Impact from ice floes and icebergs on ships and offshore structures in Polar Regions

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Abstract. The principles for ULS design and ALS design for offshore structures are discussed. The use of pressure-area curve versus force-area curves for analysis for estimation of ALS impacts is deliberated. Aspects of local and global shape of the ice feature are discussed in view of external mechanics (demand for energy dissipation) and internal mechanics (local damage). Simplified methods for structural damage assessment are reviewed for ice loads that may move both transverse to and along the shell plating. Material modelling of ice for nonlinear finite element analysis (NLFEA) of ice-structure interaction is reviewed. The material models must be calibrated against design curves and the sensitivity of the material parameters with respect to mesh size is investigated. Results from simulation of impacts of ice with different shapes on the column of a floating platform are presented, and the critical shape for penetration of the front plating for the present structural configuration is identified.

1. Introduction

Oil activity, shipping and cruise traffic in Arctic regions increase, partly sparked by global warming. This instigates safety concerns with respect to environmental pollution, fatalities and economic loss. With large distances to infrastructure it may be challenging to assist in case of critical events. Structural damage due to impacts from ice floes and icebergs may become fatal if excessive flooding and loss of stability occur. Ships and oilrigs operating in permanent ice cover will need to be ice-strengthened. Lightly ice-strengthened or non-ice strengthened structure may operate close to the ice edge or may need to move into light ice-conditions, e.g. during search and rescue operations.

The design against ice may be carried out in all general limit states, SLS (acceptable deformations), FLS (cyclic ice actions), Ultimate Limit State (ULS) and Accidental Limit State (ALS) In most cases the assessment of the resistance to ice action is carried out in ULS and ALS (for offshore structures, ISO 19906 [1], ALS design is mostly indirect for ships. ULS design (annual probability of occurrence typically $10^{-2}$) is important for structural safety. This control ensures that all foreseen loads can be resisted without damage with an adequate margin. In ALS design (annual probability of occurrence typically $10^{-4}$), damage to the structure is allowed as long as there is sufficient residual strength to prevent progressive collapse, and the safety of the crew and environment can be maintained.

Three design principles may be applied to enable structures to resist accidental or abnormal loads including extreme ice actions from impacts with ice floes and growlers/bergy-bits : strength design, ductile design and shared-energy design (see Figure 1).

Strength design assumes that the ice will dissipate most of the collision energy and that the structure will undergo only limited yielding and small permanent deformations. This assumption is virtually identical to that used for ULS design.

Ductile design assumes that the entire collision energy is dissipated by the structure. If the collision energy is dissipated with moderate damage to the structure (e.g., no penetration of cargo tanks or buoyancy compartments essential for hydrostatic stability) and with satisfactory residual strength, the ALS acceptance criteria may be satisfied.

Shared-energy design implies that both the ice and the structure undergo significant deformations and dissipate energy. At any instant, the weaker structure (or ice) will deform.
Figure 1 Characterization of design principles in terms of the share of energy dissipation versus relative strength of structure and ice

1.1. Pressure-area relationships

ISO 19906 specifies that design for ULS ice actions and ALS ice actions shall include both local and global actions. Global actions represent the total action on the structure and are applied over the nominal contact area, while local actions are applied to specific areas within the nominal contact area.

According to ISO 19906 [1], the ice pressure distribution for massive ice features is highly non-uniform within the nominal contact area and is rapidly changing during ice-structure interaction. Ice at the boundaries may spall off reducing the actual contact area relative to the nominal contact area. Smaller areas within the actual contact areas will be subjected to pressures considerably larger than the average pressure. According to ISO 19906, this should be addressed by assessing local design separately using pressures representative for the high-pressure zones. The actions are often given as local pressure-area ($p-A$) relationships ($p = CA^e$ where $C$, and $e<0$ are empirical coefficients). This is relevant for structures undergoing small or very moderate plastic deformations, which is normally assumed in ULS design.

$p-A$ relationships are also often specified for ALS design (e.g. ISO19906 [1]). Since the $p-A$ relationships depend on the recurrence period and they will be higher in the ALS than in the ULS as illustrated in Figure 2. The application of $p-A$ curves is, however, not straightforward in ALS. A principle sketch of ULS and ALS $p-A$ relationships is shown in Figure 2. The resistance domains for a structure (i.e. the structural capacity versus loaded area) are also illustrated in Figure 2. There is no clear transition between them so they are overlapping. In ULS the resistance must be in the strength domain as the behaviour shall be essentially elastic with only moderate plastic deformations. When the $p-A$ demand increases, the structure will undergo finite deformations and thus move into the shared energy domain or, ultimately, into the ductile domain. In both cases the structural resistance will govern the interface pressure; i.e. plate and stiffeners may deform before the interface force reaches the local AL pressure. This implies that the ice maintains its shape in the local high-pressure zones, the local indentation into the structure increases slightly locally, but simultaneously the contact area expands and mobilizes more structural members. Consequently, in the ALS, we are not so much concerned about the resistance to local, rapidly varying pressures, but rather the resistance to the global ice actions.

It is, of course, possible to strengthen the (offshore) structure so that the strength domain becomes larger than the ALS pressures so that the same ULS strength check techniques can be utilized, but this is essentially not required if the structure can survive the ice load with the calculated ALS damage and also has sufficient residual strength.
1.2. Damage and energy dissipation

The principle of estimating the damage and energy dissipation in the structure is illustrated in Figure 3, which is reproduced from ship collision with offshore installation according to DNVGL RP-C204 [2], but here adapted to ice impacts. The basic ice force-deformation relationship, $F_{\text{ice}}$, is the force that would be obtained when the ice is crushed against a rigid structure. $F_{\text{ice}}$, should essentially incorporate the ALS $p$-$A$ curve and would correspond to that obtained when the resistance is in the strength domain. Conversely, assuming the ice to be rigid, the force-deformation curve for the structure can be calculated. The resistance, which is in the ductile domain, will depend on the assumed shape of the impacting ice feature. Since the force acting on the ice and on the structure must be equal, the resulting damage is obtained when the energy dissipation (equal to the area under force-deformation curves) reaches the demand for energy dissipation, as required by the external mechanics analysis.

It is obvious that this approach is very simplified and does not account for interaction effects: when the structure undergoes finite deformations locally, it tends to “wrap around” the ice feature, which becomes more confined and the pressure needed to crush it is likely to increase. This is illustrated by the dotted force curve for the ice. Conversely, if the structure is strong enough to crush the ice, the contact area becomes blunter and larger; consequently, the resistance increases compared to that for rigid ice as shown by the dotted resistance curve. This demonstrates the complexity of the damage process when the resistance is in the shared-energy domain; increasing structural deformation or ice crushing may switch forth and back depending on which body that is the instantaneously the stronger one. The ISO 19906 ice $p$-$A$ relationships have been derived using force time histories and converting force values to pressure. The design curves are based on the peaks of the force/pressure data. In addition, the pressures are based on the mean plus three times the standard deviation for each contact area. For contact areas larger than approximately one square meter, the pressure values drop so fast ($A^{-0.7}$) that it is questionable whether they are can be used for ALS assessment. Kim et. al [3] argue that the physical limits to the energy absorption capacity of ice become more important than the
underlying pressure-area relationship. It may therefore be useful to consider the amount of energy spent on crushing a unit mass of ice.

**Figure 3** Estimation of force and damage (deformation) for ice-structure impacts adopting the approach for ship collision (DNMVLG RP-C204, 2019). Ice force curves to the left, resistance curves to the right. $E_{s,ice}, E_{s,str}$ – strain energy dissipation in ice and structure, $w_{ice}, w_{str}$ – deformation of ice and structure, $F_{ice}$ – ice force, $R_{stru}$ – structure resistance.

For fully confined iceberg ice at Pond Inlet (Masterson et al. [4]), when the effect of the sample size and boundaries are minimized, the specific crushing energy is maximum 6.09 kJ/kg (average value being 2.96 kJ/kg). This value was derived using force-time history records obtained in the Pond Inlet tests with 900, 1280 and 2300 mm radius indenters. While the specific crushing energy value can be used to calculate the energy spent on ice crushing, the process $p$-$A$ relationship during an ALS event could be also established. The question is: can we use the ISO curve with $C=7.4$ and $ex=-0.7$? The majority of the data for this curve comes from the Pond Inlet tests on iceberg ice with 2300 mm radius indenter. The pressures obtained in the Pond Inlet test set are a series of maxima during individual tests (four tests in total), the data were binned according to the fully confined contact area and $p = 7.4 A^{-0.7}$ is the mean plus three standard deviations. Thus, it is questionable whether this curve can be used in the context of ALS. Instead, the process $p$-$A$ relationship underlying the local ice-load calculations PC 3 vessels in IACS PC code [5] (i.e., $p = 3.2 A^{-0.1}$) was found to better represent the physical value of the energy absorption of iceberg ice during crushing and spalling failure. The experimental data and force curves corresponding to crushing energy of 2.96 kJ/kg for the Pond Inlet tests (2300 mm radius indenter) are shown in **Figure 4**. Hence, if the force curve $F = 3.2 A^{0.9}$ is used in ALS design, the local pressures and forces will be smaller than the ISO 19906 $p$-$A$ curves for $A < 4 m^2$, and larger for $A > 4 m^2$.

Thus, when ice material models are used in finite element analysis it is recommended that the ice models be calibrated/verified against force deformation curves (e.g. $F = 3.2 A^{0.9}$) and not $p$-$A$ relationships. Ideally, the ice material models should be calibrated by trying to replicate the tests conditions. In the Pond Inlet tests a rigid, spherical indenter was pushed into an ice block, so the ice was subjected to considerable confinement. In the case ice impact with a structure the degree of confinement is more uncertain and is likely to be less, (unless the structure is subjected to severe deformation). It is likely that the force crated by crushing is smaller, but experimental evidence is
lacking. A more careful approach is to calibrate to the force curve for ice crushing against a plane, rigid surface.

![Force indentation curves](image)

**Figure 4.** Force indentation curves in Pond inlet tests and “average” force curves

### 2. Impact geometry

It is very important to distinguish between local and global geometry of the ice feature, refer Lu et. al [6]. The **global** geometry is decisive with respect to the mass of the ice feature as well as the exposure to collision of the various parts of the structure, i.e., the column and the floater. In addition it is governing the demand for energy dissipation, which is expressed as a fraction of the total kinetic energy at the instant of collision. The local geometry is essential with respect to the resistance of the ice to deformation (crushing, extrusion etc.) at the contact point.

![Impact geometry illustration](image)

**Figure 5.** Left: Illustration of global geometry for collision between a large ice feature (5-10,000tons) and the column and pontoon of a semi-submersible platform in still water. The hatched areas indicate possible contacts where crushing will take place and where information of local geometry is needed. Right: Collision with a small ice feature.
Figure 5 illustrates the importance of global geometry for the exposure of a column and pontoon of floating platform at still water (or relatively small waves). The pontoon can only be hit by ‘large’ ice feature at still water. The collision will in most case be eccentric, i.e., the force vector has a large arm with respect to the centre of gravity (CG), and substantial energy may be transferred into pitch/roll motion of the ice feature. This reduces the demand for energy dissipation. The same holds true for collisions around water level.

Depending on the ice features shape, collision may take place at intermediate depths and may be close to the CG; and most of the kinetic energy must be dissipated by deformation of structure and/or the ice feature. For smaller ice features, like the spheroidal ice in Figure 5 right, only the column may be exposed to collision, which in this case is centric. It is true that the mass of the ice feature here is small; but it is also prone to larger wave induced motions leading to considerable collision speeds. No agreed global shape for bergybits/growlers exists at present and the shapes used in various studies differ greatly. An interesting case is shown in Figure 6. The ice is washed out by the waves in the waterline and gets a “foot” that protrudes significantly from the visible part above waterline. This shape has been of particular concern to seal hunters according to Gudmestad and Alme [7].

There is also no common agreement on the local shapes that should be used for ice features. The issue of local shapes is illustrated in Figure 7. The ice has a small protrusion. If this is not crushed it may be detrimental with respect to indentation and puncturing of the shell plating between adjacent frames of the structure. If the ice is crushed ice becomes more blunt and the worst shape that should be considered could be the one with large curvature; this could prevent penetration of the shell plating and allow contact to spread over several frames. Evidently, the shape the local ice shape should be considered in view of the structural layout in the contact area.

Other potential ice geometries could be shaped as discs representing ice floes, but only impacts from spheroidal ice is analysed in this paper.

2.1. The effect of relative motions
The presence of waves induces oscillatory motions in six Degrees of Freedom (6 DOFs) for both the ice feature and the structure. Depending on the motion characteristics, the vertical exposure of the structure will vary in different wave conditions. This is illustrated in Figure 8.

If only the horizontal motions of the ice are considered, the likelihood of hitting the pontoon in a wave trough will most likely be considerably smaller than the likelihood of hitting the column in a wave crest. This depends on the mean drift of the ice feature versus the relative wave induced surge/sway motion of the ice feature and the structure. On the other hand, vertical collision on the pontoon due to the relative heave motions can be significant.

Lu and Amdahl [8] investigated the drift, impact velocities and vertical collision exposure for a small, spheroidal iceberg hitting a semi-submersible platform. They adopted the method introduced by Fylling [9] for drifting ship impacts. Figure 9 shows the temporal variation of the track when the mean drift speed is small. Impact will only occur when the sphere advances towards the platform indicated by the red lines. Obviously it is more likely to hit the platform during a wave crest and at a high location.
Figure 8. Illustration of potential global geometries for collision between "large" ice feature (5,000-10,000 tons) and structure’s column and pontoon in waves. Upper sketch for high position: collision close to wave crest, lower sketch for low position: collisions close to wave trough.

Figure 9. Simulation with coupled motion track and potential impact events for slow current velocity (0.05 m/s.) [8]. The red part of the track indicates when horizontal impact is possible.

Figure 10 shows an example of the probability distribution for the vertical location of impact with the column and associated values of the impact speed at various probability of exceedance levels. The most likely place to hit the column is below still water line. The largest impact speeds occur at a high level, however, the probability of occurrence is small.

The study by Lu and Amdahl [8] was based on linear wave theory. Wave drift forces were not calculated, the nonlinearity of the vertical restoring force was accounted for as well as change of the hydrodynamics when the ice feature is close to the column. Extensive hydrodynamic analysis of the ice impact was carried out by Ommami et. al. [10], where among others the nonlinearity of the Froude-Krylov force was taken into account and the repellent action when the ice approaches the column was accounted for.

In a follow-up study [11] the method used by Lu and Amdahl [8] was improved in the sense that the restoring force, hydrodynamic added mass, the Froude-Krylov force and drift force all were based on the actual submersion of the glacial ice, which for simplicity was assumed to have a cubic shape in this case. The improved analyses remove some conservatism form the previous calculations, and the impact speeds are of similar order of magnitude.
2.2. External mechanics

The external mechanics deals with the rigid body motions of the ice and the structure during impact. It is very favourable when the external mechanics can be separated from the structural deformation estimation, because the latter, commonly denoted internal mechanics, can be determined by a single nonlinear finite element analysis of a given impact scenario with detailed local models of the structure and the ice. The result of the external mechanics is the demand for strain energy dissipation, which is determined by the internal mechanics.

If the impact is not centric part of the kinetic impact energy will be transferred to other forms of translational and rotational energy, reducing the demand for strain energy dissipation in the structure and the ice. Popov et al. [12] presented a 6DOF+3DOF model for ship and ice floe collisions. It was assumed that the impact impulse always acts in the normal direction at the impact point, and the friction between ship and ice was ignored. A “reduced mass” concept was introduced to simplify the 6DOF+3DOF problem to a 1DOF +1DOF problem. Popov’s model has been extensively used to calculate the effect of ice loads for ship navigation in ice-covered waters; see e.g. IACS [5], Daley [13, 14] and the SSC report by Dolny [15]. The SSC report acknowledged that Popov’s model presupposes that the collision process is quick, that there is no sliding along the hull, and that sliding should be considered for more severe limit states. Daley [13] noted also the limitation of Popov’s model in handling sliding impacts. Liu and Amdahl [16] removed this limitation and presented a new approach to multi-planar ship-ice impacts. It is based on the theory developed by Stronge [17] and can handle motions in 6DOF for both the ship and the ice. Closed-form solutions for the dissipated strain energy were obtained explicitly for the so-called “stick” and “slide” mechanism.
The method is illustrated for ship–ship collisions as illustrated in Figure 11 but is also relevant for structure–ice collisions. The method was formulated by considering the equilibrium and conservation of momentum in the directions normal and parallel to the tangential collision plane ($n_1, n_2, n_3$) in Figure 11. Closed form solutions for the required strain energy dissipation during impact was determined for two different cases, depending on whether the ice sticks to the structure or whether it slides along the side for a given limiting friction factor. The friction factor concerns generally “normal” steel-to-ice friction but it could also be increased to take into the increased force to transverse deformation of the ship side. From numerical simulations of grounding Alsos [18] and, from ice impacts, Quinton [19] observed that the normal force during sliding is approximately half of the value required for lateral penetration of the structure to the same indentation level. They also found that the penetration force is virtually independent of the friction factor, while the sliding force is very sensitive to it.

3. Constitutive modelling of ice

Constitutive modelling of ice seems to follow two tracks; the crushable foam model and plasticity based model. The crushable foam model was developed was used in numerical simulations of a ship–ice collision and to estimate the damage to the vessel; see Gagnon [20] and Gagnon and Wang [21] and LSTC [22]. In this model, the material strength increases as it deforms, and this is viewed as an analogy so that the essential behaviour of the actual ice can be realized in the simulations. The analogue model ensures that a relatively small region in the centre of contact experiences high pressure (a hard zone) and the surrounding contact material exerts a somewhat lower pressure (soft zone) – an established fact from various experimental studies (Gagnon [23]; Riska et al. [24]. The unknown material parameters were determined through trial and error by comparing the results from simulations of full-scale ship/bergy bit impacts with the actual field data (i.e., the actual peak load, impact duration and contact pressures). The predictive capabilities of the model were demonstrated by simulating growler impact tests at laboratory scale. The model successfully produced reasonable values for the total load and pressure when compared with the instrument measurements.

Plasticity based modelling has been adopted by several authors, e.g. Kierkegaard [25], Liu et. al [26], Shi et. al [27] and Xu et. al. [28].

3.1. Plasticity based modelling

The continuum mechanics model for ice developed by Liu et al. [26], was intended for practical design of structures against abnormal ice loading. It consists of an elastic-plastic isotropic material model, where the yield stress is dependent on the hydrostatic pressure so as to represent ice confinement. The model has the advantage of capturing several ice deformation characteristics. It was implemented in the explicit finite element software LS-DYNA. The behaviour of ice is modelled by
means of the ‘Tsai-Wu’ elliptic yield criterion [29] and a strain based failure criterion that is dependent on the hydrostatic pressure. The yield surface is a function of both the second invariant $J_2$ of the deviatoric stress and the hydrostatic pressure $p$:

$$f(p, J_2) = J_2 - \left(a_0 + a_1 p + a_2 p^2\right) = 0$$

where $a_0$, $a_1$, and $a_2$ are material coefficients to be specified by users, the values adopted in subsequent calculations are those suggested by Kierkegaard [25] $a_0 = 2.588$, $a_1 = 8.63$ and $a_2 = 0.163$. To avoid unreasonable tensile pressure a cut-off criterion equal to -2 MPa was adopted.

In order to simulate the mechanical effects of ice crushing, an empirical failure criterion is adopted based on the effective plastic strain $\varepsilon_f$ and hydrostatic pressure $p$ (pressure positive in compression) is adopted:

$$\varepsilon_f = \varepsilon_0 + \left(\frac{p}{p_2} - \gamma\right)^2$$

where $p_2$ is the larger root of the yield function, and $\varepsilon_0$ and $\gamma$ are the parameters that will be calibrated to get the desired for-contact area relationship. In the present case Lu et. al. [11] carried out the calibration with respect to the force curve $F = 3.2A^{0.9}$ discussed in Section 1.2. In numerical simulations, when the failure strain is exceeded, the element is eroded in subsequent analysis.

### 3.2. Calibration to design curves

There are two ways to calibrate to the fitted force-displacement relationships. The natural choice would be to mimic the Pond Inlet tests by pushing a rigid indenter into the ice block. When this was done the red curve in Figure 12 was obtained [11]. Next, the same ice failure parameters were used by crushing a spheroidal ice against a plane rigid wall. The corresponding force contact area relationship that was obtained is shown by the blue curve. It appears the force is reduced by approximately 50 % relative to the red curve. It is possible that the large deviation is physically correct; the ice is considerably more confined in the mimicking of the Pond Inlet tests. However, experimental evidence is missing. As the ice, would become quite soft compared to the structure in subsequent integrated analysis, it was decided to calibrate for the spheroidal ice crushed against a rigid, plane plate. This is also more in analogy with the method used in the IACS PC code [5].

**Figure 12** Calibration to force curve $F = 3.2A^{0.9}$. Red curve from calibration with rigid block pushed into ice, blue curve when same calibration is used for spheroidal ice crushed against a rigid, plane surface. Green curve from calibration of spheroidal ice crushed against a rigid, plane surface. Element size 50 mm.
By this calibration the green curve was obtained. Thus, in integrated analysis, where the structure may fold around the ice and create confinement, it is likely that the resistance will be larger than the green curve, and this method is therefore conservative. Conversely, for the first calibration method, it is likely that the force curve in integrated analysis will fall between the red and the blue curves.

It is not surprising that the calibration is sensitive to size of the finite element mesh. Figure 13 shows force-contact area curves for five different mesh sizes: 100 mm, 75 mm, 50 mm, 35 mm and 25 mm and failure strain parameters calibrated with the first method [11]. The force curve reduces with decreasing mesh size. This is expected because a fine mesh is more likely to capture concentrated strains while a coarse mesh averages the strains over the larger distance. Therefore, element erosion takes place at an earlier stage with a fine mesh. This behaviour is, in fact, analogous to the well-known mesh size sensitivity of shell elements for fracture prediction of steel plates.

When different ice shapes shall be analysed it is essential to have the same mesh size. This may, however, be difficult to achieve in practice. If different mesh sizes are used in the ice modelling it is therefore necessary to calibrate for each shape. This was done for the local ice shapes shown in Figure 14: The curvature radius varies in the range of 0.68 m – 3.51 m. The calibrated parameters and the target force curves are shown in Figure 15. For the same ice deformation level, the “ice shapes with small curvature radius produce less force, but the force-area relationship are essentially identical.
4. Local shapes

Using the calibrated material parameters for the various shapes the local resistance to ice impacts on the front plating of the column of a floating semi-submersible platform as analysed. The finite element model of the front panels is shown in Figure 16.

The resistance versus deformation of the ice and the structure is shown in Figure 17. It is observed that for the smallest curvature radius 0.68 m (2a=80 m), the ice undergoes crushing while the structure deformation is very moderate. The ice resistance is virtually identical to that of a rigid structure. For increasing curvature radii (decreasing 2a), the ice resistance increases somewhat as the structure is subjected to increasing deformations, but no puncturing of the plating occurs, until the curvature radius is 1.8 m (2a=30 m). This represents a transition. When the curvature radius is 2.35 m (2a = 23 m) the mutual behaviour is reversed. The ice undergoes very little deformation and the structure must absorb most of the energy. The resistance is virtually identical to that of a rigid ice. This shape is particularly critical wrt. penetration of the front plating, which takes place at 0.8 m deformation. When the curvature becomes larger, the ice engages more of the front panel, so structure resistance increases. The deformations of the ice and the structure at two deformation levels are illustrated in Figure 18. It appears that the ice is little crushed before it contacts directly with decks and transverse frames, but the contribution to energy dissipation is small.

**Figure 15.** Target curves and force versus deformation from simulation during calibration of the failure criterion for each local ice shape. [11]

**Figure 16.** Finite element model of front panel of a column in semi-submersible platform. Typical deck spacing 2.9 m, frame/bulkhead spacing 2.5 m, plate thickness 17 mm, stiffeners L275•100•11.5/15, spacing 625 mm.
Figure 17. Force versus displacement of ice and structure from integrated analysis with deformable ice and structure (red curves) and from analysis where either the ice or the structure is assumed rigid (black curves). The curvature radius of iceberg varies [11].

Figure 18. Structural damage and ice crushing for the critical ice shape [11].
5. Conclusions

Ships and offshore structures are typically designed against extreme ice loads (annual probability $10^{-2}$) in the ultimate limit state (ULS) where only small yielding and plastic deformations are allowed. Explicit partial safety factors or in-built conservatism shall prevent the structures to enter the ULS. The design is carried on the basis of pressure-area ($p$-$A$) relationships. For plates between stiffener/frames or stiffener with associated plate flange the design pressures can be very high. For larger areas corresponding to stiffened panels between frames and deck/stringers, the pressures become moderate.

Offshore structures are also often designed for abnormal ice loads (annual probability $10^{-4}$) in the accidental limit state (ALS). These pressures follow typically the same area dependency, but with higher intensities. In general, local plates and stiffeners do not need to be designed to resist these load levels because:

- Plates have generally considerable strength reserves when they undergo large deformations
- Stiffeners are generally too weak to support the plate in the large deformation range, but the shell plating can still carry significant loads in two directions
- When high local pressures exceed the resistance of the plate or stiffener, they yield and the ice contact is gradually transferred to adjacent structures components
- Locally deformed plates and stiffeners are accepted in the ALS

The ALS check should therefore normally be carried out for larger units, such as panels between decks/stringers and frames/bulkheads. The contact area will typically be larger than, say $4m^2$. The check should be based on average force–contact area relationships, where the pressure is only weakly decreasing with increasing contact area. Average pressure–area curves (force-area) curves implicit in the IACS code for Polar vessels may be adopted: The curve for PC 3 class vessel is e.g. fairly close to the average force curve in the Pond Inlet tests.

Integrated analysis of structures subjected to ALS loads is becoming more common, where the ice is modelled as a material as well. Various constitutive models have been developed. At best they capture the essential features of ice crushing, but they need calibration, notably w.r.t. the strain failure criterion adopted. Plasticity-based models are highly mesh size sensitive and the failure criterion must be calibrated for each size.

It was considered to calibrate the ice parameters in tests mimicking the Pond Inlet tests. However, it was found that this would give unverified ice behaviour under less confined conditions. It is therefore recommended to use the more conservative approach; to calibrate the ice for crushing a rigid, planer surface.

Characterization of global and local ice shape is essential. The global shape governs the demand for strain energy dissipation as a fraction of the impact energy. The local shape is important w.r.t. analysis of structural damage and ice crushing. Unfortunately, no commonly accepted procedure for ice shape characterization exists. Very simplistic geometries are often assumed.

The effect of local ice shape, expressed as the curvature in the contact area, on the ice-structure interaction was investigated from a structural resistance point of view. It was found that ice with relatively large curvatures was crushed by the front panel in the column of a semi-submersible platform, and thus the structure resisted the ice loads well. When the curvature was reduced, the ice crushing resistance became larger due to the growing confinement caused by the increasing structural deformations. At a certain curvature, the ice was relatively little deformed and could penetrate the front panel virtually as a rigid object. This was denoted the critical shape for the given structural configuration, ice shape and ice strength. Ice with smaller curvatures became less critical in spite of less crushing, because the ice mobilized larger areas of the front panel.

The critical curvature radius was 2.35 m. This was in the same range as the distance between deck girders and vertical bulkheads/frames. More studies are needed to clarify if this finding can be generalized.

Acknowledgement

The work of the author was supported by the Research Council of Norway through the Centres of Excellence funding scheme, project number 223254 – NTNU AMOS.
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