Scientific Journals
of the Maritime University of Szczecin

Zeszyty Naukowe
Akademii Morskiej w Szczecinie

2017, 52 (124), 9–18
ISSN 1733-8670 (Printed)
ISSN 2392-0378 (Online)
DOI: 10.17402/240

Received: 19.06.2017
Accepted: 17.07.2017
Published: 15.12.2017

Structural analysis for ships in arctic conditions

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Key words: probabilistic ice loads, mission-based design, damage mitigation, low temperature response, probabilistic approach, collision scenario

Abstract

Ships are operating in regions with seasonal ice coverage outside the Baltic Sea. Due to the lack of experience operating in regions such as the Arctic Sea, existing design guidelines may not lead to reliable and safe ships. This article summarises regulatory aspects of ship design for ice-covered waters, focusing on structural compliance and design ice load determination. The latter will be obtained using a probabilistic approach and compared to the current rule-based load. Based on the discrepancy and the existence of ice induced damage, different measures aimed at mitigating damage are presented. Furthermore, the influence of sub-zero temperature (SZT) in a collision scenario on the material response is presented

Introduction

Ships operating in areas with seasonal ice coverage must be designed in compliance with loads resulting from the ice – structure interaction. This is typically achieved by complying with a rule-based determination of the design ice loads and scantlings. A common finding here is that heavier reinforcement results in lower levels of damage; however, this conflicts with the usual goal of reducing the initial capital expenditure of new ships and reduces the payload or increases the operational expenditure. Given that typical ice-going and ice-strengthened vessels only operate for a fraction of their design life in ice, it is understandable that lower levels of ice strengthening are preferred. Furthermore, there is a rather high damage tolerance exhibited by the owners and operators of such vessels. While this approach is understandable on first sight from an economic perspective, passive safety is adversely affected because accepting lower levels of strengthening purposefully neglects probable loading scenarios and load magnitudes which can result in structural damage and eventually hull breaches. Even though hull breaching due to ice loads is a very rare event, the subsequent water ingress or oil outflow can result in severe environmental consequences. Further, the reason for the absence of severe accidents due to ice – structure interactions, other than iceberg impacts, may lie in the circumstances of shipping in the Baltic Sea; the majority of which takes place with icebreaker support in water covered with relatively thin first-year ice. Operations in the High North, with long distances to shore and practically no short-notice icebreaker support, require a different approach to ensure safe and reliable operation in spite of the lack of experience.

In order to design and operate ships in the Arctic Sea, an international legislative framework has to be followed. The main bodies of this framework are the United Nations Convention on the Laws of the Seas (UNCLOS), the International Maritime Organization (IMO), the maritime states, Recognized Organizations (ROs), and the International Association of Classification Societies (IACS) (DNV, 2012). In addition, there is the International Labour Organization (ILO, 2014). Enforcement of the mandatory requirements of the IMO conventions depends upon the individual IMO members, which include most maritime states. A member state acts both as a flag state and a port state. A flag state has the authority and responsibility to enforce regulations over vessels registered under its flag. Since all ships have to meet the international requirements set by the IMO, flag states need to integrate their own statutory requirements with the requirements set by the IMO. "When a Government accepts an IMO Convention it agrees to make it a part of its own national law and to enforce it just like any other law" (IMO, 2014a). As a result, any IMO member (maritime state) has the authority to carry out so-called Port State Controls (PSC) to ensure that the condition and equipment of ships visiting their ports complies with the IMO standards (IMO, 2014b). This complex framework regulates the design and operation of ships in general, which is further described by Bergström (Bergström, 2017).

The International Maritime Organization (IMO) Polar Code, having been in force since January 2017, seeks to contribute to the safety of ships in the Arctic Sea, particularly ice-covered waters. The code contains specific provisions for ship structure, subdivision and stability; equipment lifesaving, navigation, and communications; crew training, and environmental protection for ships in the Arctic (N of 60°N) and Antarctic (S of 60°S). These provisions are additions to the following IMO Conventions: Safety of Life at Sea (SOLAS), Prevention of Pollution from Ships (MARPOL), and Standards for Training, Certification, and Watch-keeping (STCW). Being a high level code however, exact measures to quantify or improve safety are not provided. The accompanied POLARIS system seeks to check if a vessel's journey is to be made safely following the concept of the Arctic Shipping Pollution Prevention Regulations (ASPPR). The background of the contained multipliers is, however, not well defined and thus subject to uncertainty when used. Furthermore, it remains unclear who shall control or enforce compliance with the POLARIS system, because of the inherent liability aspect. On the other hand, the International Association of Classification Societies (IACS) provides the "Unified Requirements (UR) for Polar Ships", which standardized global ice classification specifications in seven polar classes. Here it must be noted, that this seven class system has been created upon agreement with the governing stakeholders to provide an a priori governing system to accompany envisioned needs, requirements and operational targets. Consequently, the agreements on, for example, exposure or impacts associated with each polar class or the underlying ice – structure interaction scenario, are debatable. However, this degree of freedom and the associated uncertainties allow for various adjustments and improvements in the design process without contradicting the regulations.

This paper seeks to provide an insight into existing methods of rule-based ice load and strength assessment, as well as a step forward using probabilistic methods and new knowledge of ice – structure interactions. It is shown that a task-specific design can be obtained, general design decisions can be evaluated better and better initial investment decisions can be made by accounting for financial risk.

This paper allows for a better choice of ice class to be made with additional compliance to the target operations and without being in conflict with the regulations, allowing for operations to be carried out more safely and with higher reliability allowing for competitive designs when compared to the remaining fleet.

The example vessel

MV Kemira is a transversely stiffened bulk carrier built in 1980 with the highest Finnish-Swedish ice class IAS. During the 7-year period from 1985–1991, MV Kemira was instrumented with shear strain gauges attached to the neutral axis of selected frames. The difference between two shear stresses on the same frame is proportional to the load on the frame between the gauges. To date, such long-term full-scale measurements are the most reliable basis upon which to evaluate the load level as a function of occurrence frequency (return period) for the frame structure. The measured data, in the form of time history plots, as shown in Figure 1, is typically processed by accounting for the 10-minute maxima through a Gumbel I extreme value fit – see Figure 2. The ship's side view and measurement locations are presented in Figure 3. The main particulars of MV Kemira are given in Table 1. On average, MV Kemira operated for 46 days per winter in ice; respectively, 1150 days of its prospective lifetime of 25 years.

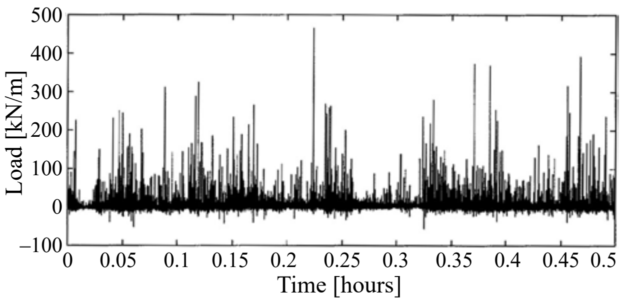


Figure 1. Time history example of measured ice loads (Lensu, 2002)

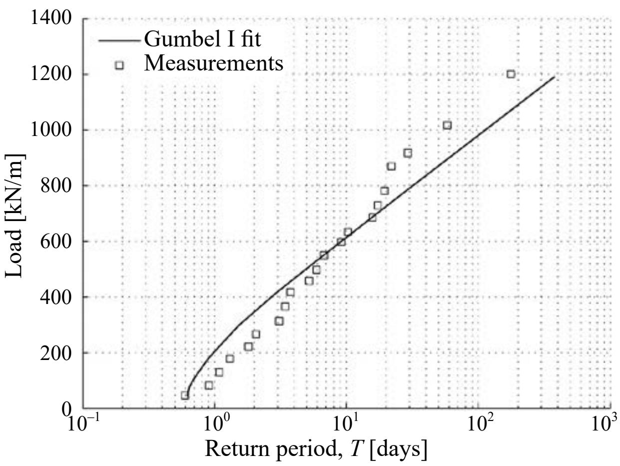


Figure 2. Long-term measured loads and fitted Gumbel I extreme value distribution on a frame at the amidships of MV Kemira based on the measuring reports by Kujala (Kujala, 1989)

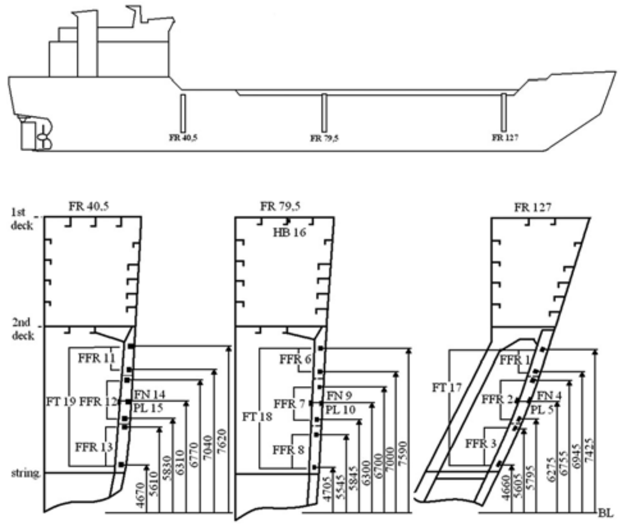


Figure 3. MV Kemira and the instrumentation used to obtain the data displayed in Figure 1 (Kujala, 1989)

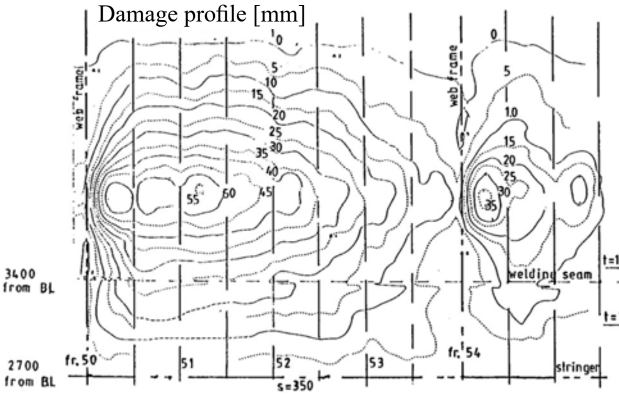
Table 1. Main particulars of MV Kemira

L_{bp}	B	T	DWT	v_{ref}
105 m	17.5 m	8.0 m	8145 t	14 knots

The most probable annual ice load (i.e. after an average of 46 days in ice) is about 870 kN/m, and the lifetime load level (i.e. 1150 days in ice) is 1350 kN/m. The Finnish-Swedish Ice Class Rules (FSICR) design ice load for the aft ship frames is 380 kN/m (Riska & Kämäräinen, 2011). As seen in Figure 2, MV Kemira frequently exceeds this load level. The reason for this is that the FSICRs utilize the yield strength as a corresponding design limit and further allow for plastic deformations to occur during the ships' lifetime. A fact that appears to favour lower initial expenditure over lower life-cycle cost, due to the comparably high damage tolerance of ship owners operating in ice-covered waters (see also Kujala & Ehlers, 2014).

Recorded ice damage

Figure 4 shows the ice-induced damage of a bulk carrier built in 1985 of ice class IA with 4693 t DWT, length of 91 m, width of 16 m, propulsion power of 2600 kW and average draught at incident of 5.95 m. The damage extended over the full webframe spacing of 2.8 m and three stiffener spacing of 0.35 m resulting in a maximum deflection of 55 mm, deforming the 15.5 mm thick plating and longitudinal HP profiles of dimension HP200×11.5. The webframes of dimension 600×10 were not deformed significantly. Due to the lowest safety margin applied during



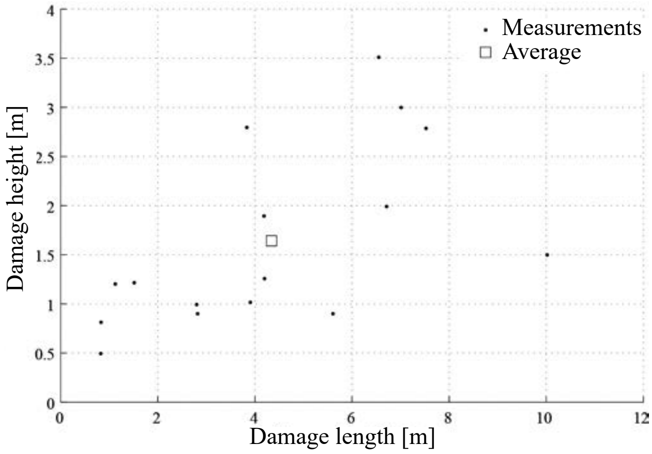


Figure 4. Observed ice-induced damage in a bulk carrier (left) and damage extent measurements (right) reproduced from Kujala (Kujala, 1991)

design to the plating, the observed ice damage can be considered representative in terms of location and magnitude. Measured ice damage extents for various ships operating in the Baltic Sea are shown in Figure 4, also displaying a fairly limited extent compared to the overall ship dimensions. Furthermore, it is worth noting that hull breaching in ice is very rare and that most damage occurs when the vessel approaches the ice with too high a speed.

Ice damage requiring repair

A clear definition of the state of deformation requiring repair is not to be found in current legislation. The Russian Maritime Register of Shipping allows the repair of smooth indentations in the hull plating during the next scheduled dry docking, if the following is met: the indentations are not larger than 20% of frame spacing and the depth to length ratio is not larger than 1:20; local dents are allowed if the depth is not greater than five times the thickness of the plating and the ratio of depth to frame spacing not greater than 1:20 (Benkovsky, 1970). Further, surveyors may require repair, if the plate deflection is above 1/12 of the frame spacing (Hayward, 2007).In conclusion, the damage shown in Figure 5 was repaired (Kujala, 1989). The cost of such a repair may be calculated with a cost module, see Rigo (Rigo, 2003) for an example, which accounts for labour, consumables and material costs in addition to the downtime and dry docking related cost. Given the damage extent shown in Figure 4, the majority of the cost presents as downtime related cost. Further details for such an assessment are given in Kujala and Ehlers (Kujala & Ehlers, 2014).

Structural analysis for service ice actions

Present design methods benefit from the vast experience of small to medium-sized transversely stiffened ships operating in first-year ice. Scantlings determination requires a design pressure and occurrence for the target ice class or the operational area in question as well as a design criterion i.e. yield. The rule-based target ice class-based concept is shown by Riska and Kämäräinen (Riska & Kämäräinen, 2011); however, current rule-based design methods are not necessarily transparent by means of design pressure and scantlings determination, because they use intrinsic design criteria.

Ice-induced loads, see Figure 1, can only be described by stochastic processes due to the unknown distribution of ice strength properties and local contact geometry in the ship/ice interaction process. Besides different operational modes, the form of the ice directly influences the ice load, i.e. level ice, ice floes and ridged ice containing first-year and multi-year ice. Further, to date there is no mathematical, numerical or analytical model available to describe the physical process of ice breaking, i.e. ship – ice interaction.

Probabilistic, or site-specific, ice load determination allows for a link between statistical data from the operational area of the vessel and the design load; however, current ice class rules do not consider probabilistic methods for determining ice-induced loads, because the requirement to specify the mission of the vessel can be considered a shortcoming by means of liability from a regulator's perspective. The latter link between the design rules and the operation of the vessel is however created in IMO's POLARIS-System, which nevertheless lacks the specification by whom this will be controlled.

Yet, probabilistic design methods can be used to enhance the design process by identifying the ice load in a continuous space in addition to the discrete rule-based load, thereby allowing for more refined design decisions to be made.

Mission-based probabilistic ice load assessment

An example of such mission-based probabilistic ice-load determination is presented by Tõns et al. (Tõns et al., 2015) based on a method by Jordaan et al. (Jordaan et al., 1993), who showed how to use pressure area relationships, obtained from full-scale measurements, to predict extreme loads at a certain exceedance probability level.

Erceg et al. (Erceg et al., 2015) presented the applicability of such probabilistic design load methods to ice-going ships operating along the Northern Sea Route (NSR) in comparison to rule-based loads. The rule-based loads were calculated according to FSICR (Trafi, 2010). For the probabilistic local design load, a global ram analysis is first carried out from which an average ram duration and penetration is determined. The local pressures on individual panel areas are modelled using an exponential distribution for peak panel pressures given as:

$$F_x(x) = 1 - \exp\left(-\frac{x - x_0}{\alpha}\right) \tag{1}$$

where x_0 and α are constants for a given area and x is a random quantity denoting pressure. To obtain the local peak pressure distribution, the number of events can be modelled as a Poisson-process resulting in:

$$F_z(z) = \exp\left\{-\exp\left(\frac{z - x_0 - x_1}{\alpha}\right)\right\} \tag{2}$$

where $x_1 = \alpha(\ln\mu)$ and x_0 is the panel exposure constant. Exposure is modelled as the proportion of events that represent actual impacts between the ice and the structure as:

$$\mu = \nu \cdot r \cdot \frac{t}{t_k} \tag{3}$$

where v is the time period, r is the proportion of events resulting in "direct hits" on the structure, t is the duration of the impact, and t_k is the reference duration associated with a design curve from Jordaan et al. (Jordaan et al., 1993). As a result, the design load, z_e, can now be calculated for a given exceedance probability, $F_z(z_e)$, as:

$$z_e = x_0 + \alpha\{-\ln[-\ln F_z(z_e)] + \ln \mu\} \tag{4}$$

For design loads in multi-year ice it is acceptable to use the envelope or upper bound curve described by $\alpha = 1.25a^{-0.7}$, where a represents the local contact area. For first-year ice, the following approach may be more appropriate, because the envelope curve overestimates local pressures by a considerable margin: the use of design equations corresponding to the datasets under ice loading conditions, similar to those expected for the design environment; see Taylor et al. (Taylor et al., 2010).

For illustration purposes, a mission-based example is now presented considering transits along the NSR from the Zhelaniya port (Kara Sea) to the Dezhnev port (Bering Strait); a distance of approximately 4500 km. The average speed is considered to be seven knots resulting in an approximate duration of one transect of 15 days. In a given year, four months is considered a feasible operational window at a maximum ice thickness of one meter, resulting in four round trips. Assumptions of stationary ice conditions and an ice concentration of 0.5 are made. Additionally, the route is ice-free for two months in a given year. Using an event duration of 0.934 s, calculated as 1/frequency from Kujala et al. (Kujala, Suominen & Riska, 2009), and Poisson's discrete probability for events to occur, the expected number of events for the chosen period is 1.88 million. The ship is designed to an exceedance level of 10^{-2}, which corresponds to the design point of FSICR, i.e. reaching yield once in any given winter. The proportion of true hits is chosen as $r = 0.5$. The exposure constant, x_0, dependent on the design area, is calculated according to Taylor et al. (Taylor et al., 2010) for the North Bering Sea 1983 dataset. With 1.88 million events along the route and Equation (3) we can solve for the corresponding design pressure using Equation (4) and $\alpha = 0.28a^{-0.7}$ for the North Bearing Sea 1983 dataset. The resulting design pressure

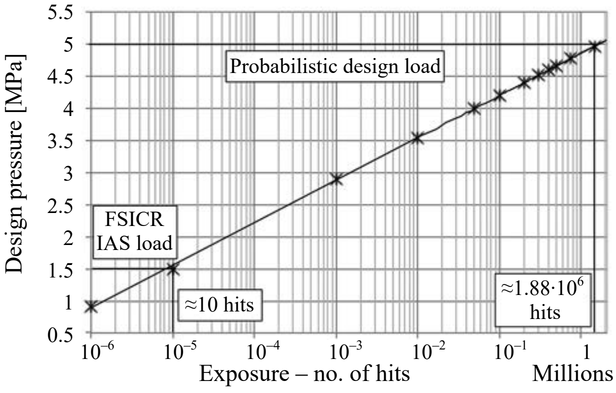


Figure 5. Design pressure for a local panel of ~ 1 m² (Erceg et al., 2015)

versus exposure, in comparison to the corresponding FSCIR load value for IAS, is given in Figure 5. Therein, it can clearly be seen that the probabilistic ice load determination accounts for significantly more impacts resulting in an increase of the design load from 1.5 MPa to 5 MPa. The latter certainly results in higher scantling requirements and thus a heavier and more expensive structure, which in turn will be less susceptible to ice induced damage.

A simplified, non-uniform, pressure patch (SPP)

A common approach for numerical structural analysis is to apply the ice load as a rectangular and uniform pressure patch, with a certain eventual decay to the lower and upper bounds of the application area. However, ice – structure interactions involve ice that is continuously damaged as the load introducing contact element and the involved fracturing and spalling processes result in a spatial and temporal variation in the ice load. Therefore, Ehlers et al. (Ehlers et al., 2014) analysed the structural response of a stiffened panel using measured ice load data from the Japan Ocean Industries Association (JOIA) field indentation test program. These tests were carried out with natural ice in the Notoro Lagoon on brackish first-year ice with some natural snow cover. The average ice thickness was approximately 30 cm throughout the test program.

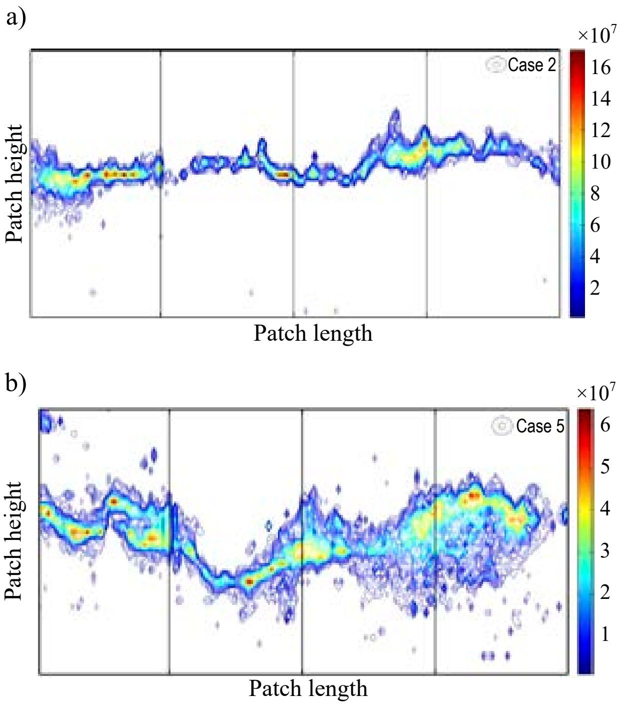


Figure 6. Measured spatial pressure distributions (Ehlers et al., 2014): a) Maximum ratio between force and pressure area, b) Maximum measured load

During the test, temporal and spatial pressures were recorded with a constant rate of 0.4 cm/s using tactile sensors on a measuring area of 150×40 cm^2 with 176×44 cells. An example spatial pressure distribution is shown in Figure 6, while the overall time series of the total force and active area measured is presented in Figure 7. The peak value on the right hand side of the time history shown in Figure 7 corresponds to the maximum area triggered by the ice load. This load also corresponds to a peak load, which would be processed by the rule-based design load approach as described earlier. However, it is quite intuitive, that concentrated loads of lower total magnitude can cause a larger local structural response. Consequently, it becomes apparent that a uniform application of the design pressure over a design ice load height can yield non-conservative structural designs. Therefore, it is important to consider the line-like load behaviour (shown in Figure 6) in the design of ice-strengthened structures together with localized pressures visible in Figure 6 also; this will likely lead to designated high pressure zones (*hpz*). One solution is to extend the uniform rectangular design load area, found in current rules, by significantly reducing the loading height, while the load length remains equal to the initial design load length and corresponds thus to the structural component under consideration. Furthermore, at least one *hpz* shall be located within this line-like load patch. The choice of one *hpz* is justified, because a stiffened panel under design consideration shall have a uniform plate thickness and constant stiffener spacing with equal stiffeners. Thus, the location of the *hpz* is arbitrary as long as it is not influenced by the panel boundaries.

The resulting SPP is shown in Figure 8 indicating that the majority of the pressure is distributed on a narrow, line-like, strip in the middle of the original rule-based pressure patch, extending throughout the

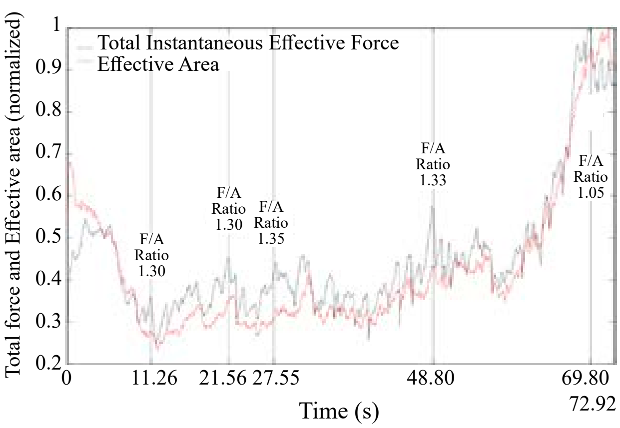


Figure 7. Time series of the measured force and active pressure area (Ehlers et al., 2014)

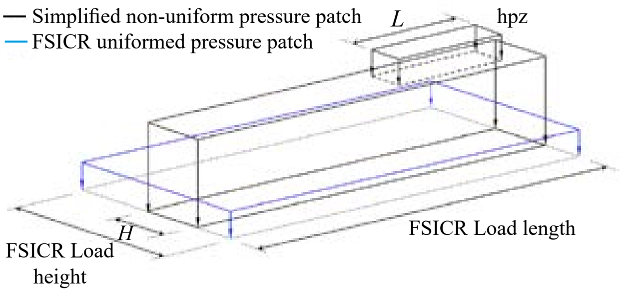


Figure 8. Proposed SPP compared to FSICR load patch

entire centre strake between two web frames. The height, *H,* of this line-like load is suggested to be 22% of the original rule-based load patch height, while the height of the *hpz* is suggested to be half of the line-like load with a length of 13% of the FSICR load length. The load magnitude of the *hpz* compared to the line-like load magnitude may be obtained through initial response calculations or estimated to be 40% of the line-like load. In general, the magnitude of the SPP is determined by observing the response of the structure under a measured load, while the total amount of energy introduced into the structure is kept equal to the FSICR case.

Analogous to the probabilistic design ice load determination, the SPP will result in more compliant structures, but also in higher scantling requirements. Consequently, damage as shown in Figure 4 may be avoided. However, the additional expenditure may be considered infeasible. Therefore, the next chapter will investigate the additional expenditure in view of the associated life cycle cost.

Risk-based design ice load assessment

Increasing the number of scantlings to avoid ice-induced damage during ships' lifetimes will result in higher production costs but no repair costs (Kujala & Ehlers, 2014). Another possibility is to allow for some plastic deformations, which must be repaired at specified nominal repair intervals. This will result in lower production costs but higher repair costs over a ship's lifetime. The optimal compromise between these two extremes is now presented based on Kujala and Ehlers (Kujala & Ehlers, 2014). The underlying procedure carries out direct calculations using the nonlinear Finite Element Method (FEM) to assess the structural response of a stiffened panel of the vessel under consideration. Long-term load measurements are utilized to dimension the stiffened panel for higher load levels, and thereby to reduce the probable repair costs, and to present the sensitivity in terms of financial risk.

In order to identify the optimum structure in terms of production costs and probable repair costs, the financial risk must be assessed. As a starting point, a reference stiffened panel, designed to a target ice class, may be used. This stiffened panel can subsequently be exposed to measured loads exceeding the rule-based design load value. In addition, the production costs for each new stiffened panel corresponding to a higher load level must be assessed, see Rigo (Rigo, 2003) for an example. Further, since the return period is known for these higher loads, the corresponding decline in probable repair costs following ice-induced damage can be calculated based on downtime and dry-docking associated costs.

For the example vessel presented in this paper, the FSICR load level is 380 kN/m as an initial design limit. The stiffened panel used as an example structure was subjected to the maximum loads measured to occur within 1-, 2-, 3-, 5-, 10- and 20-years, see also Figure 2. To identify the optimum structure, additional scantlings must be used, and their production costs must be obtained, such that no repair costs are incurred for the maximum loads occurring within 1-, 2-, 3-, 5-, 10- and 20-years, respectively. The return periods of the loads can be considered as the target repair intervals. The repair costs decrease with increasing design load levels, while the production costs increase with increasing load levels. Thus, to allow for a comparison of these structural alternatives, the Net Present Value (NPV) is calculated, comprising the possible repair costs and their probability of occurrence during the ship's lifetime, as well as the initial production costs. The resulting NPV is presented in Figure 9 for the different structural alternatives as a function of the target repair

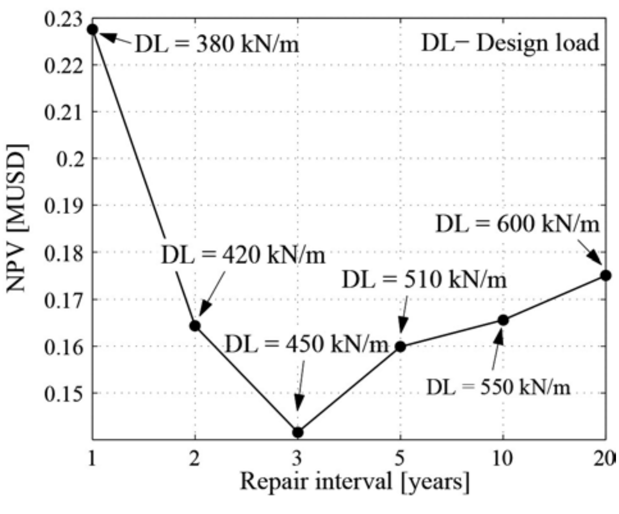


Figure 9. NPV for the different structural alternatives, repair intervals and corresponding design load levels (Kujala & Ehlers, 2014)

interval together with the corresponding load levels. The lowest NPV represents the optimum structure, which is obtained for a 3-year repair interval. However, the usual dry-docking interval is 5 years. This would require an increase of the design load to 510 kN/m and a reduction of the NPV to 70% of the original value. At the same time, this requires an increase in the structural mass of the vessel by 60 t, which can be considered to be of minor importance. Furthermore, this mass can be minimised using the structural optimization procedure identified by Ehlers (Ehlers, 2010).

Structural analysis for accidental actions

A collision may take place in regions with ice-coverage, i.e. in cold climates; therefore, this chapter will identify the structural response of the hull in Sub-Zero Temperatures (SZT). At first it is worth noting that different temperature definitions exist for the design of ships and offshore structures; see for example IACS UR, DNVGL Rules for ships, DNVGL-OS-A201, IMO PC, ISO 19906 and NORSOK N-003. For material selection, either the Lowest Mean Daily Average Temperature (LMDAT) or the Lowest Anticipated Service Temperature (LAST) is considered. For the LMDAT, at least a 20-year data series should be used, while there is no minimum required number of years for the LAST. As a result, the LAST can be as low as –40°C, and up to 30°C lower than the LMDAT; however, there is a lack of guidance relating to structural behaviour at low temperatures, especially for welds.

While the strength of steel increases at colder temperatures, it is obvious that the inherent risk of unexpected brittle fracture increases as well. All ferritic structural steels suffer from reduced fracture toughness at low temperatures due to the ductile-to-brittle transition behaviour (DBT), which is typical for steels with body-centred cubic (bcc) crystal structure. At lower temperatures, the mechanism of stable crack growth behaviour changes from plastic blunting and tearing to cleavage controlled brittle fracture. This transition occurs over a narrow range of temperature, typically of 30 K (Billingham et al., 2003), and is often characterized by the T_{27J} temperature. This is the temperature, where the interpolation of Charpy V-notch impact toughness test results yields an energy of 27 J. Other measures for the ductile-to-brittle transition temperature (DBTT) are the fracture-appearance transition temperature (FATT), where 50% of the fractured surface is related to brittle fracture; or to transitions in CTOD (BS 7448-1), K_{Ic} (ASTM E399), and T_0 (ASTM1921); or to J-integral (ASTM E1820) test results. Preliminary details on the influence on the fatigue properties of welded structures are presented by Braun (Braun, 2017) in the form of static and dynamic material properties for different welded structural details and material strengths at changing temperatures from room temperature to –50°C. Thereby he seeks to establish a physical model that explains the change of fatigue growth behaviour at low temperatures based on the changing failure mechanism.

Ship collision strength assessment at sub-zero temperature

This section presents the collision resistance of ships exposed to SZT. Furthermore, it presents the influence of the material properties on the collision force for 0, –30, and –60 degrees Celsius of typical ship building steel based on work by Ehlers and Østby (Ehlers & Østby, 2012). The energy absorbed before fracture of a ship side structure by Ehlers (Ehlers, 2010) is used. Thereby reliability of the collision simulations is achieved because a consistent link between the local material measurements and the discretized finite element model is obtained on the basis of optical measurements. This consistent link allows for a sufficiently accurate implementation of fracture initiation and propagation by deleting failing elements at the correct state of strain. As a result, comparative collision simulations can be carried out for different structures and SZTs. The experimentally obtained results for a series of NVA steel tensile tests, carried out for 0, –30, and –60 degrees Celsius, are presented in Figure 10. Therein it is interesting to note that the yield stress, the length of the Lüders plateau and the fracture strain increase in value with decreasing temperature. The increase in fracture strain is however only valid for smooth specimens without notches, a condition assumed to hold true for the intact structure, but certainly being invalid from the onset of rupture.

Collision simulations may be carried out with the explicit solver LS-DYNA (see Hallquist, 2007). Here both quasi-static and dynamic collision simulations can be carried out. Ehlers et al. (Ehlers et al., 2010) presented the influence of the ship's motion on the collision response. Therefore, this section compares the force versus penetration curves at SZT and non-SZT. The material behaviour presented in Figure 10 is implemented via material 24 of LS-DYNA, which allows failing elements to be removed at the critical strain. This constant strain failure criterion is justified due to the close ranges of triaxiality at failure

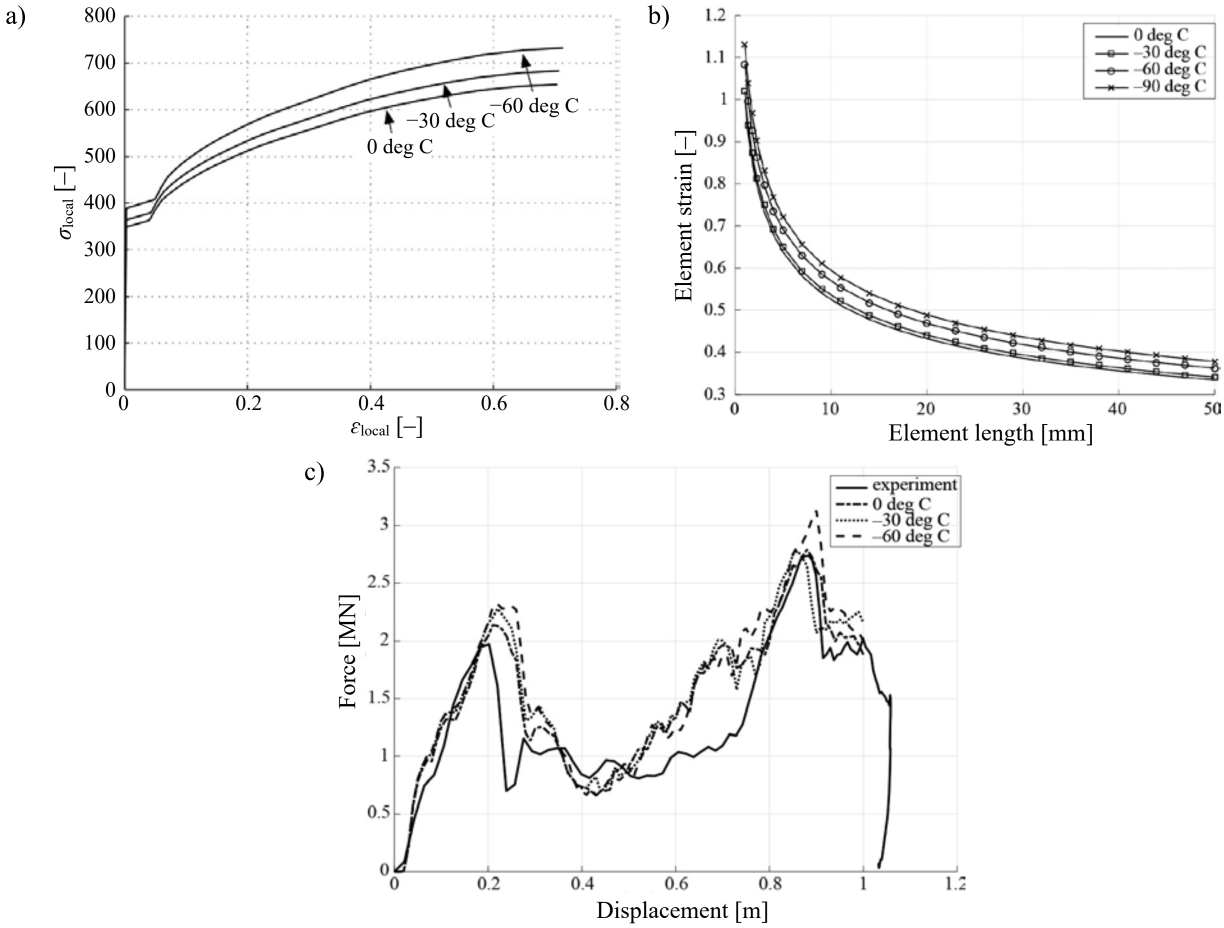


Figure 10. Local material relation (a); element-length dependent failure strain (b) for std. and arctic material at SZT; collision simulation results for SZT of a large-scale double hull structure (c)

for thin plates (Ehlers, 2010). The strain rate sensitivity is not included in this material relationship, as no influence on the ultimate tensile force and failure strain for different displacement speeds was found; see Ehlers et al. (Ehlers et al., 2010). The resulting force versus penetration curves are shown in Figure 10c. A reduction in penetration depth with decreasing temperatures as well as an increase in collision force can be seen. The latter occurs because more energy can be absorbed for the same level of deformation compared to non-SZT simulation prior to rupture. Further, for lower temperatures, the structure is continuously able to increase the collision force as a result of the increase in fracture strain. In conclusion, the material is able to absorb significantly more energy during such collision event at SZT and the resulting damage is comparably smaller.

Summary and conclusions

This paper presents the general regulatory framework for ships operating in ice-covered waters with a focus on ship structural design and design ice load assessment. The presented results clearly indicate the need to consider the line-like contact including *hpz*s compared to the uniform rule-based approach for the design ice load application in numerical analysis. This finding supports the possible development of future ice class rules by means of a simplified, non-uniform, pressure patch (SPP). Thereby, new load height and *hpz* considerations can be made consistently through a strain and stress compliance analysis. Further, mission-based direct ice load calculations can improve the decision-making process towards a certain ice class and allow for a case specific design. The latter must be treated with care however, since it may be challenging to anticipate the operational profile and ice conditions for a vessel's design life. Also, it may conflict with the regulatory perspective, which avoids recommendations on the operational profile in view of the given ice class. Nevertheless, it provides a tool for operators, designers and yards to evaluate their design against the current regulations; however, it was also shown that a probabilistic load assessment typically increases the design load and thus the capital expenditure required to reach structural compliance. The latter being an unfavourable condition, given that most ice-strengthened vessels only operate in ice-covered waters for a fraction of their design life. Consequently, an ice induced damage mitigation strategy is presented on the basis of a financial risk analysis. As a result, it was shown that while the design load level can be increased to reach compliance with an anticipated repair interval, the overall cost, i.e. initial expenditure and maintenance cost, can be reduced by as much as 62%. Thus, it is possible to significantly increase the reliability and safety of ships with no additional cost. Finally, the material and collision response in sub-zero temperature (SZT) was presented, and it was shown that a reduction in temperature can increase the material strength and consequently the energy absorbed during a collision. The benefit from the influence of the SZT may vanish in practice, given that uniform low temperature distributions have been assumed throughout the models. Furthermore, it must be noted that these findings assumed that the behaviour of a smooth specimen is representative until fracture is initiated. A condition unlikely to hold true in the presence of welded joints. The behaviour of welded joints at SZT is however not fully understood, especially concerning their fatigue life.

Acknowledgments

Publication funded by the Ministry of Science and Higher Education of Poland from grant No. 790/P-DUN/2016 for the activities of promoting science (task No. 3 “Publications of foreign, distinguished scientists and their participation in the scientific board”).

Ministry of Science and Higher Education
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