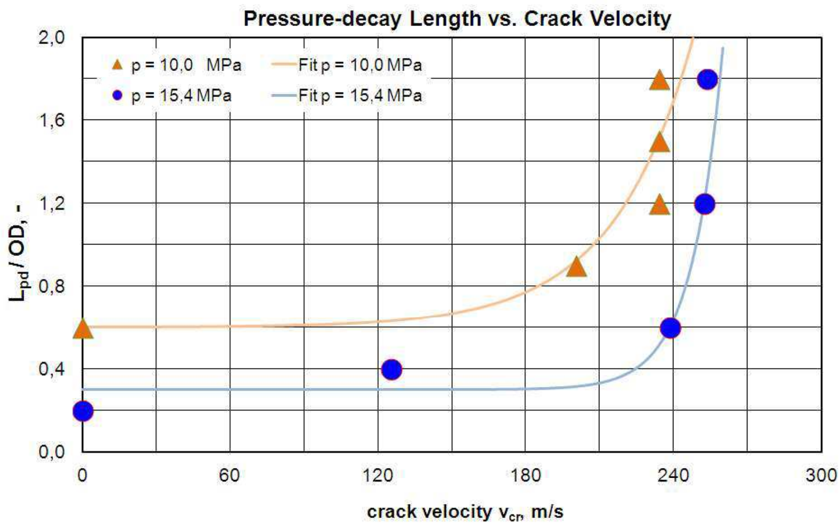


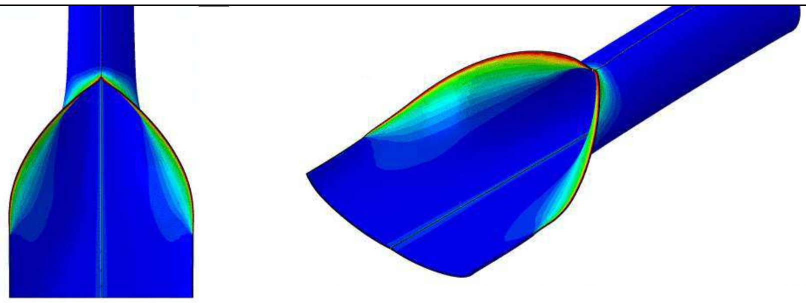
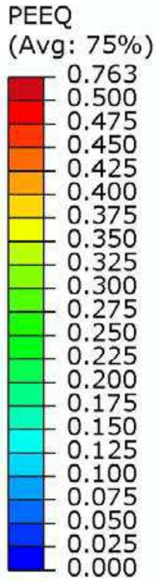
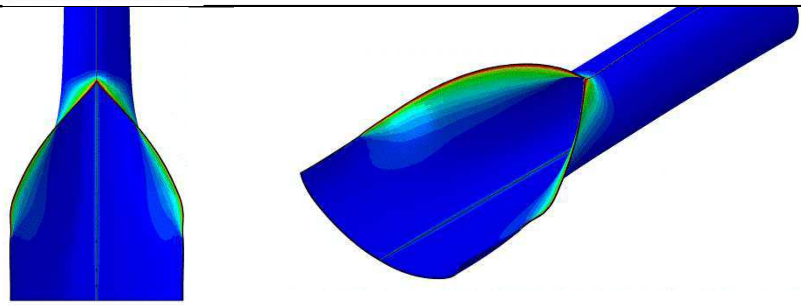
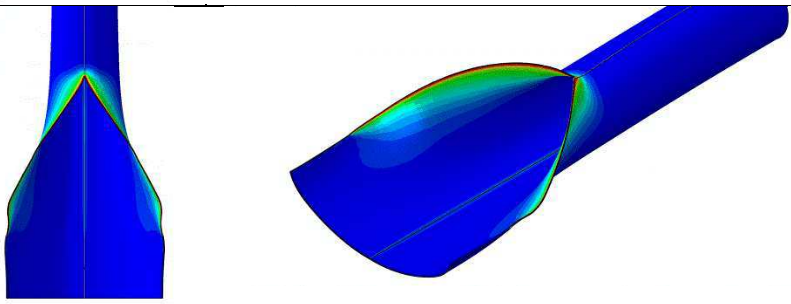
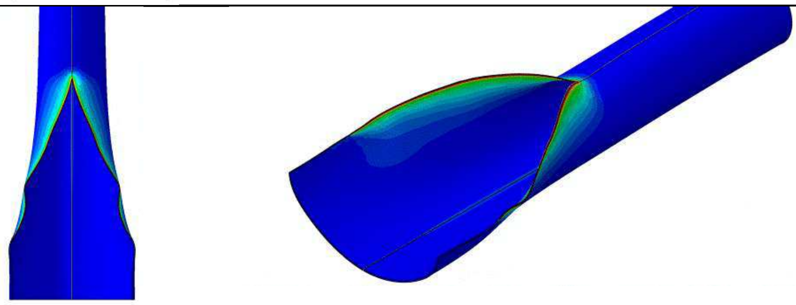
Figure 4.1: Pressure decay length vs. crack velocity.

A study of the impact from defined reference length of pressure decay on the deformation of the FEM pipe model at 10 MPa is shown in **Table 4.1**. Reference length is decreasing from 1.8 OD at the top down to 0.9 OD at the bottom. At 0.6 OD crack propagation does not occur. Plotted is the deformed structure at a crack propagation length of about two times OD. Looking at the plots it is evident that pipe opening is depending on the applied reference length: The larger the length of pressure decay, the wider becomes the pipe model in the top-view plots. And the less curvature remains, as to be seen in the isometric views.

Furthermore, formation of wave patterns along the crack path is also depending on reference length. From earlier results it is already known that if pressure decay is omitted and full pressure is applied, then the pipe deforms smoothly without any additional lateral plastic deformation along the crack path. Hence, the defined pressure decay on the flaps is triggering the wave formation. As to be seen in Figure 4.1, the lower the reference length, the more pronounced is the wave formation. It even seems to disappear already at a reference length of 1.5 OD which therefore could be interpreted as a potential maximum length of pressure decay. This idea matches with an ultimate value of 1.5 times OD which was confirmed for a similar setup in a publication by Arabey et al. [19].

Again this result assists in defining a proper reference length for the pressure decay. Length of wave formation can be compared to a deformed pipe after full-scale testing and hence serve as an additional verification for assumptions in the numerical model with regard to pressure decay on the flaps.

Table 4.1: Deformed FEM model and flap formation at 100 bar inner pressure.

L_{pd}/OD = 1.8:		
L_{pd}/OD = 1.5:		
L_{pd}/OD = 1.2:		
L_{pd}/OD = 0.9:		
L_{pd}/OD = 0.6:	*No crack propagation achieved.*	
Distribution of plastic strain PEEQ in deformed FEM model during dynamic crack growth for different reference lengths of pressure decay L_{pd}.		

Summary and outlook

With the aim to find an alternative procedure for crack arrest prediction, the original idea of the classic BTCM approach is maintained but enhanced by involving results from numerical simulation. The analytical solution to determine crack velocity is substituted by a numerical procedure defining a FEM based crack velocity curve. Core part of the novel procedure is an optimized cohesive zone model for ductile failure representing material resistance. The required model parameters, namely cohesive energy and cohesive strength, are determined in small-scale laboratory tests. Cohesive energy is related to crack-tip loading J, which is determined in a fracture mechanics test using a DWT specimen with pre-fatigue crack at a crack-depth ratio of 0.3. Cohesive strength is determined in a notched round bar tensile test. Therefore, the two model parameters have a straight relation to mechanical material values. Final calibration is done on basis of drop-weight test results with the pre-fatigue $DWT_{(a/W=0.3)}$ specimen.

Component stressing in the FEM pipe model is defined on basis of measured data of a full-scale test. A certain pressure decay is defined behind the crack-tip. It is acting over a particular reference length along the flaps in longitudinal pipe direction. Also pressure decay in circumferential direction is considered. This pressure decay is linked to the current crack-tip location and moves along the pipe as the crack propagates. By neglecting global depressurization and maintaining a fix level of pressure in front of the crack, a steady-state condition is attained in the FEM model and a corresponding maximum crack velocity is determined. By running a series of FEM simulations at different pressure levels a numerical crack velocity curve is determined.

In a final parameter study the relation between global pressure level in the pipe, length of local pressure decay on the flaps and corresponding crack velocity is investigated. These results give viable input for modeling details regarding definition of component stressing.

Currently this procedure is extended to include the effect of backfill on crack velocity. Other dimensions and grades are under process. Finally, the numerical results shall be compared to real full-scale testing data in order to verify the applicability of the proposed procedure.

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