Ship Grounding Damage

An Estimate through Acceleration Measurements

S.R. Haag
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by

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This thesis, "Ship Grounding Damage: an Estimate through Acceleration Measurements", is part of the graduation project of Stan Haag. This project is a partial fulfilment in order to obtain the degree of Master of Science at the Delft University of Technology. The study program is Maritime Engineering with the master track "Science" and a specialisation in "Ship & Offshore Structures".

Salvage of ships has fascinated me since my second year of studying Maritime Technology. It is from this fascination that I designed my own minor program about salvage and wreck removal in the bachelor of Maritime Technology. The same fascination landed me an internship at Ardent Global, a renowned salvage company in IJmuiden, The Netherlands. And now my fascination for salvage has brought me to the Structural Dynamics department of TNO where I did research that will, hopefully, enrich the knowledge and know-how applicable to marine salvage.

The concept for the newly proposed grounding damage estimation method introduced in section 1.2 originally came from my supervisors at TNO, Martijn Hoogeland and Lex Vredeveldt. Over the past 12 months, I tried to make this concept come to life with a literature review, simulations and a series of very spectacular experiments in the Structural Dynamics lab at TNO. This MSc thesis is the final report on those efforts.

This thesis would not have been possible without the support of all the people at TNO, and in the first place my daily supervisor at TNO, Martijn Hoogeland. Thanks a lot for all the conversations, help and most of all, your critical look on all my wild concepts. Speaking of wild concepts brings us to Lex Vredeveldt, the originator of the concept for my graduation research. Thanks Lex for the creative contributions and the challenging conversations. The guys at the SD-lab of TNO helped me out a lot with my experiments. Without the help of Khalid, Richard, Erik, Cees, Etienne, Birju, Jan and Nick, the test set-up would not have worked as smoothly as it did.

"I want to do research related to salvage, which involves sinking or damaged ships. What is the best place to go to?" That was my question to Mirek Kaminski, professor of my master specialisation. He enthusiastically referred me to TNO, which turned out to be a very good referral. Thank you Mirek, for your inexhaustible enthusiasm and all the challenging meetings we had. I also like to thank Reinier Bos, my go-to guy at the TU Delft for anything related to my graduation project. I admire your professional attitude and I enjoyed all our informative (and less informative) conversations. I would also like to express my gratitude towards Paul van Woerkom, for taking the time to delve into my research work and provide me with advise to improve my thesis.

Along with ambitious projects, come ambitious budgets. The willingness of the "Raking Damage Partners" to join us on this adventure into the unknown, made it possible to do the spectacular "Raking Damage Experiments". The support of our "Raking Damage Partners" is therefore gratefully acknowledged. This partnership consisted of TNO, Delft University of Technology, the Dutch Defense Material Organisation (DMO), Ardent Global, SMIT Salvage, Bureau Veritas and Damen Schelde Naval Shipbuilding. Their support made it possible to perform the experiments and take the raking damage research damage one step further.

I want to thank my girlfriend Simone, who was my biggest supporter during this challenging project. She has been cheering from the sideline all the time and never stopped encouraging me to overcome the difficulties that are part of writing a MSc thesis. The same can be said of my parents and my brother. Even though they live some place else, they have been very supportive and never lost faith in a happy ending.

Last, but not least, I would like to thank my house mates Menno, Elmer, Thijs and Patrick for supporting me. I have not always been an enjoyable house mate over the past twelve months and I want to thank you all for your patience. And of course all my friends in Delft who helped me out by just having a coffee, beer, whisky or jeneverje with me. Thank you all for your support!

S.R. Haag
Delft, May 2017
Abstract

When the Costa Concordia ran aground in Italy, it took 69 minutes to make the critical decision to abandon ship. The crew was unaware of the sheer size of the damage and the impact on the ship’s stability for too long. Instantaneous insight into the structural damage of the ship after running aground may be of great aid in such disasters. A possible way to acquire such instantaneous insight is by measuring accelerations during the grounding accident and derive the extent of the damage from analysis of these signals. Such acceleration measurements could, nowadays, even be done using a simple smart phone.

The Costa Concordia hit a rock and sustained a so-called raking damage. Friction, plastic deformation and rupture are the mechanisms that form the damage, each dissipating energy in a different way. It should be possible to distinguish the energy dissipation due to these separate mechanisms from merely analysing acceleration measurement data of the vessel and thus estimate the extent of the sustained structural damage.

In a ship grounding, the crew want to know whether or not the hull is breached. The structural damage of interest is therefore rupture of the hull plating. This thesis zooms in on detection of plate rupture only. It is based on experimental research exploring detection of plate rupture in a raking damage scenario by analysing the acceleration measurement data from a series of drop tower experiments.

This research provided a first, exploratory step into raking damage estimation by using acceleration measurements. The onset of rupture can be identified and when a proper estimate of both the failure criterion and the friction coefficient is made. It is envisaged that the extent of raking damage can indeed be derived through real time acceleration measurements on board of a vessel.

The series of drop tower experiments are designed and prepared by doing Finite Element Analysis (FEA). As part of these preparations, a sensitivity analysis is performed. The sensitivity analysis shows that the Finite Element Model (FEM) results are particularly sensitive to two input parameters: the failure criterion and the friction coefficient. So failure and friction are the two determining phenomena in a raking damage scenario in terms of energy dissipation. Besides the acceleration measurements, which form the core of this research, failure and friction are studied in detail.

In total four raking damage experiments were performed on grade-A steel specimens of 6 mm thickness. The drop tower experiments are performed in such a way that they simulate a raking damage scenario realistically. During the experiment, the accelerations and loads on the sphere shaped indenter are measured. Two high speed cameras are set up to perform Digital Image Correlation (DIC) measurements with 2500 fps, in order to capture the exact moment of plate rupture.

Using the high speed DIC measurements, strains at plate rupture were found and a Fracture Forming Limit Curve (FFLC) was calibrated. A special method of calibration was used, that uses only one single strain-state to calibrate the FFLC. As a comparison, a second FFLC was calibrated using the results of a standard uni-axial tensile test. The FFLC calibrated with the tensile test provides a very accurate prediction of the strains at failure for the raking damage experiments. The acceleration measurement data of all four raking damage experiments show an abrupt decrease directly after initiation of plate rupture. This abrupt decrease indicates that the transition from an intact plate to a ruptured plate can readily be detected from the experimental acceleration data.

Half of the raking damage experiments were performed with reduced friction. Separate tests to determine the friction coefficient for the two cases have been done. With this friction coefficient, the total energy dissipation through friction has been determined via two different calculation methods. Both these methods yielded similar results. Based on the similarity between these results, static Coulomb friction seems to be a proper model to determine the energy dissipation through friction.

This research provided a first, exploratory step into raking damage estimation by using acceleration measurements. The onset of rupture can be identified when a proper estimate of both the failure criterion and the friction coefficient is made. It is envisaged that the extent of raking damage can indeed be derived through real time acceleration measurements on board of a vessel.
Nomenclature

Symbols

\( A \) Cross sectional area \([m^2]\)
\( a_0 \) Thickness \([mm]\) (tensile testing)
\( b_0 \) Width \([mm]\) (tensile testing)
\( c_{thermal} \) Specific heat \([Jkg^{-1}kg^{-1}\])
\( E \) Energy \([J]\)
\( \dot{E} \) Rate of energy dissipation \([Js^{-1}]\)
\( F \) Force \([N]\)
\( g \) Standard gravity, 9.81 \([ms^{-2}]\)
\( l \) Moment of inertia \([m^4]\)
\( L \) Length \([m]\)
\( L_0 \) Original gauge length \([mm]\) (tensile testing)
\( m \) Mass \([kg]\)
\( M \) Moment \([Nm]\)
\( p \) Pressure \([Nm^{-2}]\)
\( r \) Radius \([m]\)
\( S \) Instantaneous cross sectional area \([mm^2]\) (tensile testing)
\( S_0 \) Cross sectional area of parallel length \([mm^2]\) (tensile testing)
\( S_c \) Contact area \([m^2]\) (friction)
\( S_u \) Ultimate cross sectional area at failure \([mm^2]\) (tensile testing)
\( T \) Temperature \(\circ C\)
\( t \) Time \([s]\)
\( V \) Volume \([m^3]\)
\( v \) Velocity \([ms^{-1}]\)
\( W \) Work \([J]\)
\( z \) Vertical displacement (of the indenter)\([m]\)
\( \dot{z} \) Vertical velocity \([ms^{-1}]\)
\( \ddot{z} \) Vertical acceleration \([ms^{-2}]\)

\( \varepsilon \) Strain \([-]\)
\( \varepsilon_1, \varepsilon_2, \varepsilon_3 \) Principal strains \([-]\)
\( \varepsilon_{eq}, \varepsilon_{VM} \) Equivalent strain, Von Mises Strain \([-]\)
\( \varepsilon_f \) Equivalent failure strain \([-]\)
\( \mu \) Friction Coefficient \([-]\)
\( \sigma \) Stress \([MPa]\)
\( \sigma_{yield} \) Yield stress \([MPa]\)
\( \theta \) Slope \(\circ\)

\( \angle \) Angle \(\circ\)
### Abbreviations

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<th>Description</th>
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<tr>
<td>BWH</td>
<td>Bressan-Williams-Hill (failure criterion)</td>
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<tr>
<td>DIC</td>
<td>Digital Image Correlation</td>
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<tr>
<td>FEA</td>
<td>Finite Element Analysis</td>
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<tr>
<td>FEM</td>
<td>Finite Element Model</td>
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<tr>
<td>FFLC</td>
<td>Fracture Forming Limit Curve</td>
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<tr>
<td>FLC</td>
<td>Forming Limit Curve</td>
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<tr>
<td>FLD</td>
<td>Forming Limit Diagram</td>
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<tr>
<td>GL</td>
<td>Germanischer Lloyd (failure criterion)</td>
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<tr>
<td>IR</td>
<td>Infra-Red</td>
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<tr>
<td>MMC</td>
<td>Modified Mohr-Coulomb (failure criterion)</td>
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<td>OMAE</td>
<td>International Conference on Ocean, Offshore &amp; Arctic Engineering</td>
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<tr>
<td>RTCL</td>
<td>Rice-Tracey-Cockroft-Latham (failure criterion)</td>
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<tr>
<td>TNO</td>
<td>Dutch institute for applied scientific research (Toegepast Natuurwetenschappelijk Onderzoek)</td>
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<tr>
<td>UTS</td>
<td>Ultimate Tensile Strength</td>
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Introduction

In this introduction the elements of the title of this thesis will be explained and the relevance of the grounding damage research will be underlined. Hereafter the scope of work will be established by the research questions and hypothesis. The introduction will conclude by setting out the structure of this thesis.

1.1. Ship Grounding Damage

Despite ongoing efforts to minimize the risks, maritime accidents keep occurring. Collision, contact and grounding combined make up the majority of the marine accidents, which can be seen from the two bar charts in figure 1.1.

![Bar chart (a) Number of casualties reported in 2014](image1.png)

![Bar chart (b) Distribution of casualties 2011-2014](image2.png)

Figure 1.1: Statistics of casualties as reported by the European Maritime Safety Agency (EMSA) [12]

Moreover, the nature of a grounding accident often involves the ship’s hull raking over the seabed along a substantial length. This makes that, even though ship grounding is not the number one cause of marine accidents, it is does account for most of the total losses (figure 1.2).

Looking at the statistics for ship grounding (in figure 1.1 and figure 1.2) it is seen that a ship running aground is a potentially perilous situation. Depending on the seabed topology as well as the speed, draft and weight of the vessel, two scenarios are possible: the ship may either tear open or just slide over the ground. Hereafter the vessel may either get stuck on the obstruction or it sails on with a lengthy damage and possibly a breached hull. Such a damage scenario is often called a raking damage scenario. Numerous examples of such grounding accidents can be named. The latest well known example is definitely the disaster with cruise ship Costa Concordia, of which a part of the damage and the rock that remained stuck in the hull is shown in figure 1.3.
When the Costa Concordia ran aground in Italy, it took 69 minutes to make the critical decision to abandon ship [1]. One of the causes of this lengthy decision making time was that the officers on the bridge were unaware of the seriousness of the structural damage. Awareness on the bridge of the extent of the structural damage could have decreased the decision making time, which might have saved lives.

From this disaster it was learned that instantaneous insight in the structural damage and especially, information on whether or not the hull is breached, may be of great aid during such disasters. The new concept presented in this thesis, is to acquire such instantaneous insight by measuring accelerations during the grounding accident. These measurements can nowadays even be done using a simple smartphone. The concept of estimating the damage is narrowed down to the most important question for any crew in such a situation: "do we have water ingress into the vessel?" In other words, the crew wants to know whether the hull plating has ruptured.
1.2. An Estimate through Acceleration Measurements

The aim of this study is to find supportive evidence for a new concept to estimate the raking damage in a ship grounding scenario (hereafter referred to as a raking damage scenario). This new concept is to measure the accelerations (real-time) during a raking damage scenario in order to acquire near instantaneous insight in the extent of the sustained damage. This concept is presented in figure 1.4.

![Figure 1.4: Illustration of the concept to detect the extent of damage (right picture) through acceleration measurements (left picture). The illustration is based on the TNO-ASIS large scale grounding experiments which will be presented more thoroughly in section 2.1.](image)

Why acceleration measurements? If the accelerations are known, physical quantities such as forces and energy, which are related to the grounding damage, can be calculated. One of these quantities that is often convenient to use when many different parameters are involved (as is the case in ship grounding), is the energy. The concept of damage estimation through acceleration measurements is based on a “balance of energy approach”:

"A common, simplified, approach is to split the problem into two uncoupled analyses; external mechanics and internal mechanics. The external mechanics uses global inertia forces and hydrodynamic effects to estimate the amount of kinetic energy available to be dissipated to strain energy and friction energy during collision (edit: or grounding). The internal mechanics analysis calculates the energy dissipation and distribution of damage in the two structures." [9]

A balance of energy approach then means that the energy dissipation through internal mechanics should be equal to the total energy of the system (the ship plus its added mass) from before the grounding accident took place. The reasons that this concept is thought to work:

- Easy to measure; Nowadays even every smart phone carries an accelerometer.
- Integration of the acceleration signal during contact twice, directly yields the length of the contact (and damage). \( \int \int \dddot{x} dt dt = x \).
- \( F = m \cdot \dddot{x} \) meaning that, if the mass of the grounding vessel is considered to be known, the grounding forces can be deduced. After simple calculation this gives an indication of the aforementioned energy dissipation due to contact.

In this study this concept is narrowed down further to the most important question: to what extent is it possible to detect when a plate has ruptured or not, based on acceleration measurements? This concept is illustrated in figure 1.5 through the results of a Finite Element Analysis on the left side, which shall be used to estimate plate rupture illustrated on the right side of figure 1.5. In order to study the concept of rupture detection through acceleration measurements, a series of raking damage experiments have been designed and carried out. These experiments are set-up in such a way that they reflect a realistic raking damage scenario. Such a scenario induces a loading on the ship’s hull plating that is both in-plane with the plating as lateral to the plate.
1.3. Research Questions
In order to study the concept of plate rupture detection through acceleration measurements, a set of research questions is defined. The research questions in this section will provide the framework - the scope of work - for this thesis. The main research question (in bold face) is supported by four sub questions. All research questions follow from an extensive exploratory literature review, of which a summary is reported in chapter 2. The conclusion of the literature research in section 2.3, provides the reasoning towards the research questions of the raking damage experiments:

How can rupture of a 6 mm ordinary steel plate, in a raking damage scenario with a hard spherical object, be distinguished from acceleration data?

1. Which phenomena occur in such a raking damage scenario? And what is the influence of each of these phenomena?
2. How is failure of the plate initiated? And what is a practical but accurate way to predict plate failure?
3. What is the ratio, in terms of energy dissipation, in which these phenomena occur?
4. What is the role of friction in a raking damage scenario?

1.4. Hypothesis
The hypotheses presented in this section are the preliminary answer to the research questions set out in section 1.3. The hypothesis are enumerated according to their relevant research question from section 1.3.

Based on the findings from the literature review in chapter 2 & appendix P, and the reasons listed before in section 1.2 of why this concept is thought to work, the moment of plate rupture can be distinguished clearly from acceleration measurement data in a raking damage experiment.

The moment of rupture is distinguished by an abrupt decrease in the acceleration measurements, due to the loss of structural resistance after plate rupture.

1. The plate is first plastically deformed (a combination of membrane and bending) until the failure strain is reached. The structural resistance of the plate then decreases significantly. Now mostly bending and crack propagation dissipate energy. A steady state crack propagation phase then follows, and the indenter comes to a standstill in the plating.

2. A practical way to determine a failure criterion is sought. In that sense, a Fracture Forming Limit Curve (FFLC) based on the measured elongation at a standardized tensile test method should
1.5. Structure of this Thesis

suffice as a failure criterion. The proposed method by Voormeeren et al. [29] is thought to give an accurate FFLC that can be used in conjunction with the raking damage experiments.

3. Based on observations from the Muscat-Fenech experiments [19], a significant decrease after rupture in the vertical forces (and thus accelerations) is to be expected. However, due to a higher bending stiffness of the thick plates - rather than the thin sheet metal - the decrease will be less prominent then what is seen in the ‘glancing collision’ experiments.

4. Friction will affect both energy dissipation due to contact and the fracture at ultimate loading. Both of these parameters have a large influence on the behaviour regarding failure. The energy dissipation due to friction can be accurately predicted using a model of static Coulomb friction, provided that the coefficient of friction is determined separately.

In order to verify these hypotheses a raking damage scenario is simulated. The simulation of raking damage scenario shall be met by the following requirements:

- It shall reflect a realistic raking damage scenario. This means any indenter/rock shall have displacement/velocity mainly in plane with the plate, but also perpendicular to the plate. These displacements/loadings shall occur simultaneously.
- Plastic deformation shall be introduced gradually until plate failure, so that the transition to the ruptured stage can be studied properly.
- Rupture shall occur in such a fashion that both crack initiation and crack propagation can be studied.
- The contribution of friction shall be identifiable.

Combining these requirements with the facilities available at TNO the raking damage experiments are designed. The raking damage experiment use the drop tower facility at the TNO - Structural Dynamics department to simulate a raking damage scenario. The design analysis for the experiments is reported in chapter 3 and the actual set-up of the experiments can be found in chapter 4 and appendix C. The set of requirements that is listed above will appear again at the beginning of chapter 3 and chapter 4.

1.5. Structure of this Thesis

This experimental research explores a method that estimates whether plating has remained intact, only dented or has ruptured in a raking damage scenario by merely looking at acceleration measurement data. In order to do so, a series of raking damage experiments have been designed, as a means to answer these research questions and verify the hypotheses.

Chapter 2 follows with the background literature that is required to read and fully understand this thesis. The literature review in chapter 2 contains the reasoning that leads up to the research questions, hypotheses. It treats the background information in the form of a summary of the extensive literature review that was performed at the start of this graduation project. The draft version of this extensive literature review can be found in appendix P.

A design analysis was performed for the raking damage experiments, which is reported in chapter 3. This chapter also contains the sensitivity analysis for the raking damage scenario. This sensitivity analysis was performed by Finite Element Analysis and it supports many of the concepts presented in the thesis.

In the body of this thesis work the reader will find chapter 4 and chapter 5, containing the set-up of the experiments and all the experimental results respectively. All results are presented and discussed in the light of the research questions, hypotheses and the presented literature.

The experimental results lead to the conclusions in chapter 6. Finally, possible applications and improvements will be discussed in chapter 7.

Additional to this graduation research, a contribution for a scientific conference has been written: The 36th International Conference on Ocean, Offshore & Arctic Engineering (OMAE 2017). The paper written for this conference gives the reader a condensed version (10 pages) of this thesis report. The interested reader can find the accepted draft version (as it has not yet been published at the date this thesis becomes public) in appendix A.
2 Background Literature

In the introduction a method was proposed to obtain an estimate on the damage that a ship sustained in a so called raking damage scenario. This chapter aims to provide the necessary background information based on previous research in the area of ship grounding damage analysis. Extensive research related to ship crash and grounding has been carried out over the years. An exploratory literature review into this interesting field of research was performed at the start of this graduation project, of which the draft version can be found in appendix P. This chapter is a summary of the most relevant literature for the raking damage experiments.

In order to touch upon all relevant (and somewhat historical) developments in the field of research for ship grounding damage analysis, this literature chapter is subdivided into five sections. Each section deals with a part of the grounding damage analysis and research that is relevant for this research. The first three sections look into experimental research, analytical analysis and FEA. These three form the backbone of grounding damage research. The latter two sections both zoom in on a more specific part of the grounding damage research, being failure and friction. These two phenomena have a large influence on the damage scenario, which will also be illustrated in the sensitivity analysis in section 3.3.

One of the early pioneers in the field of ship grounding damage research was Minorsky [18] in 1958. He developed an empirical correlation between the structural damage that is sustained in a ship grounding and the ship’s velocity before running aground. By analysing 26 full-scale ship accidents and collisions he developed the statistical relation of equation (2.1):

\[ E = 47.2V_{\text{plastic}} + 32.7 \]  

This equation statistically relates the energy that is dissipated during the accident, from the grounding velocity and mass of the vessel, to the volume of plastically deformed material. Because of its simplicity and limited number of required parameters is has been used a lot in grounding analysis. The drawback of this empirical formula is its same simplicity. It is only applicable to ship types similar to the ones Minorsky analysed and it only works well for a certain range of energy absorptions.

In an effort to gain more understanding of the different damage phenomena that occur in grounding with a hard rock pinnacle, research efforts moved to studying the separate phenomena that occur in a grounding accident. These phenomena were subdivided into cutting of plate and pure indentation of plates or sections. The grounding accident with the Exxon Valdez off the coast of Alaska in 1989 prompted a new boost for ship grounding related research in the 1990’s. Several research programs were started with the aims to both improve ship (and especially tanker) structures as well as to predict structural damage more accurately. These research programmes gave rise to several of the most interesting grounding damage experiments ever conducted, in which entire ship-sections were being grounded in large scale grounding experiments.
As the experimental field of research regarding ship grounding progressed, all the knowledge was combined into understanding grounding damage and its mechanisms as a whole. This resulted in complete analytical methods such as those of Wierzbicky [31] and Simonsen [26].

Computational power increased over the years, and so did the capabilities and accuracy of Finite Element Analysis (FEA) for grounding damage analysis. This gave rise to a new field of research by utilizing the capabilities of FEA for grounding damage analysis. The key to the current research efforts directed FEA for grounding analysis is an accurate prediction of failure in the simulation.

2.1. Experiments

The most obvious way to extensively study ship grounding and its mechanics is to conduct experiments. In the automotive it is standard procedure that each new series of vehicles is put through extensive crash tests before approval. Clearly, this approach would be prohibitively expensive and impractical for ships as these are mostly uniquely built, or in small series at best.

Even still, the best way to get to understand and analyse the behaviour of a ship structure in a grounding accident is by experimental research. In this subsection a brief overview of the most relevant experimental studies will be provided. This subsection also provides more background information about the experimental studies that were presented in the introduction which led to the initiation of the series of raking damage discussed within this report.

**TNO-ASIS** In the early 1990’s Japanese Association for the Structural Improvement of the Shipbuilding Industry (ASIS) started development on a series of experiments in collaboration with TNO. In these large scale grounding experiments the ship sections were fixed in a floating support ship, which can be seen in figure 2.1. The double hull test sections were 1:4 scale.

![Experimental set-up for TNO-ASIS large scale grounding experiments](image1.png)

![Photograph of the 'grounded vessel' briefly after the grounding occurred](image2.png)

Figure 2.1: The TNO-ASIS large scale grounding experiments [30].

Figure 2.1 also show the experimental set up and a photographic image of these experiments. An extensive measurement and data acquisition system was deployed during the experiments to measure speed, forces, deformations and the accelerations of both the striking ship and the rock. The results have not been made publicly available but have been reported in Vredeveldt et al [30]. The measured experiment data are available within TNO.

Figure 2.2 depicts the surge accelerations of one of the large scale grounding experiments done by ASIS-TNO. It is interesting to note that the five peaks in the acceleration time-trace before the stoppers, can be linked directly to the five transverse stiffeners of this specific ship-section.

The data from these experiments have been compared with finite element calculations. This will be touched upon in the background subsection dealing with FEA.
2.1. Experiments

**NSWC** Similar large scale experiments were performed by the United States Naval Surface Warfare Centre (NSWC). The NSWC mounted their 1:5 scale test sections on a a specially designed grounding test machine (figure 2.3).

"The overall objective and scope of this research is to understand qualitatively the structural failure mechanisms associated with grounding events for candidate double hull tanker structures." [23]

The focus in these experiments was on rupture of the inner shell. Three types of inner shell rupture initiation mechanisms were identified (figure 2.4) using high speed cameras:

I Simple transverse plate penetration.

II Transverse plate tearing away from its intersection with a longitudinal web.

III The aft end of a longitudinal web tearing away as the rock pushes transverse plate material aftward from it.

In figure 2.4 the surge forces are also shown. A similar observation can be made about these experiments as was previously made about the TNO-ASIS experiments: the tranverse stiffeners can be identified from this force-displacement curve. These observations are a clear cue that real-time acceleration measurements on board a vessel can be a proper tool to help make an estimate of the structural damage sustained in a raking damage accident.
figure 2.4: Results of the NSWC large scale grounding experiments [23].

Muscat-Fenech  Muscat-Fenech and Atkins [19] conducted a series of experiments to simulate "glancing collisions" which occur in ship grounding. They investigated the different types of denting, scoring and fracture between sheet steel of 0.8 mm thickness and different types of objects (balls, cones, pyramids and oblong blocks all with varying dimensions). The distinction between these experiments and the usual treatments of indentation and perforation is that the contact has motion parallel to the sheet as well as normal to it.

"To simulate a ship hitting a rock when underway the apparatus shown in figure 2.5 was built. The main aim was to determine the sort of horizontal and vertical forces experienced as sheet is dented, stretched and perforated as it passes over an obstacle." [19]

In figure 2.5 the set-up of the 'glancing collision' experiment is shown. The penetration depth could be varied as well as the shape of the indenter.

figure 2.5: Glancing collision experiment set up [19]

Figure 2.6a shows the side view of a bulged and then torn specimen by a spherical indenter. It clearly shows the 30° angle for the approach phase. One also notes the grid drawn on the plate to conduct strain measurements of the experiments. Later, in section 2.4 in the paragraph about the FLD, the strain results will be shown and explained for the failure strain pairs of these experiments.

The deformations seen in figure 2.6a show membrane type behaviour. Since the sheet is rather thin (0.8mm), bending of the sheet metal is not showing any influence on the deformation behaviour. Also the fold, due to the 30° approach angle, add additional stiffness and influences the deformation behaviour.
From the force-displacement curves of the glancing collision experiments in figure 2.6, it can be seen that the typical force needed for crack propagation is less than the maximum resistance of a plate. This is a clear cue that even plate rupture can be estimated from merely measuring the forces (i.e. accelerations).

2.2. Analytical

With the progression of knowledge regarding damage to grounded ships (i.e. raking damage), research became more focussed around integral models that could predict and calculate the extent of damage sustained in such an event. These integral methods were based on an analytical description of the separate phenomena that are observed in a raking damage scenario. One of the earliest accounts of such an integral model is that of Wierzbicki et al[31]. Following up on these early adopters came Simonsen in 1997 with a very complete and comprehensive method, which is described in the paragraph hereafter.

Simonsen  Simonsen [26] developed an extensive and oft quoted method to estimate the energy absorption of a ship grounding on a hard rock pinnacle. The crux of his theory was formulated as follows:

"When external loads are applied to a deformable structure, the power (the work rate) of these loads must be equal to the incremental energy stored elastically or dissipated in the structure. If a rigid-plastic structure is assumed, no elastic energy can be stored and the power of the external loads thus equals the rate of energy dissipated by plastic deformations, fracture and frictional effects on the surface of the structure. This can be expressed as equation (2.2)" [26]

\[ F_H V = \dot{E}_P + \dot{E}_c + \dot{E}_f = F_P V + \int_{S_c} p \mu V_{rel} dS_c \]  \hspace{1cm} (2.2)

- $F_H$: Horizontal grounding force in direction V
- $V$: Velocity of the vessel
- $\dot{E}_P$: Rate of plastic energy dissipation
- $\dot{E}_c$: Rate of energy dissipation in crack tip zone
- $\dot{E}_f$: Rate of energy dissipation by friction
- $F_P$: Plastic resistance force
- $p$: Normal pressure of the rock on plate element
- $\mu$: Coulomb friction coefficient
- $S_c$: Contact area between rock and plate
- $V_{rel}$: Relative velocity between rock and plate element
From equation (2.2), the two most important phenomena related to raking damage can be distinguished, being plastic deformation and friction.

The idea of equation (2.2) is to postulate the displacement and strain field of the ship bottom structure over a pre-determined rock to find the grounding force. In order to find this grounding force, the contributions of friction, plate plasticity, fracture and tearing/cutting are determined using analytical formulations of the governing mechanics. This is done for a variety of structural members in a double bottom structure:

- Intact hull plating
- Fractured hull plating
- Longitudinal web girders
- Longitudinal bulkheads
- Longitudinals
- Folding of transverse members
- Fractured transverse members

The result is a set of closed form solutions for these individual structural members, that can be used to analyse an entire bottom structure subjected to the postulated displacement and strain field.

In a second paper, Simonsen describes the validation and application of the developed analytical methods [27]. This is done by comparing with the large scale grounding experiments conducted by the NSWC [23].

![Figure 2.7: Comparison between Simonsen’s calculated ($\mu = 0.4$) horizontal and vertical forces and the measurements of NSWC no.1 specimen grounding experiment [27]](image)

The comparison in figure 2.7 seems to indicate that the method makes good comparison with the experimental results. Errors of the energy absorption are reported to be less than 10% in all four NSWC tests and the penetration to fracture of the inner shell is predicted with errors of 10-15%. Despite the fact that the total energy dissipation is predicted rather well, several discrepancies in the individual contributions, indicate that this theory lacks generality. Hence it can not readily be applied to any type of double bottom structure or in any type of grounding damage scenario.
2.3. Finite Element Analysis

In the light of the raking damage estimation method proposed in chapter 1, FEA is a necessary part of any future method for providing an accurate estimate of the structural damage. As full scale testing for each newly built vessel is prohibitively expensive and a waste of resources, FEA needs to be accurate enough to be able to predict structural response for a wide range of grounding scenarios. More thoughts on possibilities, opportunities and recommendations regarding a possible integral damage estimate method can be found in chapter 7, which deals with the application and recommendations for further research and developments.

This section highlights several examples of research regarding FEA for grounding damage analysis. These examples have been an inspiration for the current research and showcase some of the Finite Element Analysis and Modelling efforts that are ongoing in the research regarding grounding damage analysis.

**TNO-ASIS** The TNO-ASIS experiments mentioned in section 2.1 were modelled using FEA. The data from the experiments have been compared with finite element calculations in order to predict the grounding force and try and understand the energy dissipation mechanisms that occur during grounding. These are one of the earliest FEM calculations on crash and grounding analysis found in literature. Figure 2.8 shows an impression of these finite element calculations and clearly shows failing elements being deleted.

![Figure 2.8: Impression of the FEA results performed for the TNO-ASIS large scale grounding experiments](image)

**Nguyen et al.** Nguyen et al. [20] provided a procedure for estimating both the damage to a ship bottom and the associated rock shape for a ship running into a hard rock pinnacle. The method uses simplified formulas for contact forces and a model of a ship section to estimate the associated grounding forces. These are combined with linear equations of motion for a rigid body to derive the response of the vessel in a grounding situation. This has the aim to create a dynamic simulation without the need of realizing the full coupling as usually seen in coupled (internal and external mechanics) analysis. The results of this coupling have been compared with a static incremental approach, using the same simplified formulas and ship section. The comparison of these two methods is shown in figure 2.9.

From figure 2.9 it is assumed that the proposed static procedure calculates energy and grounding forces well enough. Hereafter, a procedure is proposed for identifying the rock shape and penetration depth in order to select the right estimate of the damage to the ship’s double bottom. Various rock shapes and penetration depths are calculated using the proposed static incremental procedure (figure 2.10).

From figure 2.10, the rock shape - penetration depth combination, that minimizes the error in energy dissipation is selected as the most probable rock shape - penetration depth combination(figure 2.11).
2. Background Literature

(a) Horizontal grounding force plot  
(b) Vertical grounding force plot

Figure 2.9: Grounding forces versus grounding distance. Comparison between the simplified dynamic simulation and the incremental static procedure [20]

(a) Energy - $\delta_0$ (initial height of the obstruction over the keel) for various shapes  
(b) The error of the energy estimate. The minimum error is chosen as the most probable rock shape.

Figure 2.10: Energy - Stopping Length - Penetration of one specific rock shape and as function of $\delta_0$ (initial height of the obstruction over the keel). [20]

Figure 2.11: Comparison of the initial kinetic energy with the various computed dissipated energies as a function of both rock shape and rock penetration. [20]
The ultimate goal of the FEA study of Nguyen et al., is to provide a near real-time prediction of the risk of rupture of cargo tanks and hull girder failure. However, it is realized that the accuracy of these calculations become very questionable given the uncertainties related to any grounding event. These uncertainties mainly lie in the complicated seabed topology, which is unknown more likely than not. The iterative method itself is a very refreshing idea though. The methodology seems very promising and the opportunity to create several scenarios and choose the scenario with the smallest error seems to reduce at least some uncertainties. In the light of the raking damage estimate through acceleration measurements, this research might prove to be a good direction.

Alsos et al. Alsos & Amdahl [3] carried out a series of five indentation experiments with unstiffened plated and stiffened panels. These panels are loaded laterally by a cone shaped indenter until fracture occurs (figure 2.12). The results of the experiments are compared with FEA results.

The FEA of Alsos et al. [4] is a simulation of the experiments shown in figure 2.12. The FEA paper provides a comparison between two of the most used fracture criteria in FEA of maritime crash and collision analysis. It is the second paper that reproduces the experiments published in [3] by using two different failure criteria: the RTCL damage criterion and the BWH instability criterion (more on failure criteria and which failure criteria will be used in the raking damage experiments is reported in section 2.4).
and the RTCL criterion appears to be mesh-size sensitive. This is explained by the adopted failure scaling law for the RTCL criterion which takes the element size into account. The scatter seems to be explained by the scaling rather than the RTCL criterion itself. In other simulations both criteria showed good results and predicted failure of the panels satisfactorily. Although recognizing that coarse meshes are often used in maritime crash and grounding analysis it is concluded that as small elements as possible should be used in order to capture strain concentrations well enough.

2.4. Failure

The key to structural damage analysis by using FEA, is the failure criterion that is applied. A failure criterion can be introduced in the analysis some FEA routines. When this failure criterion is exceeded, the element is deleted from the model in most FEA procedures. Most maritime or offshore applications of FEA make use of (rather large) shell elements for modelling a structure. From the maritime and offshore sector, there is considerable interest in an accurate failure criterion that can be used in conjunction with standardised material testing and large shell elements. In this raking damage study, the equivalent failure strain will be used as a failure criterion for the design analysis. A Fracture Forming Limit Curve (FFLC) that is constructed with Voormeeren’s method of single-point calibration will also be validated, using uni-axial tensile tests to predict experimental results of the raking damage experiments.

**Germanischer Lloyd criterion** There are many failure criteria “on the market”, of which the Germanischer Lloyd (GL) criterion [25] is easiest to use, as the failure strain can directly be deduced from a material certificate. However, this failure criterion has strong drawbacks, especially concerning the accuracy in relation to multi-axial stress states.

Failure strain in material certificates is often based on uni-axial tensile testing. Using these test results and accounting for the size of the elements in FEA (which are assumed to be square) equation (2.3) has been developed and is known as the GL criterion. The failure strain of the GL criterion is defined as the thinning strain (or $-\epsilon_3$ in principal strains) of such a uni-axial tensile test.

$$
\epsilon_f(l_e) = \epsilon_g + \epsilon_e \frac{t}{l_e}
$$

(2.3)

- $\epsilon_f$ Failure strain
- $\epsilon_g$ Structural uniform strain $\epsilon_g = 0.056$ [8] $\epsilon_g = 0.02$ [22]
- $\epsilon_e$ Necking strain $\epsilon_e = 0.54$ [8] $\epsilon_e = 0.65$ [22]
- $t$ Element thickness
- $l_e$ Element edge length

![Figure 2.14: Failure as a function of the element size (equation (2.3)) for t=6 mm. Based on values prescribed by Bureau Veritas [8] and NORSOK [22].](image)
For ordinary shipbuilding steel several values for $\varepsilon_g$ and $\varepsilon_x$ have experimentally been determined and published in engineering standards. Two of those experimentally determined values are reported in the table above. The values of this GL-criterion are plotted in figure 2.14 as a function of the element size.

The GL criterion is directly dependent on the ratio between the thickness and the length of the square element, as shown by figure 2.14. Because of this dependence, which is asymptotic for small values of the element length, a limit is determined for the GL criterion. It is recommended to use the GL criterion for $\frac{t}{l} > 5$ only.

**Equivalent Plastic Failure Strain**  

The equivalent (or effective) strain to failure is the standard failure option of the Finite Element software package LS-Dyna, which will be used for the Finite Element Analysis. As it is essentially the Von Mises strain, it is the most straightforward way to model take failure into account in FEA.

$$
\varepsilon_{eq} = \frac{\sqrt{2}}{3} \sqrt{(\varepsilon_1 - \varepsilon_2)^2 + (\varepsilon_1 - \varepsilon_3)^2 + (\varepsilon_3 - \varepsilon_2)^2} \tag{2.4}
$$

| $\varepsilon_{eq}$  | [-] Equivalent strain |
| $\varepsilon_1, \varepsilon_2, \varepsilon_3$  | [-] Principal strains |

In the design phase of the experiments the GL-criterion was regarded to be similar to the equivalent plastic failure strain. It was wrongly understood that the GL-criterion and the equivalent failure were the same failure criterion. Therefore, the equivalent plastic failure strain was used in the FEA, but it was used with input values based on experimentally determined values for the GL criterion (the values reported by Burea Veritas [9]). In figure 2.16, a comparison is shown between the two different failure criteria.

**Forming Limit Diagram**  

Failure criteria based on or translated to a Forming Limit Diagram (FLD) are relatively easy to comprehend and conceptualize. Moreover, they are very suitable to be applied on shell elements and make for easy post-processing of FEA results [32] [5]. The limiting strain is plotted in these FLD’s in the form of a Forming Limit Curve (FLC - for necking) or a Fracture Forming Limit Curve (FFLC - for fracture). If the analyst is only concerned with failure initiation, then they can conservatively assume that failure occurs at either necking or fracture, whichever has the lowest failure strain in a given state of stress. However, if there is desire to simulate damage propagation or reduce conservatism, then more sophisticated failure criteria would be necessary to capture the difference in strain between the onset of necking and ultimate failure [6].

The FLD is widely used in sheet metal forming. An example of such an FLD is shown in figure 2.15, where figure 2.15(a) shows an FLD with typical strain states that are encountered in engineering, and figure 2.15(b) shows results of the previously described Muscat-Fenech experiments in the FLD space. The FLD relates the major principal strain ($\varepsilon_1$) on the vertical axis, with the minor principal strain ($\varepsilon_2$) on the horizontal axis. The FLC then depicts the limiting strains that a sheet metal can undergo under various forming conditions before failure (which can occur in the form of yielding, necking, fracture, wrinkling, tearing, etc.). In figure 2.15 a forming limit diagram is depicted with various typical strain states. These typical strain states will be shown in all FLD’s in this thesis. This is shown in figure 2.16 on page 19.
Figure 2.15: Typical Forming Limit Diagram (FLD), with a single Forming Limit Curve (FLC). The straight lines sprouting from origin of the FLD show the relation between various typical strain states to pairs of major strain ($\varepsilon_1$) and minor strain ($\varepsilon_2$). [5]

The power of the FLD shows, when comparing the two formerly introduced failure criteria in such a diagram. To derive the 2D strain representation from a 3D strain case, the $\varepsilon_3$ needs to be written out of the equations. This is done by adhering to the assumption that the total volume does not change. Equation (2.5) represents this conservation of volume.

$$ (1 + \varepsilon_1)(1 + \varepsilon_2)(1 + \varepsilon_3) = 1 \quad (2.5) $$

The simplest to write out is the GL criterion. It is based on a uni-axial tensile test and uses the thinning strain ($-\varepsilon_3$) as a failure criterion. Equation (2.5) can be rewritten as equation (2.6).

$$ \varepsilon_1 = \frac{1}{(1 + \varepsilon_2)(1 + \varepsilon_3)} - 1 \quad (2.6) $$

When the thinning strain ($-\varepsilon = 0.218$ from Bureau Veritas, for 6 mm elements) is used as input in equation (2.6) the FFLC can be constructed. The resulting FFLC is plotted in the FLD comparison shown in figure 2.16.

For the equivalent plastic strain a similar conversion can be done. First, equation (2.5) is rewritten so that $\varepsilon_3$ is out of the equation:

$$ \varepsilon_3 = \frac{-\varepsilon_1 - \varepsilon_2 - \varepsilon_1 \varepsilon_2}{(1 + \varepsilon_1)(1 + \varepsilon_2)} \quad (2.7) $$

Then equation (2.7) is fed into the equation for the equivalent plastic failure strain (equation (2.4)):

$$ \varepsilon_{eq} = \frac{\sqrt{2}}{3} \sqrt{(\varepsilon_1 - \varepsilon_2)^2 + \left(\frac{\varepsilon_1 + \varepsilon_2 + \varepsilon_1 \varepsilon_2}{(1 + \varepsilon_1)(1 + \varepsilon_2)}\right)^2 + \left(\frac{-\varepsilon_1 - \varepsilon_2 - \varepsilon_1 \varepsilon_2}{(1 + \varepsilon_1)(1 + \varepsilon_2)} - \varepsilon_2\right)^2} \quad (2.8) $$

With the equivalent strain to failure set to $\varepsilon_{eq} = 0.218$, the resulting FFLC can be plot in the FLD comparison of figure 2.16 again.
2.4. Failure

Figure 2.16: FLD comparison of the failure locus from the GL-criterion and the equivalent failure strain.

The comparison of the two FFLC’s (an FFLC will also be referred to as failure locus) of the GL criterion and the is shown in figure 2.16.

The qualities of a FLD and corresponding FFLC’s (or FLC’s) are readily acknowledged. Especially due to its comprehensive nature and the suitability for FEA using shell elements, the FLD approach shall be adopted for the raking damage experiment study.

Single-point calibration of a FFLC  The idea of the single-point calibration method is that it only needs one strain state to calibrate the FFLC. This means that standardized uni-axial tensile test can be used to calibrate a more complicated failure locus which is valid for multiple different strain states from figure 2.15, such as plane strain or an equi-bi-axial strain state.

The single-point calibration procedure of the FFLC that will be used in this research is a rather complex procedure that is based upon a set of assumptions and theories. This paragraph aims to briefly mention several of the most important assumptions, without explaining all the basics or underlying theories. This paragraph is, in that respect, a very brief summary of the original paper [29] and its application for this research.

When the transformation of a 3D stress or strain space to a 2D space is made for analysis using shell elements, this implies an assumption of a state of plane stress. Voormeeren et al. [29] used this plane stress assumption to devise a method to calibrate a failure criterion using only one single failure strain on a FLD. The failure criterion that is calibrated is the Modified Mohr-Coulomb (MMC) failure criterion [7]. In order to accurately calibrate this MMC criterion, usually a minimum of five different failure tests for a state of plane stress. Voormeeren’s method uses only one single failure strain, meaning only one single test needs to be carried out. This MMC criterion can subsequently be translated to the FLD space, thereby providing a FFLC that can be used for further analysis. In this simplified manner a failure criterion can be calibrated using the failure strains of - for instance - one single tensile test. As tensile testing is part of a material certification in the maritime and offshore industry this provides a convenient procedure to calibrate an accurate failure criterion.

The MMC failure criterion is a phenomenological failure criterion, which means as much that it is
emperically determined, but supported by several mechanical theories. It predicts the failure limit for a wide range of stress tri-axilities. For more in depth information on the MMC failure criterion, please refer to Bai & Wierzbicki [7].

The MMC for a state of plane stress can be plotted in the space of equivalent plastic strain to failure as a function of the stress tri-axiality (equation (2.9)). This is done by using the procedure devised by Voormeeren et al. [29]. Subsequently MMC failure criterion can be transformed to the 2D strain space of the FLD using the relations of Lee [16] shown in figure 2.17.

\[ \eta = \frac{\sigma_m}{\sigma} = \frac{\sigma_1 + \sigma_2 + \sigma_3}{3\overline{\sigma}} \]  

| \( \eta \) | Stress tri-axiality |
| \( \sigma_m \) | Hydrostatic stress |
| \( \overline{\sigma} \) | Von Mises stress or equivalent stress |
| \( \sigma_1, \sigma_2, \sigma_3 \) | 1st, 2nd and 3rd principal stresses |

\[ \dot{\varepsilon}_f = \frac{2}{\sqrt{3}} \sqrt{1 + \alpha + \alpha^2} \epsilon_i \]  

\[ \eta = \frac{1}{\sqrt{3}} \frac{\alpha + 1}{\sqrt{1 + \alpha + \alpha^2}} \]  

The transformation of Voormeeren's failure criterion to the FLD space is shown in figure 2.18.
2.5. Friction

The Coulomb type of friction is the only model that was encountered in the literature review on grounding damage research. In this type of friction model, the frictional stress at the interface between plate and ‘rock’ is proportional to the normal pressure (equation (2.12)):

\[ F_{\text{friction}} = \mu F_{\text{normal}} \]  

(2.12)

| \( F_{\text{friction}} \) [N] | Force due to friction (perpendicular to \( F_{\text{normal}} \)) |
| \( \mu \) [-] | Coefficient of static friction |
| \( F_{\text{normal}} \) [N] | Normal force of contact |

This model is used in grounding damage analysis because of its simplicity. A simple model is very convenient in a situation such as a ship grounding, which involves many unknowns. The Coulomb friction model is only dependent on two variables, one of them being a constant (\( \mu \)) that can be determined independently. This makes it suitable for such an accidental situation, which is why many researchers ([2], [4], [20], [26], [27]) use it for ship grounding analysis.

Simonsen took the Coulomb friction model a step further and applied it in his analytical method to determine the energy dissipation in ship grounding. Figure 2.19 shows the side view of the model that was used by Simonsen.

\[ \dot{E}_f = \int_\Gamma p \mu V_{\text{rel}} dS \] from equation (2.2).

The friction model of Simonsen in figure 2.19 uses a symmetric distribution of the normal pressure,
which is a function of the “wrapping angle” (Ψ) of the plating. Despite this advanced application of the Coulomb friction model, and the observation that the total energy dissipation for the four NSWC grounding experiments is accurate within 10%, Simonsen notes in the discussion:

"Due to the simplified energy approach taken, the theory should not be expected to give a very accurate prediction of the distribution between plasticity and friction, even though it predicts the total energy dissipation well." [27]

Friction is mentioned as one of the most important parameters in a raking damage scenario. For all that, it often ends up being used as one of the tuning parameters. This is a pity, especially when the simplicity of the Coulomb friction model that is used, is considered. Therefore, Simonsen concluded that more research is needed in order to:

“better understand the special aspects of the contact mechanics” [27].

It would be especially useful to investigate whether such a static Coulomb friction model is also applicable in a dynamic grounding experiment. Essential in such an investigation are tests to determine the coefficient of friction. Also in the light of estimating the extent of damage from acceleration measurement, the contribution in the form of friction contained within these acceleration measurements needs to be determined.

2.6. Conclusion Literature Review

In this chapter 2, the literature related to the raking damage experiments is elaborated upon (the extensive draft version of the literature review can be found in appendix P). The research questions from section 1.3, are, for a large part, derived in response to the findings from the literature review in chapter 2. From large scale grounding experiments (section 2.1) it is seen that the extent of damage can be related to the acceleration or force measurements of the entire vessel. Following up on these large scale experiments, the small scale ‘glancing collision’ experiments (section 2.1) show that even plate rupture can be related to such a simple measurement. Moreover, the integral model of Simonsen (section 2.2) shows that several different phenomena are contributing to a rupture scenario.

To determine whether rupture occurred from merely assessing acceleration measurement data, the different contributions to the acceleration signal should be known. First, the occurring energy dissipating phenomena need to be determined and secondly in which ratio the different phenomena occur in a raking damage scenario. One of the major contributors in a raking damage scenario is friction. So far the contribution to friction is underexposed in the literature regarding grounding damage (section 2.5) and therefore it is imperative that it should be investigated. Lastly it should be possible to perform FEA of a raking damage scenario. The parameter that mainly determines the accuracy of simulating rupture, is the failure criterion that is used in the simulation (section 2.4). Therefore a failure criterion needs to be developed that is both valid for a raking damage scenario and practical in use for applications of the damage estimation method.
This chapter describes the design stage of the raking damage experiments. In order to study rupture detection through acceleration measurements the following requirements were set in section 1.4:

- The experiment shall reflect a realistic raking damage scenario. This means any indenter/rock shall have displacement/velocity mainly in plane with the plate, but also perpendicular to the plate. These displacements/loadings shall occur simultaneously.

- Plastic deformation shall be introduced gradually until plate failure, so that the transition to the ruptured stage can be studied properly.

- Rupture shall occur roughly in the centre of the plate, so that both crack initiation and crack propagation can be studied.

- The contribution of friction shall be identifiable.

- The experiment shall be designed within the limits of the drop tower at TNO.

In order to make the set up of the experiment so that it meets the design requirements, in the design stage FEA analyses were performed on the set up and the specimens. After the basic design was set, the final FEM was used to study perform a sensitivity study for various parameters of the specimens. The sensitivity analysis gave insight into the influence that the various material properties have on the results of the FEA and later the experiment.

### 3.1. Finite Element Model

The main preparation for the experiments was done using FEA. The explicit dynamic finite element code LS-Dyna [13] is used for this analysis. The keyword file of the preparatory analysis (without the geometry, nodes and shells) can be found in appendix B.

#### 3.1.1. Geometry

The geometry of the specimens is described in table 4.1. The specimens are modelled with square shell elements. The indenter (green) is modelled as a solid using shell elements. The model parameters that have been used in the preparatory analysis are denoted in section 3.1.2. In order to gain a better understanding of the sensitivity to the various parameters a sensitivity study has been performed which is documented in section 3.3.
3.1.2. Model Parameters

Table 3.1: The important model parameters for the finite element model used in the preparatory analysis. The LS-Dyna keyword file containing all input parameters can be found in appendix B.

<table>
<thead>
<tr>
<th>Element</th>
<th>Type</th>
<th>Size</th>
<th>Thickness</th>
<th>Integration points</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>Shell (Belytschko-Lin-Tsai)</td>
<td>20 mm</td>
<td>6 mm</td>
<td>5</td>
</tr>
</tbody>
</table>

<table>
<thead>
<tr>
<th>Material</th>
<th>Type</th>
<th>E-modulus</th>
<th>Poisson ratio</th>
<th>Yield stress</th>
<th>Density</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>Piecewise linear plastic (24)</td>
<td>210 GPa → [10]</td>
<td>0.3 → [10]</td>
<td>236.2 MPa → [10]</td>
<td>7850 kg m⁻³ → [10]</td>
</tr>
</tbody>
</table>

<table>
<thead>
<tr>
<th>Strain Rate</th>
<th>Type</th>
<th>Cowper-Symonds ( \sigma_y(\dot{\varepsilon}) = \sigma_y \cdot \left( 1 + \frac{\dot{\varepsilon}}{C} \right)^{1/p} )</th>
</tr>
</thead>
<tbody>
<tr>
<td>C</td>
<td>4000 s⁻¹ → [10]</td>
<td></td>
</tr>
<tr>
<td>p</td>
<td>5 → [10]</td>
<td></td>
</tr>
</tbody>
</table>

<table>
<thead>
<tr>
<th>Failure</th>
<th>Type</th>
<th>Equivalent plastic strain</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>Criterion</td>
<td>0.218 → [9]</td>
</tr>
</tbody>
</table>

<table>
<thead>
<tr>
<th>Rigid</th>
<th>D.O.F.</th>
<th>Z-direction (see section 3.1.1)</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>Initial velocity</td>
<td>-6 ms⁻¹ in Z-direction</td>
</tr>
<tr>
<td></td>
<td>Mass</td>
<td>4000 kg</td>
</tr>
</tbody>
</table>

<table>
<thead>
<tr>
<th>Contact</th>
<th>Type</th>
<th>Automatic surface to surface</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>Friction coefficient</td>
<td>0.3</td>
</tr>
</tbody>
</table>

**Material stress-strain curve**

The stress-strain curve for the piecewise linear plastic material model used for the steel plate is derived from the DNV recommended practice C208 on non-linear FEA [10] as are most of the material properties.
3.2. FEA results

Figure 3.3 shows a graphical representation of the results for the base case FEM. This is the FEM that represents the final design, which was used for the raking damage experiments. The final experimental set up is reported in chapter 4. The figure depicts the first element being deleted in the centre of the plate, which was one of the requirements for the experiment set up. From here on the crack propagates in the drop-direction of the indenter.

The acceleration time trace in figure 3.4 contains all elements previously discussed. In the first 0.046 s, the accelerations increase as the indenter travels down and into the plate. At 0.046 s the
first element fails, which can be seen from the acceleration time trace as a sharp decrease of the FEA acceleration result, directly after 0.046 s. When the first element is deleted, the acceleration (force) on the indenter increases again, until the next element fails. When the next element fails the acceleration reduces again. This failing of elements causes a saw-tooth like response in the results for FEA. This is not a physical phenomenon, however, it is expected that real crack propagation in the experiments does show a similar response in the acceleration signal. The similar response is then caused by a kind of stick-slip behaviour that is often seen in dynamic crack propagation.

The acceleration time trace can readily be transformed into a force-displacement graph. In figure 3.5 this was done for the results of the base case FEA. Such a force displacement curve makes comparison of different graphs possible, and this will be done throughout the report.
3.2.1. Requirements Check
Based on the requirements set by the hypothesis in section 1.4, the final design has been checked with
the FEA results from section 3.2. The results of this requirements check is presented in section 3.2.1.

Table 3.2: Check of all the requirements for the experimental set-up that were set in section 1.4.

<table>
<thead>
<tr>
<th>Requirement</th>
<th>Result</th>
</tr>
</thead>
<tbody>
<tr>
<td>Set-up reflects a realistic raking damage scenario.</td>
<td>+</td>
</tr>
<tr>
<td>Gradual introduction of plastic deformation.</td>
<td>+</td>
</tr>
<tr>
<td>Rupture in the centre of the plate.</td>
<td>+</td>
</tr>
<tr>
<td>Contribution of friction identifiable</td>
<td>?</td>
</tr>
<tr>
<td>Experimental set-up within limits of drop tower at TNO</td>
<td>+</td>
</tr>
</tbody>
</table>

3.3. Sensitivity Analysis
A sensitivity analysis was performed using the same model as the final design analysis. The aim is to
study the sensitivity of the model for the following input parameters:

- Failure criterion
- Friction coefficient
- Yield stress

These parameters have been selected based on the research questions (section 1.3). For each part of the sensitivity study, one of the aforementioned input parameters is varied to study the influence on the accelerations and the related forces of the drop mass.

3.3.1. Failure Criterion
The failure strain in the preparatory analysis is based on the equivalent plastic strain to failure. Failure
strain in material certificates is often based on uni-axial tensile testing. The standard failure option in
LS-Dyna uses the equivalent plastic strain to failure as a criterion to delete an element.
Using tensile test results and accounting for the size of the elements in FEA (which are assumed to be square) equation (2.3) has been developed and is known as the GL criterion. The GL-criterion is scaled according to the length of the square element (see section 2.4).

Based on equation (2.5), the equivalent strain of equation (2.4) can be plotted in the FLD diagram. This gives the failure limit that LS-Dyna will use to delete elements from the simulation. In figure 3.6 the FFLC based on equivalent plastic strain to failure, is plotted for the base case FEA. Also plotted in the same figure is the strain result - in the form of a strain path - for the element that was deleted first.

Figure 3.6 shows the FLD for the base case FEA with a FFLC which is based on equivalent plastic strain to failure (the red curve), which was derived for figure 2.16 as well. The red line that start in the origin is the strain path for the element in this simulation that fails (is deleted) first. It follows a near straight path until it reaches the FFLC, after which the element is deleted and all strains for the deleted element are set to zero (hence the straight line from the FFLC, back to the origin).

For the design stage the failure strain was based on the minimum values prescribed by Bureau Veritas [8] for the failure strain based on the GL-criterion, which is 0.218 for 6 mm plate thickness. For the sensitivity analysis the values will be varied around this recommended minimum failure strain.

Sensitivity to different values for the equivalent plastic strain at failure is depicted in figure 3.7 and figure 3.8. The accelerations show a gradual build up during initial plastic deformation. After first element deletion (plate rupture) the accelerations decrease significantly and a more or less constant phase of crack propagation follows which also shows a variations due to element deletion.

Figure 3.7: Accelerations for sensitivity study of failure criterion.

Figure 3.8: Force-Displacements for sensitivity study of failure criterion.
For the different failure criteria the accelerations follow the same path, both in the acceleration time-trace as in the force-displacements, but do not reach the same maximum at plate failure. A significant difference in the maximum accelerations is observed between 0.12 equivalent plastic strain with 34.2 \( \text{ms}^{-2} \), and a failure strain of 0.32 only reaches 135 \( \text{ms}^{-2} \). The highest failure criterion does not reach the equivalent plastic strain required for element deletion before the drop mass comes to standstill. This means no elements are deleted and no plate rupture occurs for failure criterion of 0.32 equivalent plastic strain. It is also interesting to see that the transition from build up to the plate rupture phase is much more gradual with a lower failure criterion. No distinct reduction in the acceleration signal after first element deletion is observed for these lower failure strains. Due to the large differences in acceleration at rupture and the change in mechanical behaviour the model seems to be highly sensitive to the chosen failure criterion.

### 3.3.2. Element Size

A mesh convergence study is usually performed when making a model for finite element analysis. In FEA of crashworthiness and failure of structures the failure criterion is dependent on the size of the elements. This makes a mesh convergence study difficult to interpret because the failure criterion has a very large influence on the results of the FEA (see section 3.3.1). Moreover, it is recommended that an often used failure criterion, the aforementioned GL-criterion, should only be used for \( \frac{1}{\tau} > 5 \) \([11]\). The element size used in the model of the preparatory analysis yields a \( \frac{1}{\tau} = \frac{20}{3} \approx 3.3 \), which is already small compared to the recommended value. Therefore, further mesh convergence study is not practically applicable and the chosen square mesh with an element length is deemed as accurate as practically possible.

### 3.3.3. Friction Coefficient

In section 2.5 the important, but slightly mysterious role of friction is illustrated. In order to get a grip on the actual influence of this friction it is included in this sensitivity analysis. For the design analysis, "static Coulomb friction" was applied on the FEM. For this sensitivity study the coefficient of static friction is varied around the base case (the FEA of the design analysis). The friction coefficient for the base case analysis is based on values used by other researchers \([27], [4], [2]\). These researchers all used \( \mu = 0.3 \) for steel-steel contact in their models.

![Figure 3.9: Accelerations for sensitivity study of friction coefficient.](image)
Figure 3.9 and figure 3.10 show the results for varying the friction coefficient. Increasing the friction coefficient results in a more rapid increase of the accelerations and a higher peak acceleration (at the moment of plate rupture). The influence on the peak accelerations of a variation of the friction coefficients is significant. Without friction ($\mu = 0$) the maximum acceleration is $43.3\text{ms}^{-2}$ and a friction coefficient of 0.6 results in $133.8\text{ms}^{-2}$ at plate rupture.

A decrease in the accelerations directly after plate rupture remains detectable for small friction coefficients. The transition to plate rupture does become less distinct when the friction coefficient is smaller but it remains present even for the simulation without friction ($\mu = 0$). Large differences in accelerations at plate rupture are observed for altering the friction coefficient. Even though the transition to a ruptured plate becomes less prominent for smaller friction coefficients, the model seems to be highly sensitive to the friction coefficient.

### 3.3.4. Yield Stress

Another parameter that is thought to have a large influence on model behaviour is the yield stress. A higher yield stress means more force is needed to plastically deform the steel plate. A larger deformation force automatically means that the accelerations will be larger as well. The yield stress is varied to values higher than the minimum required yield stress of 235 MPa. By increasing the yield stress, the entire material curve is translated with a similar increase as the yield stress. What this translation of the material curve looks like, when varying the yield stress, can be seen in figure 3.11.
3.3. Sensitivity Analysis

The sensitivity to the yield stress of the plate steel is limited, as can be seen in figure 3.12 and figure 3.13. Even though the accelerations at plate rupture vary between 84.0 m/s² for $\sigma_{\text{yield}} = 265$ MPa and 94.3 m/s² for $\sigma_{\text{yield}} = 362$ MPa there is no clear trend. The resistance of the plate does seem to increase slightly leading with a higher yield stress. This leads to a more rapid increase (steeper slope) of the accelerations, which is to be expected. Due to the motion being mostly in plane with the plate this increase in resistance does not have a significant influence on the maximum accelerations at plate rupture. Therefore it can also be concluded that the yield stress in general does influence the acceleration results slightly. Compared to the failure criterion and the friction however, the influence of the yield stress is limited.

3.3.5. Strain Rate Effects

Strain rate effects are modelled using the Cowper-Symonds relation that increases the yield stress when higher strain rates occur (see table 3.1).

Because the sensitivity to an increase in the yield stress is relatively small, the sensitivity to the strain rate is also considered to be small. With increasing strain rates, the yield stress tends to increase. An increased yield stress does not have a large effect on this model, which was concluded from section 3.3.4.
3.3.6. Conclusion Sensitivity Analysis

Compared to the sensitivity for friction and the failure criterion, the yield stress does not have a large influence on the acceleration results. Concluding the sensitivity analysis it can be stated that the model is particularly sensitive to two input parameters:

- Failure criterion
- Friction coefficient

In that sense it is critical that these are estimated correctly. In the literature study it was shown that a failure criterion applicable for each stress state is difficult to determine from standardised material testing only. How to approach friction for this type of damage scenario seems to be unclear as well. In general, it seems both parameters require extensive additional testing before being of practical use in raking damage predictions and estimates.

3.4. Design Calculations for the Experimental Set-up

In order to check the strength and stiffness of the experimental set up, the FEA is used to do calculations in the design stage. Two thing need to be checked via calculations. The strength and stiffness of the frame. The frame needs to be able to withstand the forces during the raking damage experiment. Moreover, the stiffness needs to be calculated in order to verify the boundary conditions that are used in the FEA for the specimens. Another part of the experiment set up that is heavily loaded is the indenter. It needs to be able to withstand the experiments so the minimum yield stress of the material needs to be determined.

Figure 3.14: Normal forces in X-direction in the plate. A clamped boundary condition has been applied on the numbered nodes. These nodes are the nodes for which the nodal forces are plotted in figure 3.15. These nodal forces are subsequently used to determine the loading on the support frame.
3.4. Design Calculations for the Experimental Set-up

3.4.1. The Support Frame
In the FE model used for preparation the plate - specimen - is clamped along the vertical edges (see figure 3.1b).

Loading on the Frame
The loading on the frame is determined based on the maximum nodal forces during the FEA of the experiment along the nodes of the clamped edge. Figure 3.1b shows the nodes that have a clamped boundary condition imposed on them. The reaction forces on this boundary condition are determined using the results of the FEA. Figure 3.14 shows the normal forces in X direction in the plate, which eventually act upon the selected nodes (also indicated in figure 3.14 as the numbered nodes).

Response of the Frame  The response of the support frame, as well as its required strength, is checked by imposing the loading on the support frame. The loading on the frame is based on the finite element calculations and are derived from the nodal forces along the clamped boundary condition of the plate. These nodal forces are taken in the x-direction on the plate (see figure 3.1b). The results for determining these nodal forces can be seen in figure 3.15

![Figure 3.15: A plot of the nodal forces and the resulting shear forces and bending moments on the support frame. The nodal forces are results of the FEA.](image)

Figure 3.16 shows how the nodal forces, derived from the FEA, are applied on the support frame. The purple line is the same nodal forces line that is shown in figure 3.15. The dash dotted line indicates the resulting deflection. The boundary condition of the beam in the support frame are taken, conservatively, as simply supported.

![Figure 3.16: Bending beam with simply supported boundary conditions. The nodal forces (purple line) from FEA are applied to this beam. The dash-dotted line indicates the deflection.](image)
The response of the support frame on the imposed loading is determined using Euler-Bernoulli beam theory:

\[ M(x) = \int V(x) \, dx \]

\[ \sigma_{\text{max}} = \frac{M_{\text{max}} w}{I} = 31.04 \text{MPa} \]

\[ \theta(x) = \frac{1}{EI} \int M(x) \, dx \]

\[ y(x) = \int \theta(x) \, dx \]

Figure 3.17 shows the deflection - \( y(x) \) - and slope - \( \theta(x) \) - of the support frame of the specimens in the experiment. The frame is made from 140 x 140 mm square tube with 12.5 mm wall thickness (see chapter 4 for all dimensions of the support frame). The boundary conditions on the support frame itself are assumed as simply supported at the ends which is a conservative assumption.

![Figure 3.17: plot nodal force](image)

The slope is smaller than 0.05 mm and the deflections are maximum 0.02 deg (figure 3.17). It is concluded that a clamped boundary condition along the edge is a reasonable assumption, especially considering that the boundary condition for the support frame itself is rather conservative.

### 3.4.2. The Indenter

In the FEM the indenter is modelled as a rigid, while for the experiments, it will be machined out of high tensile steel.

**Loads on the Indenter**  The indenter is loaded by its contact with the plate by a vertical force \( F_{\text{vertical}} \) and an axial force \( F_{\text{axial}} \), as shown in figure 3.18. This loading imposes a normal force in axial direction and a bending moment around the support point. The normal forces are determined by the total of the reaction forces in Y-direction (the same as axial direction of the indenter) from figure 3.14. The maximum axial force is divided by the surface area of the indenter for the maximum axial stress. The maximum bending moment is determined by equation (3.5), taking the maximum acceleration from figure 3.4.
3.4. Design Calculations for the Experimental Set-up

![Image](image.png)

Figure 3.18: Loading on the indenter by the contact with the plate - specimen.

\[ M_{\text{bend}} = m_{\text{max}} \cdot \ddot{z} \cdot L_{\text{tip-support}} \]  \hspace{1cm} \text{(3.5)}

\[ \sigma_{\text{bend}} = \frac{M_{\text{bend}} \cdot r_{\text{indenter}}}{I_{\text{indenter}}} \]  \hspace{1cm} \text{(3.6)}

\[ \sigma_{\text{norm}} = \frac{F_{\text{axial}}}{\pi \cdot r_{\text{indenter}}^2} \]  \hspace{1cm} \text{(3.7)}

Table 3.3: Variables and input for calculating minimum required strength of the indenter.

<table>
<thead>
<tr>
<th>Variable</th>
<th>Description</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>( M_{\text{bend}} )</td>
<td>Bending moment [Nm]</td>
<td>4000 kg</td>
</tr>
<tr>
<td>( m )</td>
<td>Indenter mass [kg]</td>
<td>4000 kg</td>
</tr>
<tr>
<td>( \ddot{z}_{\text{max}} )</td>
<td>Vertical acceleration [m/s²]</td>
<td>89.3 [m/s²]</td>
</tr>
<tr>
<td>( L_{\text{tip-support}} )</td>
<td>Indenter length from tip to support [m]</td>
<td>0.17 m</td>
</tr>
<tr>
<td>( \sigma_{\text{bend}} )</td>
<td>Bending stress [MPa]</td>
<td></td>
</tr>
<tr>
<td>( r_{\text{indenter}} )</td>
<td>Radius of the indenter [m]</td>
<td>0.075 m</td>
</tr>
<tr>
<td>( I_{\text{indenter}} )</td>
<td>Moment of inertia of the indenter [m⁴]</td>
<td></td>
</tr>
<tr>
<td>( \sigma_{\text{norm}} )</td>
<td>Normal stress [MPa]</td>
<td></td>
</tr>
<tr>
<td>( F_{\text{axial}} )</td>
<td>Axial force on the indenter [N]</td>
<td>256.6 \cdot 10^3 N</td>
</tr>
</tbody>
</table>

**Minimum required strength of the Indenter**  
The minimum required strength of the indenter is governed by the highest stresses in the indenter. These maximum stresses determine the absolute minimum yield strength of the material the indenter is made of. To calculate this, Bernoulli beam theory is used, which is similar to the calculation of the support frame. The indenter is assumed clamped at its support (see drawings in appendix C).

Table 3.4: Maximum loading on the indenter during the experiments, based on the FEA. This maximum loading determines the minimum yield strength of the material for the indenter.

<table>
<thead>
<tr>
<th>Stress Type</th>
<th>Maximum Loading [MPa]</th>
</tr>
</thead>
<tbody>
<tr>
<td>Normal Stresses</td>
<td>183.3 MPa</td>
</tr>
<tr>
<td>Bending Stresses</td>
<td>14.5 MPa</td>
</tr>
<tr>
<td><strong>Total</strong></td>
<td><strong>197.8 MPa</strong></td>
</tr>
</tbody>
</table>

The absolute minimum for the yield stress of the indenter material is 197.8 MPa. However, as the sensitivity analysis in section 3.3 showed that large variation in maximum acceleration can occur, it is recommended to apply a safety factor to this minimum yield stress. The material used for construction of the indenter is S355J2+N steel with a minimum yield of 355 MPa.
Experimental Set-up

The set up of these experiments is described in this chapter. Section 4.2 of this chapter sets out to explain the various measurements on the experiments in correspondence with the research questions that are presented in section 1.3.

Figure 4.1: Visual of the experimental set-up.

(a) Drop tower  (b) Close-up
The experiments shall meet the requirements that were set about by the research questions and hypotheses in section 1.4:

- The experiment shall reflect a realistic raking damage scenario. This means that any indenter/rock shall have displacement/velocity mainly in plane with the plate, but also perpendicular to the plate. These displacements/loadings shall occur simultaneously.
- Plastic deformation shall be introduced gradually until plate failure, so that the transition to the ruptured stage can be studied properly.
- Rupture shall occur roughly in the centre of the plate, so that both crack initiation and crack propagation can be studied.
- The contribution of friction shall be identifiable.
- The experiment shall be designed within the limits of the drop tower at TNO.

4.1. Set-up

Figure 4.1 shows an overview and figure 4.2 the side view, with the coordinate system that is used, of the experimental set-up. More detailed graphics and technical drawings of various parts of the set-up can be found in appendix C. The concept is to dynamically simulate a grounding loading on a ship's hull plating. FEA has been used for designing the experimental set up, which is reported in section 3.1. The drop-tower facility at the TNO - Structural Dynamics laboratory tower is used to perform the raking damage experiments. The inertia in this case comes from the "rock" (represented by the drop-mass with an indenter) instead of the "ship" (represented by the steel plates being the specimens). All relevant parameters are reported in table 4.1.

Figure 4.2: Side view of the experimental set up with the coordinate system that is used.
The drop mass is 4575 kg; the maximum drop height is 1.5 m. The mass and the drop height are chosen for rupture to initiate roughly in the centre of the plate. This choice is based on the preparatory analysis performed with FEA (chapter 3). The indenter represents the rock pinnacle with which a vessel collides. The shape of the indenter is a cylinder with spherical ends which have a radius of 75 mm. It is chosen such that plastic deformation is gradually introduced (denting) until rupture occurs, after which a phase of crack propagation in combination with plastic deformation follows. The two specimens are placed at an angle of 15° to the drop direction. The slight angle to the drop direction ensures that plasticity is gradually introduced. A combination of denting (or bulging) and friction precedes rupture and crack propagation. The dimensions of the plate are 800 x 600 x 6 mm. The width represents a typical stiffener spacing used in maritime structures, and the thickness represents a minimum thickness as used in maritime structures. The plate is fixed along the vertical edges to a rigid steel frame representing clamped boundary conditions. The specimens are made of certified grade A shipbuilding steel (the material certificate can be found in appendix L).

Table 4.1: Parameters used for the experimental set up of the raking damage experiments. Technical drawings of the indenter, specimens and support frame can be found in appendix C.

<table>
<thead>
<tr>
<th>Drop tower</th>
<th>Drop mass</th>
<th>4575 kg</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>Maximum drop height</td>
<td>1.5 m</td>
</tr>
<tr>
<td>Indenter</td>
<td>Shape</td>
<td>Spherical</td>
</tr>
<tr>
<td></td>
<td>Radius</td>
<td>75 mm</td>
</tr>
<tr>
<td></td>
<td>Material</td>
<td>Steel (S355J2+N)</td>
</tr>
<tr>
<td>Specimens</td>
<td>Steel grade</td>
<td>A</td>
</tr>
<tr>
<td></td>
<td>Thickness</td>
<td>6 mm</td>
</tr>
<tr>
<td></td>
<td>Width (between supports)</td>
<td>600 mm</td>
</tr>
<tr>
<td></td>
<td>Height</td>
<td>800 mm</td>
</tr>
<tr>
<td></td>
<td>Fixation</td>
<td>flanged &amp; bolted</td>
</tr>
<tr>
<td>Support frame</td>
<td>Angle with drop direction</td>
<td>15°</td>
</tr>
<tr>
<td></td>
<td>Square tube dimensions</td>
<td>140 x 140 x 12.5 mm</td>
</tr>
</tbody>
</table>

4.2. Measurements

The measurements that are performed on the experiments need to answer the research questions. This section will describe which measurements are performed and how these measurements can be related to each research question.

The measurements performed during the raking damage experiments are listed below. The location of all measurement equipment can be seen in figure 4.3 and figure 4.4. The specifications of all measurement equipment can be found in appendix G.

- Two accelerometers on drop mass
- High speed cameras
- Strain gauges on the indenter
- Infra-Red camera
- Displacement meter

Four high speed cameras will be set up: two sets of two cameras on the outside of each of the specimens. A randomized spot pattern is applied to the specimens from which a 3D displacement and strain field will then be deduced using Digital Image Correlation (DIC). The commercial software ARAMIS Professional [28] is used to compute the displacements and strains with the DIC procedure.
Figure 4.3: Set up of the measurements for the raking damage experiments - Accelerometer & strain gauges on the indenter.

Figure 4.4: Set up of the measurements for the raking damage experiments - Two (of the four) high speed cameras for DIC measurements and the IR camera.
4.2. Measurements

4.2.1. Measurements vs. Research Questions
The research questions can be coupled to the measurements as follows:

1. **Which phenomena occur in such a raking damage scenario? And what is the influence of each of these phenomena?**
   The high speed images enable studying in what way plate rupture initiates.

2. **How is failure of the plate initiated? And what is a practical but accurate way to predict plate failure?**
   The high speed cameras and a random spot pattern on the specimens allow to calculate the 3D strain field using DIC. The derive a failure criterion these data are compared with the material properties derived from the tensile testing section 4.2.3.

3. **What is the ratio, in terms of energy dissipation, in which these phenomena occur?**
   From the acceleration measurements the energy dissipation can be calculated. This is then combined with the exact moment of plate rupture, which can be found using the high speed images, to determine the energy dissipation during the different stages of the raking damage experiment.

4. **What is the role of friction in a raking damage scenario?**
   The role of friction is the most complex to quantify. The IR camera will be used to quantify the total energy dissipation in the plate. The dissipation through plastic straining will be deduced using the DIC strainfield. A separate friction test (see section 4.2.2) will be performed to determine the friction coefficient for coulomb friction. The axial force in the indenter will be determined using the strain gauges and combined with the displacement field. The normal force on the surface and corresponding friction force will be determined from examining the indenter and compared to the derived value from the measurements.

A synchronized trigger signal is used to ensure accurate time tracing of the measurement data and the high-speed footage of the DIC. Thickness measurements are performed before and after the experiment to find the through-thickness strain that enables verification of the DIC strain results. The results of these thickness measurement are reported in appendix I.
4.2.2. Friction Coefficient Test
The static friction coefficient of both steel on steel and lubricated steel on steel is determined using the set-up shown in figure 4.5 and figure 4.6.

![Figure 4.5: Photo of the test set-up to determine the friction coefficient.](image)

![Figure 4.6: Experimental set-up for determining the static friction coefficient.](image)
4.2. Measurements

Figure 4.7: Photo of the specimens used for determination of the friction coefficients.

The specimens for the friction tests (figure 4.7) are placed in the enclosures in the two sliding plates. Cylinder A applies a force of 50kN and the force needed to slide the lower clamping plate out then determines the friction coefficient from equation (2.12).

4.2.3. Tensile Testing

To determine the material properties a series of tensile tests are performed according to the international standard NEN-EN-ISO-6892-1:2016 [14]. These are done with coupons (see figure 4.8 and table 4.2) cut from the same plate as the specimens are made of. Two sets of specimens are tested, each set cut from the plate with a 90 degree orientation to one another in order to capture differences in rolling direction (which is unknown for the specimens). The testing procedure is the same testing procedure that was used to determine the material properties found on the material certificate (appendix L).

Figure 4.8: Photo of the flat tensile test specimens. The two sets of tensile specimens (L & D) were cut out with a 90 degree orientation to one another.
Table 4.2: Initial parameters of the tensile test specimen: thickness ($a_0$), width ($b_0$), cross sectional area of the parallel length ($S_0$) and the original gauge length ($L_0$).

<table>
<thead>
<tr>
<th></th>
<th>$a_0$ [mm]</th>
<th>$b_0$ [mm]</th>
<th>$S_0$ [mm$^2$]</th>
<th>$L_0$ [mm]</th>
</tr>
</thead>
<tbody>
<tr>
<td>F20354-D1</td>
<td>6.1</td>
<td>20.1</td>
<td>122.0</td>
<td>62.4</td>
</tr>
<tr>
<td>F20354-D2</td>
<td>6.1</td>
<td>20.0</td>
<td>122.1</td>
<td>62.4</td>
</tr>
<tr>
<td>F20354-D3</td>
<td>6.1</td>
<td>20.1</td>
<td>122.4</td>
<td>62.5</td>
</tr>
<tr>
<td>F20354-L1</td>
<td>6.1</td>
<td>20.0</td>
<td>122.0</td>
<td>62.4</td>
</tr>
<tr>
<td>F20354-L2</td>
<td>6.1</td>
<td>20.1</td>
<td>122.9</td>
<td>62.6</td>
</tr>
<tr>
<td>F20354-L3</td>
<td>6.1</td>
<td>20.0</td>
<td>122.5</td>
<td>62.5</td>
</tr>
<tr>
<td>average</td>
<td>6.1</td>
<td>20.0</td>
<td>122.3</td>
<td>62.5</td>
</tr>
</tbody>
</table>

The results of these tensile tests are reported and discussed in section 5.1.1.
Experimental Results & Discussion

The main goal of this research is to study rupture detection in a raking damage scenario through acceleration measurements. In order to study rupture detection, a series of experiments using a drop tower were carried out. The experimental set-up is presented in chapter 2. In the current chapter the experimental results will be presented and discussed. To answer all research questions (section 1.3), the chapter is subdivided in the following four parts:

1. Failure
2. Accelerations
3. Friction
4. Discussion

The first part, which is about failure, reports of what happens in a raking damage scenario, how failure initiates in the steel plates and concludes with a prediction for a Fracture Forming Limit Curve (FFLC) based on a single uni-axial tensile test. The acceleration part deals with the main research question, whether or not it is possible to estimate plate rupture from acceleration measurement only. Everything that is related to friction is reported in the third section. The chapter concludes with a general discussion on the experimental results.

In total four raking damage experiments were performed in this experimental campaign. An overview of the four experiments is presented in table 5.1.

Table 5.1: Overview of the four raking damage experiments performed. The drop heights that are indicated are approximate drop heights that were measured from the location the indenter touches both plates.

<table>
<thead>
<tr>
<th>Experiment 1</th>
<th>Experiment 2</th>
<th>Experiment 3</th>
<th>Experiment 4</th>
</tr>
</thead>
<tbody>
<tr>
<td>0.5 m (drop 1-1)</td>
<td>1.0 m (drop 2-1)</td>
<td>1.5 m (drop 3)</td>
<td>1.5 m (drop 4)</td>
</tr>
<tr>
<td>0.5 m (drop 1-2)</td>
<td>1.5 m (drop 2-2)</td>
<td></td>
<td></td>
</tr>
<tr>
<td>0.5 m (drop 1-3)</td>
<td></td>
<td></td>
<td>Reduced friction</td>
</tr>
<tr>
<td>0.5 m (drop 1-4)</td>
<td>Reduced friction</td>
<td></td>
<td>Reduced friction</td>
</tr>
</tbody>
</table>

The eventual aim of the raking damage experiments set up was that the plate would rupture in one single drop. To get familiar with the test set-up the first experiment was divided into four smaller drops of approximately 0.5 m. For the second drop test the drop heights were increased to be able to reach plate rupture within one drop. The third and the fourth raking damage experiment were performed with reduced friction in order to study and quantify the influence of friction on the accelerations and failure mechanism. The friction was reduced by sanding the plate surface and applying grease to both the plate surface and the indenter. The influence of this treatment on the friction coefficient is studied through separate tests. The results of these tests are presented in section 5.3.1. Separate tensile tests were done, using the same material the specimens for the raking damage experiments were made of. The results of these tensile tests are reported and discussed in section 5.1.1, which will also treat a conversion of engineering stress-strain to true stress-strain values for these tensile tests.
Figure 5.1: Photograph of a ruptured specimen directly after drop 3.

Figure 5.2: A comparison of the two different plates of the same experiment (drop 3). In each of the raking damage experiments only one of the two plates ruptured.

Figure 5.1 and figure 5.2 show the test set-up directly after the experiments. In this case drop 3 is shown, which was performed with reduced friction. In drop 3, plate rupture was reached within only one drop.
5.1. Failure

Rupture of the plate is designated as plate failure. The moment a visual crack appears at the outer surface, the plate is assumed to be ruptured. After the rupture initiation, a phase of crack propagation follows until the crack has reached the end of the plate. This sequence of plate rupture can be seen in great detail through the high speed images in figure 5.3. These high speed images were also used for the DIC measurements (figure 5.4).

![Figure 5.3: Typical sequence of plate rupture for these raking damage experiments. Recorded with the high speed cameras.](image)

The sequence of plate rupture in figure 5.3 shows the first development of a through-thickness crack on the left picture, which initiates on top of the contact point of the indenter with the plate. The crack quickly travels both upward and downward and eventually travels ahead of the indenter until it reaches the end of the plate. The crack develops near the lower edge of the specimen. This causes the crack to advance ahead of the indenter to the edge of the plate. Because of this close proximity to the edge of the specimen, the envisaged stage of crack growth does not occur in the raking damage experiments.

Figure 5.4 shows the results that are obtained through Digital Image Correlation (DIC). The picture in figure 5.4 is the actual high speed camera image, with the DIC results as an overlay over the original picture. The randomized pattern of white spots (which can also be seen in figure 5.3) allows the computer to mark and track locations on the plate. When the displacements of all these white spots are known, the strains can be computed.

In the phase of crack initiation, no necking or extreme localisation of the strains is observed. This was confirmed by ball-micrometer measurements in the vicinity of the crack, which are presented in table 5.2. It is however noticed that the large plastic strains mainly occur in the area surrounding the indenter, which can clearly be seen from figure 5.4. Outside this area, the strains remain relatively small and mainly displacement out of plane, rather than in plane strain is observed. This is different compared to the deformation patterns seen in the Muscat-Fenech glancing collision experiments [19], where the sheet-metal behaved rather more like a membrane. This is a good indication that is useful to do raking damage testing with larger thickness. In this way a steel plate is test with a plate thickness that is also used for real ship structures.
Experimental Results & Discussion

Figure 5.4: Engineering Strains from high speed DIC measurements of drop 3 briefly before plate rupture occurs.

Table 5.2: Engineering Strains at failure from DIC and ball-micrometer thickness measurements of the cracked specimens. A complete overview of the strains at failure can be found in appendix H. All thickness measurements before and after are reported in appendix I.

<table>
<thead>
<tr>
<th>Experiment</th>
<th>$\epsilon_1$</th>
<th>$\epsilon_2$</th>
<th>$\epsilon_3$</th>
<th>$\epsilon_3$ pt.2/3</th>
<th>measured $\epsilon_3$</th>
</tr>
</thead>
<tbody>
<tr>
<td>Experiment 1</td>
<td>-*</td>
<td>-*</td>
<td>-*</td>
<td>-*</td>
<td>-0.38</td>
</tr>
<tr>
<td>Experiment 2</td>
<td>0.61</td>
<td>0.07</td>
<td>-0.42</td>
<td>-0.37</td>
<td>-0.38</td>
</tr>
<tr>
<td>Experiment 3</td>
<td>0.48</td>
<td>0.04</td>
<td>-0.35</td>
<td>-0.32</td>
<td>-0.30</td>
</tr>
<tr>
<td>Experiment 4</td>
<td>0.48</td>
<td>0.06</td>
<td>-0.36</td>
<td>-0.28</td>
<td>-0.33</td>
</tr>
</tbody>
</table>

Data about the strains at failure are retrieved from the experiments through DIC and ball-point micrometer measurements. All these strains at failure* are reported in Table 5.2. The strains that have been found can be characterized by a constrained plane strain condition.

*DIC strain at failure data for drop 1 are unusable and are therefore neither used for comparison and calculation, nor presented here.
All relevant strain components are reported in the boxes at the top of figure 5.4 and in table 5.2. All of the principal strains that are reported in table 5.2 have been acquired in the same manner and with the same strain reference length of approximately 20mm (which is also the size of the elements used in the FEA). The reported strains are the three principal strains as well as the equivalent strain ($\phi_M$) and the strain reference length ($L_o$). This is done for three points - one on top of the future crack (point 1) and two points adjacent to the future crack (point 2 & 3). At point 2 & 3 the thickness reduction has also been measured using a ball-point micrometer. The thickness measurement procedure using a ball-point micrometer and all thickness measurements on the raking damage specimens are reported in appendix I.

### 5.1.1. Tensile Tests

Tensile tests were performed after the experiments (figure 5.5). The results are shown in figure 5.7 and an extensive report of the results and findings from the tensile testing can be found in appendix M and appendix N. The tensile tests have been performed according to the ISO standard for material testing [14] and were done with coupons cut from the same plate - in two perpendicular directions - as the specimens were made of. The testing procedure is the same testing procedure that was used to determine the material properties found on the material certificate (appendix L). The results of the tensile test will be used to construct true stress strain curve for the raking damage experiments. These true strains, especially the strain at failure, will provide the input for the prediction of a FFLC in section 5.1.2. The procedure for this FFLC prediction is described in section 2.4.

![Tensile test specimens after the test](image)

**Figure 5.5**: Photo of the tensile test specimens after the test. Fracture in the tensile tests is preceded by localisation (necking) which indicates semi-ductile fracture. Semi-ductile fracture is further supported by the 45 degree fracture planes that indicate brittle fracture after the ductile initiation of the fracture that can be seen in figure 5.6.

<table>
<thead>
<tr>
<th>Design Certificate Tensiletesting</th>
<th>Design</th>
<th>Certificate</th>
<th>Tensile testing</th>
</tr>
</thead>
<tbody>
<tr>
<td>$\sigma_{yield}$ [MPa]</td>
<td>235</td>
<td>298</td>
<td>303.4</td>
</tr>
<tr>
<td>$\sigma_{UTS}$ [MPa]</td>
<td>360</td>
<td>430</td>
<td>456</td>
</tr>
<tr>
<td>Elongation [-]</td>
<td>0.22</td>
<td>0.43</td>
<td>0.33</td>
</tr>
</tbody>
</table>

The comparison in table 5.3 shows the different material properties found over the course of this experimental campaign for the same material. The discrepancies seen in this comparison explain part of the difficulties one experiences when trying to model failure in maritime grounding (and crash).
Experimental Results & Discussion

(a) Close-up of the 45 degree fracture surface. The centre of the fracture surface shows brittle fracture, indicating that the specimen as a whole experienced semi-ductile fracture.

(b) Close-up of the cross section of the fracture surface of the tensile tests. Figure N.3 explains the locations where the thicknesses are measured.

Figure 5.6: Close-up pictures of the fracture surface of one of the tensile test specimens.

The engineering stress-strain curves for the tensile tests are shown in figure 5.7. The engineering strain (or the "elongation") from a standard tensile test is defined as:

$$\epsilon_{\text{eng}} = \frac{\Delta L}{L_0}$$

(5.1)

And the engineering stress is defined as:

$$\sigma_{\text{eng}} = \frac{F}{S_0}$$

(5.2)

Figure 5.7: Engineering stress-strain curves from tensile testing. These curves are the "raw" results from the testing machine.
In many applications not the engineering stress-strain curve of the material is needed, but the true stress-strain curve. For instance, the input for the FEA package LS-Dyna requires a true stress-strain curve. But also the single point calibration method used to acquire a failure locus in section 5.1.2 needs true strain values as input. In order to derive true stress-strain values from the tensile test data (figure 5.7), the following procedure is followed: The total volume remains constant, which requires that, for a tensile test, equation (5.3) applies:

\[ S_0 L_0 = SL \]  

(5.3)

From the constant volume requirement in equation (5.3) and the relation for the engineering strain the relations in equation (5.4) can readily be derived:

\[ \frac{S_0}{S} = \frac{L}{L_0} = \frac{L_0 + \Delta L}{L_0} = \frac{\Delta L}{L_0} + 1 = (\varepsilon_{\text{eng}} + 1) \]  

(5.4)

The definition of true strain requires that the strain increment is used, rather than the total elongation after the tensile test. This is illustrated in equation (5.5), where the true strain is eventually expressed in terms of the final reduction in cross-sectional area of the tensile test specimens.

\[ \varepsilon_{\text{true}} = \sum_{L_0}^{L} \frac{\Delta L}{L} \approx \int_{L_0}^{L} \frac{dL}{L} = \ln \left( \frac{L}{L_0} \right) = \ln (\varepsilon_{\text{eng}} + 1) = \ln \left( \frac{S_0}{S} \right) \]  

(5.5)

The definition of the true strain as \( \ln (\varepsilon_{\text{eng}} + 1) \) is only valid up till the point that localisation (necking) occurs. After this point, the engineering stresses decreases. This implies a decrease of the stresses while in reality strain hardening occurs, which means an increase of the stress. This means that the actual surface area \( (S \text{ or } S_u) \) needs to be taken into account, rather than the original surface area \( (S_0) \). The true stress is therefore defined as:

\[ \sigma_{\text{true}} = \frac{F}{S} \]  

(5.6)

From the tensile testing, unfortunately, the instantaneous cross-sectional area \( S \) is unknown. However, from the broken tensile test specimen, the ultimate cross-sectional area at failure \( (S_u) \) can be obtained. By careful measurements with a ball point micrometer, the cross-sectional area close to the crack can be measured. The exact procedure and the results for this ball point micrometer measurements can be found in appendix N. With \( S_u \) known, the true stress at failure can be calculated using equation (5.7):

\[ \sigma_{\text{true}} = \frac{\sigma_{\text{eng}} S_0}{S_u} \]  

(5.7)

With \( S_u \) known, also the true strain at failure can be computed by using the relations derived in equation (5.5). With the above described methodology, both true stress and true strain have been computed for the tensile tests. The results hereof are shown in table 5.4.

| \( \varepsilon_{\text{eng}} \) | \([-\] | Engineering strain |
| \( L \) | \( [mm] \) | Instantaneous gauge length |
| \( L_0 \) | \( [mm] \) | Original gauge length |
| \( \sigma_{\text{eng}} \) | \( [MPa] \) | Engineering stress |
| \( F \) | \( [N] \) | Force |
| \( S_0 \) | \( [mm^2] \) | Original cross sectional area of parallel length |
| \( S \) | \( [mm^2] \) | Instantaneous cross sectional area |
| \( \varepsilon_{\text{true}} \) | \([-\] | True strain |
| \( S_u \) | \( [mm^2] \) | Ultimate cross sectional area at failure |
| \( \sigma_{\text{true}} \) | \( [MPa] \) | True stress |
Table 5.4: Conversion to true stress-strain values at failure of the tensile test specimens.

<table>
<thead>
<tr>
<th>Elongation [-]</th>
<th>$S_0$ [mm$^2$]</th>
<th>$S_u$ [mm$^2$]</th>
<th>$\sigma_{true}$ [MPa]</th>
<th>$\epsilon_{true}$ [-]</th>
</tr>
</thead>
<tbody>
<tr>
<td>F20354-D1</td>
<td>0.33</td>
<td>122.0</td>
<td>48.4</td>
<td>826.0</td>
</tr>
<tr>
<td>F20354-D2</td>
<td>0.33</td>
<td>122.1</td>
<td>46.3</td>
<td>852.0</td>
</tr>
<tr>
<td>F20354-D3</td>
<td>0.33</td>
<td>122.4</td>
<td>47.9</td>
<td>823.0</td>
</tr>
<tr>
<td>F20354-L1</td>
<td>0.33</td>
<td>122.0</td>
<td>51.4</td>
<td>829.0</td>
</tr>
<tr>
<td>F20354-L2</td>
<td>0.32</td>
<td>122.9</td>
<td>51.9</td>
<td>818.0</td>
</tr>
<tr>
<td>F20354-L3</td>
<td>0.33</td>
<td>122.5</td>
<td>53.4</td>
<td>795.0</td>
</tr>
<tr>
<td>average</td>
<td>0.33</td>
<td>122.3</td>
<td>49.9</td>
<td>824.0</td>
</tr>
</tbody>
</table>

Using the values from table 5.4, a true stress-strain curve can be constructed. In the most simple and straightforward way, this is done by making a linear extrapolation from the point of localisation to the true stress-strain at failure. This can be seen in figure 5.8, in which the cross at the end of the true stress-strain curve is the value from table 5.4.

![Figure 5.8: Stress-strain curves from tensile testing. The linear extrapolation of the true stress-strain curve is done from the point localisation occurs, towards the average stress-strain at failure.](image)

5.1.2. Comparison with the FFLC and the Equivalent Failure Strain

A failure locus is a description of the onset of failure that may be applicable in a variety of stress/strain states. An effective failure criterion - or failure locus - for the onset of failure in plate and sheet metal is sought. This failure criterion should applicable for states of stress between uni-axial and equi-bi-axial. Using a FLC or a FFLC (see section 2.4) is considered to be such an effective and very practical failure criterion. In figure 5.9, two FFLC’s are plotted in a FLD. Both of these FFLC’s are based on the Modified Mohr-Coulomb (MMC) failure locus [7] and they are constructed using the single-point calibration method proposed by Voormeeren et al. [29]. The tensile test FFLC is calibrated using the average true strain at failure of the tensile tests (figure 5.8) and by assuming that $\frac{\sigma}{\epsilon} = -0.5$ holds true for a uni-axial tensile test (see figure 2.15). The experimental FFLC is calibrated using the average true strains at failure found from the DIC analysis of three of the four raking damage experiments. The blue line is the equivalent failure strain plotted in the FLD space. In figure 5.9 it is based on the average strains at failure from the drop tests (i.e. raking damage experiments).

DIC failure strain data for drop 1 are unusable and are therefore neither used for comparison and calculation, nor presented here.
Table 5.5: Strain values used for the single-point calibration of the FFLD presented in figure 5.9. The comparison shows the results of the calibrated FFLC for the same minor strain ($\varepsilon_2$).

<table>
<thead>
<tr>
<th></th>
<th>$\varepsilon_2$ [-]</th>
<th>$\varepsilon_1$ [-]</th>
</tr>
</thead>
<tbody>
<tr>
<td><strong>Calibration</strong></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Raking Damage experiments</td>
<td>0.06</td>
<td>0.42</td>
</tr>
<tr>
<td>Tensile Testing</td>
<td>-0.45</td>
<td>0.90</td>
</tr>
<tr>
<td><strong>Comparison</strong></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Raking Damage experiments</td>
<td>0.06</td>
<td>0.42</td>
</tr>
<tr>
<td>Tensile Testing</td>
<td>0.06</td>
<td>0.38</td>
</tr>
</tbody>
</table>

Figure 5.9: FLD comparison of the failure locus found from the experimental results for the strains, with several other failure loci. The values used for the single-point calibration are presented in table 5.5. All strain values in this FLD are true strain values.

A comparison of the different failure strains in the FLD (figure 5.9) shows that the single-point calibration method based on a uni-axial tensile test works well to predict the failure criterion found from the experimental data. The failure strain predicted by using the uni-axial results provides an accuracy of 0.04 of the actual failure strain, for similar minor strains (table 5.5).

This observation provides preliminary evidence that the FFLC calibrated using only one single strain-state - by using the method proposed by Voormeeren et al. [29] - provides an accurate failure criterion for the raking damage experiments.
5.2. Accelerations

The acceleration measurements are the core of these experiments. It was envisaged that rupture of hull plating could be detected by looking at acceleration measurements only. In order to do so, a decrease in accelerations should occur after rupture of the plate, and a more or less steady state crack growth phase would then follow.

In the following figures, the dashed vertical lines indicate key moments during the experiment. By using a synchronised trigger signal for both the acceleration measurements, as the high speed camera system (figure 5.3), these key moments could be marked in the acceleration measurements.

Figure 5.10: Acceleration time-trace of drop 4 of the experiments.

Figure 5.10 shows an acceleration measurement of one of the raking damage experiments. At 0.12 s the drop mass is released and enters into free fall. After 0.68 s the drop mass has reached the specimens, which can be seen by the slight vibration. Briefly thereafter, at 0.69 s, the drop mass makes contact with both plates. Now the phase of plastic deformation starts. The first dash dotted line - green - is the exact moment the indenter touches both plates, which is traced back using the high speed cameras. The second line - red - indicates the exact moment a crack appears on the outer surface. The third line is the moment the crack, which travels ahead of the indenter, reaches the edge of the plate. The last line indicates the moment the bottom of the plate is reached and the indenter hits the rubber brake-pads. This is the end of the raking damage experiment.

Some of the experiments have different drop heights and combinations of several drop tests. In order to be able to compare all experiments, the force-displacement graphs are an effective tool. The total displacements for each experiment are the same and so the force-displacements can readily be compared with one another. The drop mass is a known, constant value. With Newton’s second law (equation (5.8)) the forces can be computed from the acceleration measurements (figure 5.10):

\[ F = m \cdot \ddot{z} \]  

(5.8)

From kinematics, equation (5.9) and equation (5.10) enable (numerical) calculation of the velocity and displacements from the acceleration measurements (figure 5.10):

\[ v = \dot{z} = \int \ddot{z} \, dt \approx \frac{1}{2} \sum_{n=1}^{N} [\ddot{x}_n + \ddot{x}_{n+1}] \cdot \Delta t \]  

(5.9)

\[ z = \int \int \ddot{z} \, dt \, dt \approx \frac{1}{2} \sum_{n=1}^{N} [v_n + v_{n+1}] \cdot \Delta t \]  

(5.10)

The numerical part in equation (5.9) and equation (5.10) being done using trapezoidal summation as a means of numerically integrating the discrete measurement signal.
5.2. Accelerations

The force displacements of drop 4 in figure 5.11 show a similar picture as the raw acceleration measurements in figure 5.10. The displacements from touch to end of experiment are the same (0.51m) for all experiments except the first experiment (figure 5.14) which had an extra rubber brake pad.

The accelerations/forces show a gradual build up that starts a short moment after the indenter touches both plates. The moment a visible crack develops at the outer surface of the plate is back traced using the images of the high speed cameras (figure 5.3). This is indicated in the figure by one of the dash-dotted lines. Right after the moment a crack appears the accelerations of the drop mass decrease significantly. Also the acceleration signal becomes "messy" after rupture initiation.

Due to a misalignment of the indenter it hits one of the two plates first. This can be seen in the acceleration signal after the dash-dotted line that indicates plate rupture.

As was pointed out in section 5.1, the crack develops near the lower edge of the specimen. This causes the crack to advance ahead of the indenter to the edge of the plate. The envisaged stage of crack growth therefore does not occur.

A vibration in the drop mass is observed slightly before the mass is touching both plates, rather than only one of the two plates.

Drop 3 in figure 5.12 is a near duplicate of drop 4 (figure 5.11). Again a similar build-up of the force is observed. After rupture of the plating the force decreases significantly until the drop mass hits the brake pads at the bottom.
By using the fact that the total displacement from touch to end of each raking damage experiment is the same, the force displacement graphs can be combined. This can be done because the forces will basically "pick up" where the last drop stopped. In that way the consecutive drops of raking damage experiment 1 and 2 were combined in figure 5.13 and figure 5.14.

The reduction in the acceleration signal also occurs in absence of plate rupture. Drop 2-1 in figure 5.13 and drop 1-1, 1-2 and 1-3 in figure 5.14 were all drop tests without plate rupture. The difference with drop 2-2 and drop 1-4 is that the mass has come to a complete stop. This can be seen by the forces (accelerations) reducing to zero rather than a higher, more or less steady state, value, which is observed when plate rupture does occur.

One observation that makes detection of plate rupture more difficult when analysing the consecutive or combined drops such as experiment 1 & 2, is the fact that the indenter falls onto a pre-formed horizontal spot that is left behind by the drop tests that preceded the second, third or fourth drop test. This drop onto a horizontal spot causes a response that has strong, transient like, vibrations in it, which differs from what is seen in the first drop test. This vibrating response, is different from the gradual build up that occurs for an undeformed set of specimen.

In general, the transition from the gradual build-up of the force, to the decrease in the acceleration signal when a crack appears, are strong indications that the acceleration measurements can indeed be used to estimate the raking damage and especially, to determine whether a crack has developed.
5.3. Friction

The sensitivity analysis has shown that friction is an influential parameter in a raking damage scenario. When looking at friction, the interesting part is the energy that is dissipated in the form of friction. From the acceleration measurements only the total energy that has been dissipated can be derived through equation (5.11):

\[ E_{\text{total}} = E_{\text{potential}} + E_{\text{kinetic}} + E_{\text{dissipated}} \]  
\[ (5.11) \]

Rewriting the energy balance from equation (5.11) for the dissipated energy gives equation (5.12):

\[ E_{\text{dissipated}} = E_{\text{total}} - E_{\text{potential}} - E_{\text{kinetic}} \]  
\[ (5.12) \]

These separate parts of the energy balance can now be derived from the acceleration measurements and the mass of the drop weight.

\[ E_{\text{potential}} = m \cdot g \cdot z \]  
\[ (5.13) \]

\[ E_{\text{kinetic}} = 0.5 \cdot m \cdot v_z^2 \]  
\[ (5.14) \]

The total energy input \( E_{\text{total}} \) for one drop test is equal to the potential energy before the drop mass is released. This assumption, combined with equation (5.12), equation (5.13) and equation (5.14), leads to the graphs in figure 5.15.

![Energy as a function of time for drop 4 of the experiments.](image)

Figure 5.15: Energy as a function of time for drop 4 of the experiments.

Similarly as with comparison of the acceleration results, for sound comparison the energy-time graphs will be transformed to energy-displacement graphs. From the energies as a function of time in figure 5.15 the energies as a function of the displacement of the drop mass can be computed using equation (5.10), which is shown in figure 5.16. Using this principle, all energies as a function of the displacement have been determined (figure 5.17).
5.3.1. Friction coefficient tests

Based on the same materials that were used in the raking damage experiments, separate friction tests were performed to determine the friction coefficients of the two different friction scenarios.

Separate tests have been performed to determine the friction coefficient for the two different contact modes. From these tests the friction coefficients that have been taken are the average steady state values. The results of the friction coefficient tests are presented in figure 5.18 and the determined friction coefficients are presented in
5.3 Friction

The friction force \( F_{\text{friction}} = \mu F_{\text{normal}} \) is linearly dependent on the friction coefficient (when assuming static Coulomb friction) so the dissipated friction work \( W_{\text{friction}} = F_{\text{friction}} \Delta z \) - and thus energy - is also linearly dependent on \( \mu \). Because the total dissipation work is a sum of the dissipation through both friction and strain \( W_{\text{dissipated}} = W_{\text{friction}} + W_{\text{strain}} \), this leads to the linear extrapolation of the experimental results for the dissipated energy as a function of the friction coefficient in Figure 15. When combining the energy results with the results from the separate friction coefficient tests, the significance of friction in a raking damage scenario becomes even more obvious. In table 5.7 it can be seen that the experiments without grease have an average energy dissipation due to friction of 34.8% of the total energy dissipation compared to 5.7% with grease applied to the surface (reduced friction). The question is however, whether the assumption of static coulomb friction holds when addressing a rather complex combination of friction and plastic deformation simultaneously.

### 5.3.2. Energy Dissipation through Friction

When static Coulomb friction is assumed the following relations hold true:

\[
W_{\text{friction}} = F_{\text{friction}} \cdot \Delta z
\]  

\[
F_{\text{friction}} = \mu \cdot F_{\text{normal}}
\]  

Based on these relations and under the assumption that the \( \Delta Z \) is equal for all experiments, the friction coefficient can be used to make a linear extrapolation of the dissipated energy based on the friction coefficient only. Such an extrapolation is shown in figure 5.19.
Figure 5.19: Extrapolation of the energy dissipation as a function of the friction coefficient.

Based on the linear extrapolation shown in figure 5.19, the energy dissipation due to plastic deformation only, can be deduced. This is the energy dissipation at zero friction, which is 70.0 kJ.

Table 5.7: Dissipated energy in the experiments. Percentage of the dissipated energy due to friction based on the linear extrapolation of the friction coefficient (figure 5.19).

<table>
<thead>
<tr>
<th>Drop</th>
<th>$E_{dissipated}$ [kJ]</th>
<th>$E_{friction}$ [%]</th>
<th>$\mu$ [-]</th>
</tr>
</thead>
<tbody>
<tr>
<td>Drop 1</td>
<td>107.7</td>
<td>35.0 %</td>
<td>0.28</td>
</tr>
<tr>
<td>Drop 2</td>
<td>107.0</td>
<td>34.6 %</td>
<td>0.28</td>
</tr>
<tr>
<td>Drop 3</td>
<td>73.2</td>
<td>4.4 %</td>
<td>0.03</td>
</tr>
<tr>
<td>Drop 4</td>
<td>75.2</td>
<td>6.9 %</td>
<td>0.03</td>
</tr>
</tbody>
</table>

The obtained energies seem to correlate with each other very well for the separate cases. Despite the limited number of repetitions experiments seem to reproduce and the results do look very encouraging.

**Strain Measurements** The energy dissipation through friction can also be calculated by using the strain measurement data from strain measurements on the indenter. This can be done by determining the contact area and the corresponding contact pressure. Integrate the contact pressure over the contact area and multiply with the friction coefficient and one obtains the friction force. Multiplying again with the displacement yields the energy dissipation (from equation (2.2): $E_{friction} = \int p \cdot \mu \cdot z_{rel} dS$). Simonsen (1997)[26] used a similar approach in his analytical analysis to determine energy dissipation through friction.

For these experiments however, it proved to be impossible to retrieve the exact contact pressures and contact area. Instead, the axial forces on the indenter were measured using strain gauges (figure 5.20 and figure 5.21). This enables calculation of the resultant contact forces (figure 5.22) and the respective angle with the indenter’s centre-line of these forces.
5.3. Friction

Figure 5.20: Strain measurements of drop 3. The location of the strain gauges on the indenter are illustrated in figure 5.21(a) and the picture in figure 4.3.

(a) Location of the strain gauges on the indenter.

(b) Combination of bending strains and axial strains. Under the assumption that the bending strains have a symmetric distribution, the average stress/strain is the same as the axial stress/strain.

Figure 5.21: Strain measurements and determination of the axial strains on the indenter.

From these strain measurements in figure 5.20, the axial forces can be computed. The indenter is subjected to a combination of bending and axial stresses, which result in corresponding strains through Hooke’s law (equation (5.17)):

$$\sigma = E \cdot \epsilon$$

(5.17)

By using the principle explained in figure 5.21(b), the axial strains can be determined and by equation (5.18) the axial forces on the indenter can be calculated:

$$F_{axial} = E \cdot A \cdot \epsilon_{avg(1,2,3,4)}$$

(5.18)

The resultant force on the indenter then follows from equation (5.19):

$$F_{resultant} = \sqrt{(F_{axial})^2 + (F_{vertical})^2}$$

(5.19)
5. Experimental Results & Discussion

Figure 5.22: Axial, Vertical and resultant forces on the indenter from drop 3. Axial force is determined using strain gauges. The vertical force comes from the accelerometers.

Figure 5.23: The forces that are involved in determining $F_{\text{normal}}$ and $F_{\text{friction}}$. $F_{\text{axial}}$ and $F_{\text{vertical}}$ are measured, and from these two force $F_{\text{resultant}}$ is calculated. Using the fixed angle for contact from figure 5.24, a decomposition can be made to determine $F_{\text{normal}}$ and $F_{\text{friction}}$.

Figure 5.24: Determination of the contact area and the angle of the normal force.

(a) Determination of the contact area on the indenter by observing the scratch marks left behind after the raking damage experiments.

(b) Determination of the angle of the normal force.
By looking at the contact marks on the indenter, the area and the angle of the normal force of contact could be estimated (figure 5.24). This angle was taken as a fixed angle throughout all the drop tests. The following procedure (which is also illustrated in figure 5.23) can be used to determine the normal force of contact is described below.

The angle of the resultant force is calculated through equation (5.20):

\[ \angle F_{\text{resultant}} = \arctan \left( \frac{F_{\text{vertical}}}{F_{\text{axial}}} \right) \]  

(5.20)

The angle of the contact force and the resultant force are subsequently used to determine the magnitude of the normal force of contact (equation (5.21)). The normal force is needed for determination of the friction force \( F_{\text{friction}} = \mu \cdot F_{\text{normal}} \).

\[ F_{\text{normal}} = F_{\text{resultant}} \cdot \cos \left( \angle F_{\text{friction}} - \angle F_{\text{resultant}} \right) \]  

(5.21)

Hereafter the friction coefficients – found from the friction coefficient tests – are applied and the energy dissipation through friction is calculated with

\[ W_{\text{friction}} = F_{\text{friction}} \cdot \left( \frac{\Delta Z}{\cos(\angle F_{\text{friction}})} \right) \]  

(5.22)

The results for energy dissipation for a single raking damage experiment are illustrated by the results for experiment 1 in figure 5.25. Again, the same principle of adding up consecutive drop tests is used for the experiments that consist of multiple drops. The results that are presented in table 5.8 for the fixed angle method are all determined in this manner.

![Figure 5.25: The total energy dissipation and the energy dissipation through friction in experiment 1, calculated through the method of a fixed angle of contact.](image)

The results of the energy dissipations through friction calculated via the two different methods are shown in table 5.8. It can be seen that for the steel-steel case the friction energies are within 10% of the total energy dissipation. For the lubricated cases, the results are even within 1% of the total energy dissipation.

<table>
<thead>
<tr>
<th></th>
<th>Linear extrapolation</th>
<th>Fixed angle of ( F_{\text{normal}} )</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>( E_{\text{friction}} [\text{kJ}] )</td>
<td>% friction</td>
</tr>
<tr>
<td>Experiment 1</td>
<td>37.74</td>
<td>35.0%</td>
</tr>
<tr>
<td>Experiment 2</td>
<td>37.01</td>
<td>34.6%</td>
</tr>
<tr>
<td>Experiment 3</td>
<td>3.24</td>
<td>4.4%</td>
</tr>
<tr>
<td>Experiment 4</td>
<td>5.23</td>
<td>6.9%</td>
</tr>
</tbody>
</table>
Even though both methods of determining the energy dissipation can be considered as somewhat crude and they both carry some far-reaching assumptions, the similarity between the results is striking.

Based on the results presented in table 5.8, the static Coulomb friction model leads to corresponding results when determining the energy dissipation in two different ways. Therefore the static Coulomb friction model is deemed to be a proper model to use in a raking damage scenario.

5.3.3. Infrared & DIC measurements

The literature review showed the difficulties with modelling the friction in these raking damage scenarios. Measuring the actual energy dissipation due to friction can take away any uncertainty regarding the static Coulomb friction model. It was attempted to measure the actual friction energy dissipation in this study, by applying a combination of Infra-Red (IR) cameras and the DIC measurements. In this section the principle of combining the IR and DIC measurements is explained and the results derived through this method are presented. For an overview of all the results of the method presented in this section, please refer to appendix K.

The rate of energy dissipation in a ship grounding situation is calculated by equation (5.23), which was introduced in section 2.2. It is built up out of the energy dissipation due to plasticity, crack propagation and friction. Because the energy due to crack propagation is several orders of magnitude smaller than the other two energy dissipations, it can be neglected in the analysis.

\[
F_{il}V = \dot{E}_p + \dot{E}_c + \dot{E}_f = F_p V + \int_{S_c} p \mu V \, dS_c \tag{5.23}
\]

The aim of this part of the raking damage research is to quantify the amount of energy dissipation through friction in an independent way. This is done through measurements with the IR camera, which are combined with the DIC measurements. The IR camera provides an optical temperature measurement of 320 x 240 pixels, with each pixel being an independent temperature measurement. The concept is that the IR camera captures the total amount of energy dissipation, as all friction energy is dissipated in the form of heat. The DIC measurements capture the strains, from which the energy dissipation due to plastic deformation can be calculated. Now the energy dissipation due to plastic deformation is known, and from the IR measurements the total energy dissipation is known.

\[
E_{\text{diss, total}} = E_{\text{diss, plastic}} + E_{\text{diss, friction}} \tag{5.24}
\]

From equation (5.23) the total energy dissipation can be written as equation (5.24). In order to quantify the actual dissipation of energy, equation (5.24) is supported by the following assumptions:

- All friction energy is dissipated in the form of heat
- All plastic deformation energy is dissipated in the form of heat [21]
- The specimen absorbs all dissipated energy in the form of heat
- 100% of the heat is radiated and measured by the IR camera

\[
E_{\text{diss, friction}} = E_{\text{diss, total}} - E_{\text{diss, plastic}} = E_{IR} - E_{DIC} \tag{5.25}
\]
5.3. Friction

With these assumptions the energy dissipation can be calculated from the IR and DIC measurements in figure 5.26. This energy dissipation is calculated with equation (5.25). The separate contributions of equation (5.25) are calculated with equation (5.26) and equation (5.27).

\[ E_{DIC} = \epsilon_{VM} \cdot V \cdot \sigma_{yield} \] (5.26)

\[ E_{IR} = m \cdot \Delta T \cdot c_{thermal} \] (5.27)

Equation (5.26) and equation (5.27) are calculated using constant values for the yield stress \(\sigma_{yield}\) and the specific heat \(c_{thermal}\) shown in table 5.9.

Table 5.9: Constants used for calculation of the energy for the IR minus DIC approach in equation (5.26) and equation (5.27).

<table>
<thead>
<tr>
<th>(\sigma_{yield})</th>
<th>303 MPa (from tensile testing, section 5.1.1 &amp; appendix M)</th>
</tr>
</thead>
<tbody>
<tr>
<td>(c_{thermal})</td>
<td>434 (\text{J/kg}^°\text{C}) ([17] table A.1 - AISI 1010 steel)</td>
</tr>
</tbody>
</table>

Figure 5.27: The principle of calculating the energy contribution due to friction. The total energy dissipation is calculated using the IR images by equation (5.27). The energy dissipation due to the plastic deformation is calculated with the DIC strains (appendix H) by using equation (5.26).

In order to be able to subtract the plastic energy from the total energy, both images need to be aligned so that the same area on the plate is used. In order to make this alignment, the x and y
locations on the plate are determined using markers that can be seen on both the high speed images (DIC) and the IR images. The principle of these markers is illustrated in figure 5.28.

By using the markers shown in figure 5.28 an aligned overlay of the two images (IR and DIC) can be made. This overlay is made with the two energy images shown in figure 5.27. An example of the results of this overlay is shown in figure 5.29 and figure 5.30.
The nice pictures in figure 5.29 and figure 5.30 show the energy density of for one of the drop tests. Eventually the energy dissipation of the entire plate is to be determined, because this allows for comparison of the different contributions to the total energy dissipation. In order to determine the total energy dissipation the energy density is summed over the “measurement volume”. The results of this summation are presented in table 5.10.

Table 5.10: IR minus DIC energy results.

<table>
<thead>
<tr>
<th>IR energy (kJ)</th>
<th>DIC energy (kJ)</th>
<th>Friction energy (kJ)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Drop 1-1</td>
<td>13</td>
<td>7</td>
</tr>
<tr>
<td>Drop 2-1</td>
<td>19</td>
<td>12</td>
</tr>
<tr>
<td>Drop 3</td>
<td>27</td>
<td>18</td>
</tr>
<tr>
<td>Drop 4</td>
<td>25</td>
<td>18</td>
</tr>
</tbody>
</table>

The values of energy dissipation from table 5.10 are not to be compared directly to the values determined in section 5.3.2 due to the following reasons:

- The measurement volume does not span the entire plate, only the region of interest.
- The IR camera only measured one of the two plates in each experiment.
- For experiment 1 and experiment 2 only the first drop test was used.

Table 5.11: Comparison of dissipated energy through friction.

<table>
<thead>
<tr>
<th>IR minus DIC</th>
<th>Energy results from section 5.3</th>
</tr>
</thead>
<tbody>
<tr>
<td>$E_{IR}$</td>
<td>$E_{diss,total}$</td>
</tr>
<tr>
<td>% friction</td>
<td>% friction, linear</td>
</tr>
<tr>
<td>--------------</td>
<td>-------------------</td>
</tr>
<tr>
<td>Drop 1-1</td>
<td>13 kJ 43%</td>
</tr>
<tr>
<td>Drop 2-1</td>
<td>19 kJ 37%</td>
</tr>
<tr>
<td>Drop 3</td>
<td>27 kJ 33%</td>
</tr>
<tr>
<td>Drop 4</td>
<td>25 kJ 29%</td>
</tr>
</tbody>
</table>

The energy results of the IR minus DIC method are compared with the energy results obtained through the acceleration & strain measurements in table 5.11. On the left side of table 5.11 the total energy dissipation is presented together with the percentage of that total energy that is dissipated through friction. On the right hand side of table 5.11 the total energy dissipation that is calculated using the acceleration measurements is presented in the first column. These totals are corrected so that they represent the energy dissipation which is to be measured in only one plate for that specific drop test. The second and third columns on the right side show the percentage of friction energy dissipation that is calculated through the linear extrapolation and the fixed angle of contact.

Comparing the total energy dissipation for both methods, the order of magnitude looks to be similar. This is an indication that the IR images give comparable results with the acceleration and
strain measurements. When looking at the percentage of the total that is dissipated in friction, large discrepancies can be observed from table 5.11. Especially the percentage of friction energy for drop 3 and drop 4, the reduced friction cases, seem to differ significantly from the formerly determined values on the right hand side of table 5.11. The percentage calculated through IR minus DIC remains more or less constant, independent of changing the friction. From figure 5.19 and table 5.7 a clear difference in the total energy dissipation is observed between the two friction cases, as well as a significant difference in the percentage of energy dissipation due to friction. These large discrepancies make that the results obtained through IR minus DIC are not accurate enough to be able to draw conclusions regarding the Coulomb Friction model.

5.4. Discussion

In section 5.1, section 5.2 and section 5.3, the results for the raking damage experiments have been presented for the failure, acceleration and friction parts of this research. In this section these results will be discussed. The discussion section will conclude with a check of the hypotheses of this thesis work.

5.4.1. Failure

- Plate rupture was introduced gradually, as was hypothesised. Plate rupture occurred near the bottom of the plate. The close proximity to the bottom of the specimen, means that the envisaged phase of steady state crack propagation did not occur. Neither did the indenter come to a standstill without reaching the bottom of the specimens. Due to the short distance of crack propagation, no conclusions can be drawn regarding the forces that occur during the crack propagation.

- Table 5.12 shows a comparison of the various material properties that were found over the course of the raking damage experiments. The uncertainty regarding the failure strain especially, is problematic. The failure criterion that is used for numerically analysing the raking damage experiments in the design phase, has a large influence on the FEA results. This was also shown by the sensitivity analysis in section 3.3. The failure criterion can, in practical sense, only be based upon the results of known material properties. The high variance (or uncertainty) regarding these material properties, and especially the failure strain, is therefore problematic. In other words, a reliable and accurate failure strain that can be used as a basis for a failure criterion is required in order to be able to model these raking damage scenarios.

| Table 5.12: Comparison of material properties for the raking damage experiments. (Table is the same as table 5.3) |
|---|---|---|---|
| Design | Certificate | Tensile testing |
| $\sigma_{yield}$ [MPa] | 235 | 298 | 303.4 |
| $\sigma_{UTS}$ [MPa] | 360 | 430 | 456 |
| Elongation [-] | 0.22 | 0.43 | 0.33 |

- The strains that were found by using the DIC measurement technique were checked by performing ball-point micrometer measurements (see table 5.13). No abnormalities were found during this check, and therefore the DIC measurements were deemed reasonably accurate. However, the DIC measurements are only as accurate as the random pattern that is applied on the specimens (i.e. the spacing between the reference points that the DIC software can use to compute displacements and strains). The pattern applied on the specimen for the raking damage experiments was such that the minimum reference length that could be achieved for some specimens, was only 20mm in the region of interest. Fortunately, no localisation occurred in the process of failure. This means that a uniform strain state led up to failure and that the reference length is not relevant.
Table 5.13: Engineering Strains at failure from DIC and ball-micrometer thickness measurements of the cracked specimens. A complete overview of the strains at failure can be found in appendix H. All thickness measurements before and after are reported in appendix I. (Table is the same as table 5.2)

<table>
<thead>
<tr>
<th></th>
<th>$\epsilon_1$</th>
<th>$\epsilon_2$</th>
<th>$\epsilon_3$</th>
<th>$\epsilon_3$ pt.2/3</th>
<th>measured $\epsilon_3$</th>
</tr>
</thead>
<tbody>
<tr>
<td>Experiment 1</td>
<td>*</td>
<td>*</td>
<td>*</td>
<td>*</td>
<td>-0.38</td>
</tr>
<tr>
<td>Experiment 2</td>
<td>0.61</td>
<td>0.07</td>
<td>-0.42</td>
<td>-0.37</td>
<td>-0.38</td>
</tr>
<tr>
<td>Experiment 3</td>
<td>0.48</td>
<td>0.04</td>
<td>-0.35</td>
<td>-0.32</td>
<td>-0.30</td>
</tr>
<tr>
<td>Experiment 4</td>
<td>0.48</td>
<td>0.06</td>
<td>-0.36</td>
<td>-0.28</td>
<td>-0.33</td>
</tr>
</tbody>
</table>

• Only two strain states are used in the approach with the single point calibration method for the FFLC. The strain state of the tensile test (red + in figure 5.31) and the average strain state of the raking damage experiments (green $\odot$ in figure 5.31). More strain states than the constraint plane strain (raking damage experiment) and the uni-axial strain (tensile test) may be needed in order to draw definitive conclusions regarding the applicability of this approach. Moreover, the assumption that $\frac{\epsilon_w}{\epsilon_t} = -0.5$ for a tensile test with a flat specimen, is not entirely correct. The dimensions of the fracture surface of these tensile tests have been measured and reported in appendix N. In table N.3 and table N.4 of that appendix, the strains in length, width and thickness direction of the tensile specimen have been calculated. Based upon the ball-point micrometer measurements and calculated strains reported in appendix N the assumption of $\frac{\epsilon_w}{\epsilon_t} = -0.5$, is close to reality though.

![FFLD comparison of the failure locus from the design analysis and the failure locus from the experiments.](image)

• In the design phase of the experiments the GL-criterion was regarded as similar as the equivalent plastic failure strain. It was wrongly understood that the GL criterion and the equivalent failure
were the same failure criterion. The GL criterion is actually based upon a maximum allowable thinning strain (−𝜖_{th}). Figure 5.31 provides a FFLC of both failure criteria in the FLD space for the design values of the failure strain. The comparison in figure 5.31 shows that the equivalent failure strain was a conservative design value, not only compared to the experimental results, but also compared to the (intended) GL criterion. One could argue that in the light of the comparison shown in figure 5.31, the equivalent plastic strain to failure used in the design phase it is too conservative.

• Figure 5.31 also showed that usage of the equivalent failure strain criterion is limited to states of strain for which it is specifically tuned. In practice this means that if the equivalent failure strain criterion is calibrated based on plane strain testing, then the failure strains near the uni-axial condition are very conservative. Similarly, if the true failure strain of the tensile tests were to be used (not plotted in figure 5.9), the predicted equivalent failure strain would be very un-conservative, which is undesirable.

• The last point of discussion on the failure criterion applicable on a raking damage scenario also deals conservatism. The concept of a conservative failure criterion does not hold in the light of the application of this research. Imagine a conservative failure criterion is taken in the application for a rupture detection algorithm that can be used on a real vessel. This would mean a lower strain value is used than what is actually expected, in order to be conservative. For instance the design value for “Failure Strain 𝜖_{ef} = 0.218” in figure 5.31 would be such a conservative failure criterion.

Now this vessel runs aground and the rupture detection algorithm estimates that the hull is breached. The estimate of the breached hull prompts the captain to abandon ship with his passengers and crew, in order to bring everyone to safety. It may very well be the case that the hull was not breached in the accident, because a conservative failure criterion was used by the rupture detection algorithm. So the conservative failure criterion caused a dangerous abandon ship situation, while in reality everyone would have been safe on board the vessel.

This imaginary situation illustrates the difficulty with a failure criterion. In the light of the raking damage experiments, there is no such thing as a conservative failure criterion. This observation makes the variance in failure strains, reported in table 5.12, even more problematic. If the concept of rupture detection through acceleration measurements would ever be applied on real vessels, the issues regarding the "conservative" engineering approach in failure limits need to be addressed.

5.4.2. Accelerations

• The results look promising. A decrease in the acceleration signal after the gradual build up indicates plate rupture. However, the number of tests is limited. Especially when taking the different scenarios into account (friction).

• Detection of plate rupture is made more difficult for the consecutive or combined drops such as experiment 1 & 2. The indenter falls onto a pre-formed flat spot that is left behind by the drop tests that preceded the second, third of fourth drop test. This drop onto a flat spot causes a more transient response than what is seen in the first drop test. These vibrations make that the transition at plate rupture is less obvious for drop 1 and drop 2. However, the decrease after plate rupture is still present and possible to detect. The measurements show a decrease compared to the maximum force before rupture which is very encouraging.

• Section (section 5.2) dealt with presentation of the acceleration measurement results of the raking damage experiments. However, after these results the main question of this research remains open. How to detect rupture from these measurements? A concept that works for drop 3 and drop 4 of the raking damage experiments is filtering the experimental data based on the displacements and then using the gradient of the filtered signal to detect rupture. Figure 5.32 illustrates the results of filtering on the basis of displacements for drop 4.

So how does this displacement filtering work? The acceleration measurement data have a constant sampling frequency, meaning that all data points have an equal Δ𝑡 between them. The
5.4. Discussion

Force-Displacement graph is now reconstructed so that there is equal displacement, $\Delta z$, between the data points. This ‘reconstruction’ is done with linear interpolation. The displacement filtering is done on the basis of the “typical crack displacement”. This typical crack displacement is the displacement of the indenter from rupture until the end (so the displacement between the red and the blue dash-dotted lines). For drop 4 this typical crack displacement is 0.073 m. The filter is a moving average filter that filters exactly as many data points in order to represent the typical crack displacement.

Figure 5.32: The filtered force displacement and the gradient of the filtered force displacement of drop 4. The results of this gradient filtering approach are presented in appendix F for all raking damage experiments.

The next step is to analyse the gradient of these filtered data. In section 5.2 the acceleration results showed a decrease after rupture initiation. If a decrease occurs for a displacement larger that the typical crack displacement, the plate is assumed to be ruptured. In other words, if the gradient of the filtered signal is smaller than zero, the plate has ruptured.

By using the concept of displacement filtering the damage length - crack length - can be determined from figure 5.32. The principle of integrating the time signal to obtain a displacement has already been applied in this figure. According to the gradient of the filtered signal, the plate ruptured at a displacement of 0.41 m, and the crack propagation phase ended at 0.51 m. This means that a crack of 0.1 m is the estimated damage (crack length) from these acceleration data, which is a good estimate compared to the actual 0.073 m of crack length.

The concept to detect rupture through filtering the acceleration signal and determining the gradient of the filtered signal, presented in figure 5.32, does have several drawbacks. The first problem with this approach appears at a displacement of 0.19 m in figure 5.32. At this displacement the gradient is also smaller than zero. If the aforementioned reasoning is followed, rupture would be detected from 0.19 m onwards for drop 4 of the raking damage experiments. Clearly, this is not the case.

A solution would be, to increase the count (or typical crack displacement value) of the moving average filter, which would solve the issue in this case. However, in this case the exact moment of rupture is known, which would allow tuning of the filter. In a more realistic case, without a priori knowledge, such tuning would not be possible.

In figure 5.32, drop 3 has been used to apply the proposed filter/gradient detection method. If one would look at drop 1 in figure 5.14, or drop 2 in figure 5.13, the filter/gradient approach would not work. Drop 2, for instance, has an energy surplus at the moment of plate rupture, which reduces the detectability of plate rupture. This means that another algorithm needs to be devised that can deal with these problems.

- In general it can be concluded that the concept of rupture detection through acceleration measurements does look promising. The exact algorithm that will detect the rupture in such a scenario still needs to be devised, but the fact that such a clear decrease in the accelerations
is observed directly after plate rupture is positive. Moreover, the accelerations decrease to zero, when plate rupture does not occur.

5.4.3. Friction

• The two different friction scenarios provided a significant difference in the total energy dissipation. This supports the part of the hypothesis and the conclusion of the sensitivity analysis that friction has a large influence on the accelerations/forces in a raking damage scenario. However, altering the friction did not seem to influence the location or manner of crack initiation.

• The two ways in which the energy dissipation through friction has been derived in section 5.3.2 can both be considered as somewhat simplistic. The linear extrapolation of the friction coefficient is mathematically valid based on the assumption of the Coulomb friction model, but a third set of measurements with different μ is needed to validate the use of a linear relation between energy dissipation and the friction coefficient. The approach with the fixed angle of the normal force has many assumptions that are all based on observations, but might not be entirely valid: First of all, the angle of the force of contact varies throughout the drop test. For this approach it is taken as a fixed angle. Also, the middle of the cross section of the area of contact (see figure 5.24) is taken to determine the angle of this contact force. More accurate would be to determine the centroid of this area.

• The separate friction coefficient tests showed that μ = 0.3 seems reasonable as a friction coefficient for steel-steel contact in the raking damage experiments. However, the friction measurements were not performed on exactly the same materials. In order to be more accurate one would have to perform the friction coefficient test by using the exact same materials with the same surface preparation. Then again, in a realistic raking damage scenario, the exact same friction coefficient data would not be available either.

• The main assumption in this friction study, is that the Coulomb friction model is used. The question however is, whether this Coulomb friction is the proper model to use in such a scenario. The energy dissipations have been calculated using the Coulomb friction in two different ways. The fact that the results of these two different calculation methods, using the same Coulomb friction model, can not be considered strong evidence. Even more so, it has elements of circular reasoning.

• Because of the circular reasoning that leads to the partial conclusion about the Coulomb friction model, it was attempted to perform separate measurements of the energy dissipation through friction in order to confirm the validity of the Coulomb friction model. This was done by the IR minus DIC concept presented in section 5.3.3. It proved difficult to combine these two different measurement techniques with sufficient accuracy. Therefore, no conclusions could be drawn based upon these measurements. Nevertheless, the results of this approach (and nice looking graphics) are presented in section 5.3.3 and appendix K. More testing and accurate consideration of the assumptions are needed in order for this sophisticated measurement technique to contribute in solving the friction puzzle.

• In this study only the Coulomb friction model is studied. In order to solve this friction puzzle, other models for determining energy dissipation through friction might be studied or considered. However, the power of Coulomb friction is in the limited number of variables that need to be determined. In a situation with many unknowns, such as ship grounding, the least amount of input variables yields the preferred model for analysis.
5.4.4. Hypothesis Check

To conclude this discussion on the research results, the posed hypotheses will be discussed in the light of the results of this research. In section 1.4 the following set of hypotheses is presented:

The moment of rupture is distinguished by an abrupt decrease in the acceleration measurements, due to the loss of structural resistance after plate rupture.

1. The plate is first plastically deformed (a combination of membrane and bending) until the failure strain is reached. The structural resistance of the plate then decreases significantly. Now mostly bending and crack propagation dissipate energy. A steady state crack propagation phase then follows, and the indenter comes to a standstill in the plating.

2. A practical way to determine a failure criterion is sought. In that sense, a Fracture Forming Limit Curve (FFLC) based on the measured elongation at a standardized tensile test method should suffice as a failure criterion. The proposed method by Voormeeren et al. [29] is thought to give an accurate FFLC that can be used in conjunction with the raking damage experiments.

3. Based on observations from the Muscat-Fenech experiments [19], a significant decrease after rupture in the vertical forces (and thus accelerations) is to be expected. However, due to a higher bending stiffness of the thick plates - rather than the thin sheet metal - the decrease will be less prominent than what is seen in the ‘glancing collision’ experiments.

4. Friction will affect both energy dissipation due to contact and the fracture at ultimate loading. Both of these parameters have a large influence on the behaviour regarding failure. The energy dissipation due to friction can be accurately predicted using a model of static Coulomb friction, provided that the coefficient of friction is determined separately.

These hypothesis are verified according to the results presented in chapter 5 and specifically, the discussion in this section. A summary of the presented hypothesis check is presented in table 5.14. The verification is also supported graphically by using plus and minus signs. A + sign for a verified hypothesis, +/- for a partially verified hypothesis, a - for a discarded and ? for unchecked hypothesis:

- There is an abrupt decrease of the acceleration measurements after initiation of plate rupture. +
- Using the presented displacement filter, it is possible to detect plate rupture. +
- The energy surplus seen in drop 2, however, reduces the detectability of plate rupture. +/-

1. Plastic deformation is gradually introduced by the sphere shaped indenter attached to the drop mass. Plate rupture initiates close to the lower edge of the specimen, rather than in the centre. Because of the close proximity to the edge, the phase of steady state crack propagation did not occur. +

2. An FFLC was calibrated with Voormeeren’s method [29], which uses only a single strain state for calibration. An FFLC was made for both the raking damage experiments as well as a standard uni-axial tensile test. The FFLC calibrated with the tensile test provided a very accurate prediction of the strains at failure of the raking damage experiments. Taking into account the accuracy of the prediction, the FFLC calibrated with the uni-axial tensile test can readily be used as a failure criterion to simulate the raking damage experiments. +
3. A significant decrease was observed directly after plate rupture. The decrease seems to be very prominent still. However, the decrease is more difficult to detect for the raking damage experiments that consisted of consecutive drop tests, such as drop 2. Because the phase of steady state crack propagation did not occur, it is not possible to draw definitive conclusions about the ratio between the intact and the cracked phase.

4. The difference in total energy dissipation between the two friction cases shows that the friction has a large contribution in the energy dissipation in a raking damage scenario. The large contribution is also confirmed by the FEM sensitivity analysis. The contribution of friction to the total energy dissipation has been calculated through two different ways. Both these methods used the Coulomb friction as a basis. The results between these two calculation of the energy dissipation through friction yielded comparable results. However, both these results are calculated using the same Coulomb model. Independent measurements are needed in order to truly verify validity of the Coulomb model for a raking damage scenario. Independent measurements, using a combination of IR and DIC measurements, did not meet the required accuracy. The measurement method does look promising, but it needs further development in order to be accurate enough to truly verify the Coulomb friction model.

Table 5.14: Summary of the hypothesis check presented in section 5.4.4

<table>
<thead>
<tr>
<th>Rupture detection through acceleration measurements.</th>
<th>+</th>
</tr>
</thead>
<tbody>
<tr>
<td><strong>What happens?</strong> Plastic deformation is gradually introduced. Plate rupture initiates near the bottom of the specimen. A phase of steady state crack propagation did not occur.</td>
<td>+/-</td>
</tr>
<tr>
<td><strong>Failure</strong> An accurate FFLC, that is based on a standardised tensile test, proved to be accurate for the raking damage experiments. This can readily be used as a failure criterion for FEA. Voormeeren's method is used to construct the FFLC.</td>
<td>+</td>
</tr>
<tr>
<td><strong>Accelerations</strong> A significant decrease is observed in the accelerations directly after plate rupture. This decrease is an indication that it is possible to detect plate rupture from acceleration measurements.</td>
<td>+</td>
</tr>
<tr>
<td><strong>Friction</strong> Coulomb friction model gives accurate prediction of energy dissipation due to friction. Independent measurements &amp; calculations did not reach the necessary accuracy for verification of the Coulomb friction model.</td>
<td>+/-</td>
</tr>
</tbody>
</table>
Conclusions

Instantaneous insight in the structural damage and especially, information on whether or not the hull is breached, may be of great aid in the direct aftermath of a ship grounding accident. The new concept presented in this thesis is to acquire such instantaneous insight by measuring accelerations during the grounding accident. In order to distinguish hull plate rupture from acceleration measurements, the contributions of plastic deformation, friction and crack propagation to such an acceleration signal have been studied with the raking damage experiments.

Finite Element Analysis (FEA) was performed in the design stage of the experiments. A sensitivity analysis with this FEA concluded that the raking damage experiments are particularly sensitive to two parameters: The applied failure criterion and the coefficient of friction. These two parameters were also studied with the raking damage experiments.

The conclusions are related to the research questions that were presented in section 1.3:

1. How can rupture of a 6 mm ordinary steel plate, in a raking damage scenario with a hard spherical object, be distinguished from acceleration data?
   1. Which phenomena occur in such a raking damage scenario? And what is the influence of each of these phenomena?
   2. How is failure of the plate initiated? And what is a practical but accurate way to predict plate failure?
   3. What is the ratio, in terms of energy dissipation, in which these phenomena occur?
   4. What is the role of friction in a raking damage scenario?

In the discussion in section 5.4, the experimental results are checked against the hypotheses that are formulated as a preliminary answer to the research questions. A summary of this hypothesis check is presented in table 5.14 on page 74. The hypotheses check leads to the following conclusions:

The clear transition at plate rupture that is observed in the measurement data directly after plate rupture, is a strong indication that detection of plate rupture from acceleration measurements is possible. Therefore, it can be concluded that using real time acceleration measurements is promising as a method to estimate the structural damage of a vessel in the moments following a grounding accident.

1. The plate is plastically deformed until the failure strain is reached and the plate ruptures. Plate rupture occurred close to the lower edge of the plate, rather than in the centre, which was hypothesised and designed accordingly. Therefore, the phase of steady state crack propagation did not occur, and the indenter did not come to a standstill before it reached the end of the plate.
2. Failure in the racking damage experiments is initiated by plastic straining of the plate. Strains are observed in close proximity of the indenter mainly. Initiation of rupture occurred in the experiments
without clear evidence of localisation (necking). The strains directly before rupture have been captured using high speed DIC. Large discrepancies were found when comparing the values used for failure in the design, which are based on recommended practice, and the actual experimental data.

A failure criterion is sought to predict the failure strains. Because of the observed discrepancies between design values and the results, any type of failure criterion should closely match experimental values rather than standard design values. A Fracture Forming Limit Curve (FFLC) is constructed using Voormeeren's methodology in conjunction with the results of a standard uni-axial tensile test. This FFLC provides a failure locus for the raking damage experiments that is accurate within 0.04 major strain for the same minor strains.

3. It was envisaged that rupture of hull plating could be detected by merely analysing acceleration measurements. In order to do so, a decrease in accelerations should occur after rupture of the plate, and a more or less steady state crack growth phase would then follow.

The acceleration measurements during the experiment, as well as the FEA that was used to design the experiment, show a clear transition from the intact phase, to a ruptured plate and the phase of crack propagation. The measurement data show a reduction in the acceleration levels that indicate the reduced resistance of the plate after crack initiation. If the accelerations reduce to zero, rather than a steady state non-zero value, the drop mass has come to a stand-still without a crack developing.

An algorithm that can detect rupture from the acceleration measurement signal has been designed. Such an algorithm should be capable of detecting rupture for the raking damage experiments in all scenarios that were encountered. The concept of displacement filtering that was introduced in this thesis, is capable of predicting plate rupture for two of the four raking damage experiments that are done.

4. Altering the friction changed the total energy dissipation of the raking damage experiments by as much as 31% (107 kJ versus 74 kJ). Two separate methods of determining the energy dissipation only due to friction, yields comparable results. Unfortunately, the measurements that were devised to determine the exact contribution of energy dissipation through friction independently from the chosen friction model (the IR minus DIC measurements) did not attain the required accuracy. In order to truly verify the validity of the Coulomb friction model it is recommended that independent additional measurements need to be performed.

On the basis of the two energy dissipations that are calculated using Coulomb friction model, it can be concluded that static Coulomb friction is a proper model for use in a raking damage scenario. Determining the friction coefficients is thereby key to an accurate calculation of the energy dissipation due to friction.

This research provided a first, exploratory step into raking damage estimation by using acceleration measurements. The onset of plate rupture can be identified from the experimental data. When a proper estimate of both the failure criterion and the friction coefficient is made, the contributions of plastic deformation, crack propagation and friction can be determined. With these contributions known, it is envisaged that the extent of raking damage can indeed be derived through real time acceleration measurements on board of a vessel.
Applications & Recommendations

Even though the concept of rupture detection through acceleration measurements seems to be feasible from the results presented in this thesis, a lot of research still has to be done before this concept can be applied for any real vessel. This chapter presents the ideas about the future of the raking damage experiments. In the first section, possible application of the results of this thesis will be put in the broader perspective of real time estimation method for the damage sustained by a ship running aground. The second section will put forward several interesting recommendations for further research.

7.1. Applications for the Raking Damage Experiments

The envisaged application for the concept of rupture detection through acceleration measurements is in real-time damage estimate aboard a vessel. This section present one idea for such a real-time damage estimate. The idea for an estimate method is based upon reference curves constructed using FEA.

Figure 7.1: The concept of using reference curves for various combinations of penetration and rock shapes, presented by Nguyen et al [20]. These curves are constructed using FEA of a grounding accident with a section of double hull. The graph presents the energy and Stopping Length as a function of penetration ($\delta_o$) of one specific rock shape.
7. Applications & Recommendations

Real-time Damage Estimation Method  In the light of the raking damage estimation method proposed in chapter 1, FEA is a necessary part of any future method for providing an accurate estimate of the structural damage. As full scale testing for each newbuild vessel is prohibitively expensive, and a waste of resources, FEA needs to be accurate enough to be able to predict structural response for a wide range of grounding scenarios.

A concept presented by Nguyen et al [20], which is also presented in section 2.3, might be interesting to use for a workable estimation method. It works with minimizing the error in energy dissipation for several different scenarios. The minimization in energy dissipation is used to find the most probable rock shape and penetration depth of this rock shape. By using FEA, a series of results for various different scenarios (rock shape and penetration depth) are simulated. The results of these scenarios are checked against a "balance of energy approach and the scenario with the smallest error is chosen as the most probable damage scenario. Figure 7.1 shows a series of reference curves for several penetration depths and for one single rock shape.

Rather than in the reference curves presented in figure 7.1, the length of the raking damage can be determined by double integration of the time signal: \[ \int \int \ddot{x} \, dt \, dt = x. \] This means that only the "depth" and "width" of the raking damage need to be determined by using the reference curves. The vertical force can be determined by using the principle of Newton’s second law: \[ F = m \cdot \ddot{z}. \] The vertical force can also be determined quasi statically by determining the vertical displacement with \[ \int \int \ddot{z} \, dt \, dt = z \] and multiplying the displacement with the waterplane area, specific mass of water and the standard gravity. The vertical force and the horizontal force are the most important force components in a grounding scenario. With these two components known, an approach based on reference curves is within reach.

7.2. Recommendations for Further Research
This experimental research had an explorative nature. The concept of estimating grounding damage from acceleration measurements was postulated and experimentally explored. In order for this concept to further develop several there are several recommendations for future research:

- Perform FEA that reflects the experiment as accurately as possible, using a calibrated failure criterion and a calibrated friction parameter according to the experimental results of this study.
- Check whether the rupture detection algorithm works for all situations encountered in the raking damage experiments.
- Study the applicability of the concept of rupture detection on more realistic grounding experiments such as the grounding experiments conducted by TNO in the 1990’s [30] or the experiments performed by the U.S. NSWC [23].
- More testing to vary the strain states for validation of single point calibration of the FFLC.
- Determine the actual energy dissipation through friction in order to verify which model is best applicable.

Additional Raking Damage Experiment  If there were the option to do additional experiments there are several interesting options:

- Study influence of the shape of the indenter. A ship colliding with a rock pinnacle will probably not encounter such a nicely rounded rock every time.
- Vary plate thicknesses and/or type of steel.
Improvements on the Experimental set up  Several opportunities to improve the experimental set-up have surfaced during this research:

- First of all the experiments should be modified such that the plate does rupture in the centre while still maintaining the gradual introduction of the plastic deformation. This would allow for capturing the transition to a ruptured plate with steady state crack propagation.

- Better alignment of the drop-mass could cause both plates to experience rupture. With the current drop tower set up, the symmetry (2 specimens in each drop test) is mandatory because the drop tower is not designed to take sideways loads. An even better option would be to re-design so that only one specimen at the time can be tested.

- Increase number of experiments in order to achieve an improved statistical significance.

Interesting options for future investigation  A couple of questions arose from this raking damage research. Here are some “out of the box” ideas for future investigation related to the experiments or simulations that were performed for this study:

- From the discussion on an algorithm for rupture detection it was shown that the ruptured signal differs in several aspects from an intact signal. Using data analysis techniques (for instance cluster analysis) the various properties can be categorized in order to make for a sound rupture detection algorithm.

- It is interesting to see that the yield stress has such a limited influence on the FEA results presented in section 3.3. The reason could be that the yield stress and the strain rate sensitivity act as communicating vessels in this case. It would be interesting to explore this idea.
The support of our “Raking Damage Partners” is gratefully acknowledged. This partnership consisted of TNO, Delft University of Technology, the Dutch Defense Material Organisation (DMO), Ardent Global, SMIT Salvage, Bureau Veritas and Damen Schelde Naval Shipbuilding. Their support made it possible to perform the experiments and take the raking damage research damage one step further.
Bibliography


OMAE 2017 paper - Grounding Damage Estimate through Acceleration Measurements
GROUNDING DAMAGE ESTIMATE THROUGH ACCELERATION MEASUREMENTS

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ABSTRACT

Instantaneous insight into the structural damage sustained by a ship running aground, can be of great aid in the decision making following such a disaster. It should be possible to distil the extent of structural damage from acceleration measurement data of the entire vessel. Such measurements could possibly even be done using a simple smartphone.

This paper describes experimental research exploring the detection of plate rupture in a raking damage scenario by merely looking at acceleration measurement data. Drop tower experiments were performed, which reflect a realistic raking damage scenario and also aim to gain better understanding of both friction and failure in such a scenario.

In total four drop tests were performed. Ductile fracture occurred without precedence of necking. A calibration method for a failure criterion, using only one single strain-state, was successfully applied. Separate friction tests showed that static Coulomb friction seems to be a proper model to be applied for the energy dissipation through friction. Moreover, the transition to plate rupture can readily be detected from the experimental acceleration data.

When accurate calibration of both the failure and the friction is performed, it is envisaged that the extent of raking damage can indeed be derived through real time acceleration measurements on board of a vessel.

Keywords: ship grounding, structural damage, acceleration measurements, plate failure, friction

INTRODUCTION

A ship colliding with a hard rock pinnacle is a potentially perilous situation. Depending on the seabed topology and vessel speed, draft and weight, the ship may either tear open or just slide over the ground. Hereafter, the vessel may get stuck on the obstruction or sail on with a considerable damage and possibly a breached hull. Such a damage scenario is called a raking damage scenario. Numerous examples of such grounding accidents can be named, with the latest well-known example being the disaster with cruise ship Costa Concordia.

When the Costa Concordia ran aground in Italy, it took 69 minutes [1] to make the critical decision to abandon ship. One of the causes of this lengthy decision making time was, that the officers on the bridge were unaware of the seriousness of the structural damage. Instantaneous insight in the structural damage and especially information on whether or not the hull is breached, may aid the decision making process during such disasters in the future. A possible way to acquire such instantaneous insight is by measuring accelerations during the grounding accident. With current technology, acceleration measurements can even be done using a simple smartphone.

In a raking damage scenario the hull plating is subjected to loading that may dent or even rupture the hull of the vessel. This experimental research explores a method that estimates whether plating has remained intact, only dented or has ruptured in a rock-pinnacle raking damage scenario by merely looking at acceleration measurement data.

The experiments are set up in such a way that they reflect a realistic raking damage scenario of a ship colliding with a rock pinnacle or iceberg. Such a raking damage scenario induces a loading on the ship’s hull plating that is both in plane with the plate and normal to the plate.

In order to deduce an estimate on the structural damage, the different components present in the accelerations data have to be determined from the measurement signal. These components are identified as being [2]:

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² Corresponding author: Martijn.Hoogeland@TNO.nl
• Friction
• Plastic deformation
• Crack propagation

Therefore, the questions that will be addressed in this paper are the following:
- What is the role of friction in a raking damage scenario?
- To what extent is plate rupture distinguishable from plastic deformation in the acceleration measurement data?
- In what way is failure of the plate initiated, and what is the energy dissipation during crack propagation?

BACKGROUND
Extensive research related to ship grounding has been carried out over the years. One of the early pioneers in this field of research was Minorsky [3], with his empirical correlation between sustained structural damage and the ship’s velocity. In an effort to understand the different damage phenomena that occur in grounding with a hard rock pinnacle, research efforts moved to cutting experiments (for instance: [4] [5]).

One of the researches that had a large contribution to the understanding of grounding damage and especially the energy absorption in such an event is that of Simonsen (1997) [2] [6]. His analytical approach distinguished three contributors to the energy absorption, being plastic deformation, crack tip related phenomena and friction:

\[ F_{\text{horizontal}} v = \dot{E}_{\text{plastic}} + \dot{E}_{\text{crack}} + \dot{E}_{\text{friction}} \approx F_{\text{plastic}} v + \int p \mu v_{\text{rel}} dS \]  

（1）

Where F is the force, v represents the velocity of the vessel, \( \dot{E} \) is the rate of energy dissipation, S the contact area between rock and hull plating, p the contact pressure, \( \mu \) the friction coefficient and \( v_{\text{rel}} \) the relative velocity of the rock-plate contact.

The crack tip related phenomena were considered to be very small compared to the other two contributions and could therefore be neglected.

Interesting to note, also in later research, is that even though friction is mentioned as one of the most important parameters in such a grounding event, it often ends up being used as one of the tuning parameters. Simonsen concluded that more research is needed in order to “better understand the special aspects of the contact mechanics” [6]. It would be especially interesting to see whether the static Coulomb friction model is also applicable in a dynamic grounding experiment.

Several spectacular large-scale experiments were also carried out in the 1990’s. The NSWC performed 1/5 scale tanker grounding experiments with the aim to qualitatively understand the failure mechanisms for various double hull structures [7]. A similar set-up was seen at the ASIS-TNO large scale grounding experiments with the aim to validate Finite Element Analysis (FEA) of the same experiments [8].

Figure 1 depicts the surge accelerations of one of the large scale grounding experiments done by ASIS-TNO. It is interesting to note that the five peaks in the acceleration time-trace before the stoppers, can be linked directly to the five transverse stiffeners of this specific ship-section. A similar observation can be made from the force-displacement curves of the NSWC experiments [7]. This has been the first cue that real-time acceleration measurements on board a vessel can be a proper tool to help make an estimate of the structural damage sustained in a raking damage accident.

![Figure 1. Surge acceleration time-trace of the ASIS-TNO large scale grounding experiments [8].](image1)

Muscat-Fenech & Atkins performed a series of very interesting “glancing collision” experiments using thin sheet metal in the 1990’s [9].

Their aim was to determine the forces that are experienced as a sheet is dented, stretched and perforated as it passes over an obstacle. The idea is that the combination of motion parallel as well as normal to it makes for different behaviour, whereas previous research only examined each of these motion components separately.

From the force-displacement curves of the glancing collision experiments in Figure 2, it can be clearly be seen that the typical force needed for crack propagation is less than the maximum resistance of a plate. This is a clear cue that even plate rupture can be estimated from merely measuring the forces (i.e. accelerations).

![Figure 2. Force-displacement of the glancing collision experiments [9].](image2)

The key to structural damage analysis with using FEA is the failure criterion that is applied. As most maritime or offshore applications of FEA make use of (rather large) shell elements for modelling a structure, there is considerable interest...
in an accurate failure criterion that can be used in conjunction with standardised material testing and large shell elements.

There are many failure criteria “on the market”, of which the GL criterion [10] is easiest to use, as the failure strain can directly be deduced from a material certificate. However, this failure criterion has strong drawbacks, especially concerning the accuracy in relation to multi-axial stress states.

The various failure modes in a maritime grounding accident ask for more generally applicable failure criteria. The BWH criterion [11] and the RTCL criterion [12], among others, are often quoted and used in the maritime and offshore crash analysis. But as Atli-Veltin et al. (2016) [13] concluded, most of these methods are not yet mature enough in terms of validation.

Failure criteria based on or translated to a Forming Limit Diagram (FLD) are very interesting in the sense that these criteria are relatively easy to comprehend and conceptualize. Moreover, they are very suitable to be applied on shell elements and make for easy post-processing of FEA results [14] [15]. The limiting strain is plotted in these FLD’s in the form of a Forming Limit Curve (FLC - for necking) or a Fracture Forming Limit Curve (FFLC - for fracture). If the analyst is only concerned with failure initiation, then they can conservatively assume that failure occurs at either necking or fracture, whichever has the lowest failure strain in a given state of stress. However, if there is desire to simulate damage propagation or reduce conservatism, then more sophisticated means would be necessary to capture the difference in strain between the onset of necking and ultimate failure [13].

When the transformation of a 3D stress or strain space to a 2D space is made for analysis using shell elements, this implies an assumption of a state of plane stress. Voormeeren et al. [16] used this plane stress assumption to devise a method to calibrate a failure criterion (the Modified Mohr-Coulomb criterion [17]) using only one single failure strain on a FLD. This MMC criterion can subsequently be translated to the FLD space, thereby providing a FFLC that can be used for further analysis.

In this raking damage experimental study, the equivalent failure strain will be used as a failure criterion for the design analysis. An FFLC that is constructed with Voormeeren’s method of calibration will also be validated, using uni-axial tensile tests to predict experimental results of the raking damage experiments.

RAKING DAMAGE EXPERIMENT SET-UP

Figure 3 shows a graphical representation of the experimental set-up. The idea is to dynamically simulate a grounding loading on a ship’s hull plating. FEA has been used in order design the experimental set up. This FEA will not be elaborated upon in this paper.

A drop tower is used to perform the raking damage experiments. The drop mass is 4575kg; the maximum drop height is 1.5m. The mass and the drop height are chosen for rupture to initiate roughly in the centre of the plate. This choice is based on the preparatory analysis performed with FEA, using design material properties.

The indenter represents the rock pinnacle with which a vessel collides. The shape of the indenter is a cylinder with spherical ends and has a radius of 75mm. It is chosen such that plastic deformation is gradually introduced (denting) until rupture occurs, after which a phase of crack propagation in combination with plastic deformation follows.

![Figure 3. Graphic representation of the experimental set-up for the raking damage experiments.](image-url)

The two specimens are placed at an angle of 15deg to the drop direction. The slight angle to the drop direction ensures that plasticity is gradually introduced. A combination of denting (or bulging) and friction precedes rupture and crack propagation. The dimensions of the plate are 800x600x6mm. The width represents a typical stiffener spacing used in maritime structures, and the thickness represents a minimum thickness as used in maritime structures. The plate is fixed along the vertical edges to a rigid steel frame representing clamped boundary conditions.

The important parameters for answering the three research questions, are the accelerations during the impact and the deformation of the specimen. Further, as friction plays an important role, the normal force to the plate needs to be determined. Therefore, measurements taken during the experiment are:

- Accelerations on the drop mass for force and displacement (after integration)
- Axial force (strain gauges on indenter) for determination of the normal force of contact
- Strains in plating (through high speed Digital Image Correlation (DIC) for the plastic deformation
- Temperature of the plating (IR camera) for the heat production due to strain and friction
A synchronized trigger signal is used to ensure accurate time tracing of the measurement data and the high-speed footage of the DIC. Thickness measurements are performed before and after the experiment to find the through-thickness strain.

**EXPERIMENTAL RESULTS & DISCUSSION**

The main goal of the experiments is to study feasibility of rupture detection through acceleration measurement. In total, four raking damage experiments were performed. The drop height was increased over the course of the experimental campaign (see Table 1).

<table>
<thead>
<tr>
<th>Exp 1</th>
<th>Exp 2</th>
<th>Exp 3</th>
<th>Exp 4</th>
</tr>
</thead>
<tbody>
<tr>
<td>0.5m (drop 1-1)</td>
<td>1.0m (drop 2-1)</td>
<td>1.5m (drop 3)</td>
<td>1.5m (drop 4)</td>
</tr>
<tr>
<td>0.5m (drop 1-2)</td>
<td>1.5m (drop 2-2)</td>
<td>Reduced</td>
<td>Reduced</td>
</tr>
<tr>
<td>0.5m (drop 1-3)</td>
<td>Reduced</td>
<td>Reduced</td>
<td>Reduced</td>
</tr>
<tr>
<td>0.5m (drop 1-4)</td>
<td>friction</td>
<td>friction</td>
<td>friction</td>
</tr>
</tbody>
</table>

Figure 4. Photo of the experimental set up moments before drop 1 of the drop tests.

The last two experiments were performed with reduced friction in order to study and quantify the influence of friction. Friction was reduced by sanding the plate surface and applying grease to both the plate surface and the indenter.

The photo in Figure 4 gives a good impression of the drop tests. During the drop test, the indenter will travel all the way to the end of the plates, into the rubber braking pads.

**Failure**

Rupture of the plate has been designated as plate failure. The moment a visual crack appears at the outer surface, the plate is assumed to be ruptured. After the rupture initiation, a phase of crack propagation follows until the crack has reached the end of the plate. This sequence of plate rupture can be seen in great detail through the high-speed images in Figure 5. These high-speed images were also used for the DIC measurements (Figure 6).

The sequence of plate rupture in Figure 5 clearly shows the first development of a through-thickness crack on the left picture which initiates on top of the contact point of the indenter with the plate. The crack quickly travels both upward and downward and travels ahead of the indenter until it reaches the end of the plate.

Figure 5. Sequence of plate rupture in one of the raking damage experiments.

The crack develops near the lower edge of the specimen. This causes the crack to advance ahead of the indenter to the edge of the plate. The envisaged stage of crack growth therefore does not occur.

There was a small misalignment of the indenter so it touches one of the two plates first. Only one of the two plates ruptured in each of the four raking damage experiments. The side that ruptured was consistent, so this probably had a connection with this slight misalignment.

In the phase of crack initiation, no necking or extreme localisation of the strains is observed. This was confirmed by ball-micrometer measurements in the vicinity of the crack. It is however noticed that the large plastic strains mainly occur in the area surrounding the indenter, which can clearly be seen from Figure 6. Outside this area, the strains remain relatively small and mainly displacement out of plane rather than in plane strain is seen. This is different compared to the deformation patterns seen in the Muscat-Fenech glancing collision experiments [9] where the sheet-metal behaved rather more like a membrane.

Moreover, it seems that changing the friction in the experiments does not influence the mode of failure.

For the development of a failure criterion for FEA, the strain condition of the plate right before the rupture moment is the most important parameter. The strain condition is captured using high-speed DIC measurements on the plate surface.

Figure 6 depicts the strains taken from the high speed DIC measurements briefly before a visual crack appears on the surface of the plate. The background image is the actual image as it was recorded by the high speed camera. The colours and white arrows indicate the magnitude and direction of the major strains ($\varepsilon_1$).

The minor strain ($\varepsilon_2$), thickness reduction (-$\varepsilon_3$) and equivalent strain (phiM) as well as the strain reference length (L0) for three points - one on top of the future crack (point 1) and two points adjacent to the future crack (point 2 & 3) - are
reported in the boxes at the top and in Table 2. All of the principle strains in Table 2 have been acquired in the same manner and with the same reference length of approximately 20mm.

In Table 2, the strains from the raking damage experiments are reported. The strains that have been found can be characterized by a constrained plane strain condition.

Table 2. Engineering Strains at failure from DIC and ball-micrometer thickness measurements of the cracked specimens.

<table>
<thead>
<tr>
<th></th>
<th>$\varepsilon_1$</th>
<th>$\varepsilon_2$</th>
<th>$\varepsilon_3$</th>
<th>$\varepsilon_3$ pt.2/3</th>
<th>measured $\varepsilon_3$</th>
</tr>
</thead>
<tbody>
<tr>
<td>Drop 1</td>
<td>-</td>
<td>-</td>
<td>-</td>
<td>-</td>
<td>-38%</td>
</tr>
<tr>
<td>Drop 2</td>
<td>61%</td>
<td>7%</td>
<td>-42%</td>
<td>-37%</td>
<td>-38%</td>
</tr>
<tr>
<td>Drop 3</td>
<td>48%</td>
<td>4%</td>
<td>-35%</td>
<td>-32%</td>
<td>-30%</td>
</tr>
<tr>
<td>Drop 4</td>
<td>48%</td>
<td>6%</td>
<td>-36%</td>
<td>-28%</td>
<td>-33%</td>
</tr>
</tbody>
</table>

DIC failure strain data for drop 1 are unusable and are therefore neither used for comparison and calculation, nor presented here.

In order to verify the DIC results, the calculated strains are checked with ball-micrometer thickness measurements around the cracked area (pt.2/3 from Figure 6). In Table 2, these measured thickness strains are shown in the last column. The DIC results from pt.2/3 seem to be in good agreement with the thickness measurements.

Tensile tests were performed after the experiments. The results are shown in Figure 7. The tensile tests have been performed according to the ISO standard for material testing [18] and were done with coupons cut from the same plate as the specimens were made of. The testing procedure is the same testing procedure that was used to determine the material properties found on the material certificate (Table 3).

The linear extrapolation of the true stress-strain relation is constructed by measuring the area reduction of the fractured specimen with a ball-micrometer and so establishing the true failure stress and strain.

The comparison in Table 3 shows the different material properties found over the course of this experimental campaign for the same material. The discrepancies seen in this comparison explain part of the difficulties one experiences when trying to model failure in maritime grounding (and crash).

Table 3. Comparison of material properties for the raking damage experiments.

<table>
<thead>
<tr>
<th>Design</th>
<th>Certificate</th>
<th>Tensile testing</th>
</tr>
</thead>
<tbody>
<tr>
<td>$\sigma_{\text{yield}}$ [MPa]</td>
<td>235</td>
<td>298</td>
</tr>
<tr>
<td>$\sigma_{\text{UTS}}$ [MPa]</td>
<td>360</td>
<td>430</td>
</tr>
<tr>
<td>Elongation [%]</td>
<td>22%</td>
<td>43%</td>
</tr>
</tbody>
</table>

The uncertainty regarding the failure strain is especially problematic. The sensitivity analysis showed the large influence of the failure criterion used for numerically analysing these results. In other words, a reliable and accurate failure strain that can be used as a basis for a failure criterion is required in order to be able to model these raking damage scenarios.

An effective criterion for the onset of failure in plate and sheet metal for states of stress between uniaxial and equibiaxial is by using a FLC or a FFLC. In Figure 8, two FFLC’s are plotted in a FLD. Both of these FFLC’s are based on the Modified Mohr-Coulomb (MMC) failure locus [17] and they are constructed using a single-point calibration method proposed by Voormereren et al. (2014) [16].

The tensile test FFLC is calibrated using the average true failure strain of the tensile tests (Figure 7) and by assuming that $\frac{\varepsilon_2}{\varepsilon_1} = -0.5$ for a uni-axial tensile test.

The experimental FFLC is calibrated using the average true failure strains found from the DIC analysis of the four raking damage experiments.
The blue line is the equivalent failure strain, plotted in the FLD space. In Figure 8 it is based on the average failure strains from the drop tests (i.e. raking damage experiments).

Figure 8. FFLD comparison of the failure criterion used for design and the retrieved failure strains from DIC.

Figure 8 shows that the equivalent failure strain criterion is limited to states of stress for which it is specifically tuned. In practice this means that if the equivalent failure strain is calibrated based on plane strain testing, then the failure strains near the uni-axial condition are very conservative. Similarly, if the true failure strain of the tensile tests were to be used (not plotted in Figure 8), the predicted equivalent failure strain would be very un-conservative, which is undesirable.

A comparison of the different failure strains in the FLD (Figure 8) shows that the single-point calibration method based on a uni-axial tensile test works well to predict the failure criterion found from the experimental data. This observation provides preliminary evidence that the FFLC calibrated using only one single strain-state - by using the method proposed by Voormeeren et al. [16] - provides an accurate failure criterion for the raking damage experiments.

**Accelerations**

The acceleration measurements are the core of these experiments. It was envisaged that rupture of hull plating could be detected by merely looking at acceleration measurements. In order to do so, a decrease in accelerations should occur after rupture of the plate, and a more or less steady state crack growth phase would then follow.

In the following figures, the dash-dotted vertical lines indicate key moments during the experiment. The first line is the moment the indenter touches both plates. The second line indicates the exact moment a crack appears on the outer surface and the last line indicates the moment the bottom of the plate is reached and the indenter hits the rubber brake-pads.

In order to be able to compare all experiments, some with different drop heights and combinations of several drop tests, the force-displacement graphs are an effective tool. The total displacements for each experiment are the same and the material deformation picks up where the previous drop ended. This means that the force-displacements can readily be compared with one another.

The total absolute displacements are the same (0.51m from touch to bottom) for all experiments except for drop 1 which had an extra rubber brake pad.

**Figure 9. Vertical force – displacement of drop 3.**

The accelerations in Figure 9 show a gradual build up that starts shortly after the indenter touches both plates. The moment a visible crack develops at the outer surface of the plate is back traced using the images of the high speed cameras (Figure 5). This is indicated in the figure by the dash-dotted line. Right after the moment a crack appears the accelerations of the drop mass decrease significantly. Due to a misalignment of the indenter it touches one of the two plates first. This can clearly be seen in

**Figure 10. Vertical force – displacement of drop 4.**
the acceleration signal. The moment the indenter touches both plates is indicated in Figure 9 by the most left vertical dash-dotted line. A vibration in the drop mass is observed slightly before the mass touches both plates.

Drop 4 in Figure 10 is a near duplicate of drop 3. Again a similar build-up of the force is observed. After rupture of the plating the force decreases significantly until the drop mass hits the brake pads at the bottom.

Figure 11. Vertical force - displacement of raking damage test 1. Combined data from the four drops until plate rupture.

Figure 12. Vertical force - displacement of raking damage test 2. Combined data from the two drops until plate rupture.

The reduction in the acceleration signal also occurs in absence of plate rupture (which occurs in drop1-1 / 1-2 / 1-3 and 2-1 in Figure 11 & Figure 12). The difference is that the mass has come to a complete stop. This can be seen by the forces (accelerations) reducing to zero rather than a higher, more or less steady state, value.

It can also be seen that the transition at plate rupture is less obvious for drop 1 and drop 2, but still present and possible to detect. The measurements show a decrease compared to the maximum force before rupture which is very encouraging.

In general, the transition from the gradual build-up of the force to the drop in the acceleration signal when a crack appears is a strong indication that the acceleration measurements can indeed be used to estimate the raking damage and especially, to determine whether a crack has developed.

**Friction**

The friction is an influential parameter in a raking damage scenario. When looking at friction it is convenient to determine the energy that is dissipated in the form of friction. This can be done using the acceleration (force) measurements. Rewriting the energy balance for the dissipated energy gives:

$$E_{\text{dissipated}} = E_{\text{total}} - E_{\text{potential}} - E_{\text{kinetic}} \quad (3)$$

Where $E$ is the energy. The potential energy comes from the displacements of the drop mass and the kinetic energy from the velocities. Both displacements and velocities have been derived from the accelerations measured during the experiments.

Figure 13. Dissipated energy - displacement for all four experiments.

The dissipated energy as a function of the displacement from the location the indenter touches both plates is shown in Figure 13. The asterisk indicates plate rupture and the circle indicates the indenter reaches the bottom and thus the end of the experiment. For the first and the second experiment, the dissipated energies have been determined by adding up the consecutive drop tests and using the same displacements as established for the combined force-displacement graphs.

The dissipated energies in Figure 13 (which can also be found in Table 4) seem to correlate with each other very well for the two separate cases of varying the friction. Despite the limited number of repetitions, the experiments seem to reproduce and the results do look very encouraging.
Based on the same materials that were used in the raking damage experiments, separate friction tests were performed to determine the friction coefficients of the two different friction scenarios.

The friction force \( F_{\text{friction}} = \mu F_{\text{normal}} \) is linearly dependent on the friction coefficient (when assuming static Coulomb friction and similar) so the dissipated friction work \( W_{\text{friction}} = F_{\text{friction}} \Delta z \) - and thus energy - is also linearly dependent on \( \mu \). Because the total dissipation work is a sum of the dissipation through both friction and strain \( W_{\text{dissipated}} = W_{\text{friction}} + W_{\text{strain}} \), this leads to the linear extrapolation of the experimental results for the dissipated energy as a function of the friction coefficient in Figure 15.

When combining the energy results with the results from the separate friction coefficient tests, the significance of friction in a raking damage scenario becomes even more obvious. In Table 4 it can be seen that the experiments without grease have an average energy dissipation due to friction of 34.8% of the total energy dissipation compared to 5.7% with grease applied to the surface (reduced friction).

The question is however, whether the assumption of static coulomb friction holds when addressing a rather complex combination of friction and plastic deformation simultaneously.

<table>
<thead>
<tr>
<th>Dissipated Energy [kJ]</th>
<th>Friction energy [kJ]</th>
<th>% friction</th>
<th>( \mu ) [-]</th>
</tr>
</thead>
<tbody>
<tr>
<td>Drop 1 107.7</td>
<td>37.74</td>
<td>35.0%</td>
<td>0.28</td>
</tr>
<tr>
<td>Drop 2 107.0</td>
<td>37.01</td>
<td>34.6%</td>
<td>0.28</td>
</tr>
<tr>
<td>Drop 3 73.2</td>
<td>3.24</td>
<td>4.4%</td>
<td>0.03</td>
</tr>
<tr>
<td>Drop 4 75.2</td>
<td>5.23</td>
<td>6.9%</td>
<td>0.03</td>
</tr>
</tbody>
</table>

The energy dissipation through friction can also be calculated from the measurement data. This can be done by determining the contact area and the corresponding contact pressure. Integrate the contact pressure over the contact area and multiply with the friction coefficient and one obtains the friction force. Multiplying again with the displacement yields the energy dissipation \( E_{\text{friction}} = \int p \mu z_{\text{rel}} dS \) with \( E = \int p \mu z_{\text{rel}} dS \).
energy dissipation, $S =$ contact area, $p =$ contact pressure, $\mu =$ friction coefficient and $z_{rel} =$ displacement of the contact force. Simonsen (1997) [2] used a similar approach in his analytical analysis to determine energy dissipation through friction.

For these experiments however, it proved to be impossible to retrieve the exact contact pressures and contact area. Instead, the axial forces on the indenter were measured using strain gauges. This enables calculation of the resultant contact forces (Figure 16) and the respective angle with the indenter’s heart-line of these forces.

By looking at the contact marks on the indenter, the area and the angle of the normal force of contact could be estimated (Figure 17). This angle was taken as a fixed angle throughout all the drop tests.

The angle of the contact force and the resultant force are subsequently used to determine the normal force of contact. The normal force is needed for determination of the friction force ($F_{friction} = \mu \cdot F_{normal}$). Hereafter the friction coefficients – found from the friction coefficient tests – are applied and the energy dissipation through friction is calculated.

The results of the energy dissipations through friction calculated via the two different methods are shown in Table 5. It can be seen that for the steel-steel case the friction energies are within 10% of the total energy dissipation. For the lubricated cases the results are even within 1% of the total energy dissipation.

### Table 5. Comparison of dissipated energy through friction.

<table>
<thead>
<tr>
<th>Linear extrapolation</th>
<th>Fixed angle of $F_{normal}$</th>
</tr>
</thead>
<tbody>
<tr>
<td>$E_{friction}$ [kJ]</td>
<td>$\mu \cdot F_{normal}$ [%]</td>
</tr>
<tr>
<td>$E_{friction}$ [kJ]</td>
<td>$\mu \cdot F_{normal}$ [%]</td>
</tr>
<tr>
<td>Drop 1 37.74 35.0%</td>
<td>46.33 43.0%</td>
</tr>
<tr>
<td>Drop 2 37.01 34.6%</td>
<td>46.24 43.2%</td>
</tr>
<tr>
<td>Drop 3 3.24 4.4%</td>
<td>3.94 5.4%</td>
</tr>
<tr>
<td>Drop 4 5.23 6.9%</td>
<td>3.85 5.1%</td>
</tr>
</tbody>
</table>

Even though both methods of determining the energy dissipation can be considered as somewhat crude, the similarity between the results is striking. Based on these results the static Coulomb friction model leads to corresponding results even when determining the energy dissipations in a different way. Therefore the static Coulomb friction model is deemed to be a proper model to use in a raking damage scenario.

Measuring the actual energy dissipation due to friction can take away any uncertainty regarding the static Coulomb friction model. It was attempted to measure that in this study by applying a combination of infra-red cameras and the DIC measurements. Unfortunately it proved to be difficult to combine these two different measurement techniques with sufficient accuracy.

### CONCLUSIONS

In order to determine hull plate rupture in ship grounding from acceleration measurements, the various contributions to such an acceleration signal have been studied with these raking damage experiments:

- The acceleration measurements during the experiment as well as the FEA of the experiment show a clear transition from pure plastic deformation to crack growth. The measurement data show a reduction in the acceleration levels that indicate the reduced resistance of the plate after crack initiation. A reduction to zero indicates the drop mass having come to a stand-still without a crack developing.

- Friction – Static coulomb friction is a proper model for use in a raking damage scenario. Determination of the real friction coefficients is thereby key to an accurate determination of the energy dissipation due to friction.

- Failure in the raking damage experiments is initiated in a local zone around the indenter. Crack initiation occurred in the experiments without clear evidence of necking. The strains directly before rupture have been captured using high speed DIC. Large discrepancies were found when comparing the values used for failure based on recommended practice and the actual experimental data.

- For an estimate for plate rupture through acceleration measurement, and especially for FEA of a similar scenario, any type of failure criterion should closely match experimental values rather than standard design values. The FFLC constructed using Voormeerens’s methodology provides a very accurate failure locus for the raking damage experiments.

The clear transition at plate rupture, seen in the measurement data after plate rupture, is a strong indication that detection of plate rupture from acceleration measurements is possible. Therefore, it can be concluded that using real time acceleration measurement is feasible as a method to estimate the structural damage of a vessel directly after a grounding accident.

### RECOMMENDATIONS

This experimental research had an explorative nature. The idea of estimating grounding damage from acceleration measurements was postulated and experimentally verified. In order for this idea to further develop, several ideas for future research are recommended by the authors:

- Perform FEA that reflects the experiment as accurately as possible with an updated failure criterion according to the experimental results of this study. This would allow for a comparison between the experimental results and FEA.

- Development of an algorithm that can distinguish rupture in more realistic cases. In reality the acceleration measurements will also be influenced by ship motions, the ship structure as a whole, shape and size of the rock and many more parameters.

- Study the applicability of the ideas developed in this research on more realistic grounding experiments such as the grounding experiments conducted by TNO in the 1990’s [8] or the experiments performed by the U.S. NSWC [7].
Several opportunities to improve or extend the experimental set up, have surfaced during this research:

- The experiments should be redesigned such that the plate does rupture in the middle, while still maintaining the gradual introduction of the plastic deformation. This would even better capture the transition to a ruptured plate with steady state crack propagation.
- Study influence of the shape of the indenter.
- Vary plate thicknesses and/or type of steel.
- Increase the number of experiments in order to achieve statistical significance.

ACKNOWLEDGMENTS

The support of our “Raking Damage Partners” is gratefully acknowledged. This partnership consisted of TNO, Delft University of Technology, the Dutch Defense Material Organisation (DMO), Ardent Global, SMIT Salvage, Bureau Veritas and Damen Schelde Naval Shipbuilding.

NOMENCLATURE

$\epsilon / \epsilon_1, \epsilon_2$: Strain [-] / principal strains [-]

$\mu$: Friction coefficient [-]

$\sigma$: Stress [MPa]

$E$: Energy [J]

$F$: Force [N]

$p$: Pressure [Nm$^{-2}$]

$W$: Work [J]

$z$: Vertical displacement [m]

DIC: Digital Image Correlation

FEA: Finite Element Analysis

FEM: Finite Element Model

FLC: Forming Limit Curve

FFLC: Fracture Forming Limit Curve

FLD: Forming Limit Diagram

MMC: Modified Mohr-Coulomb (failure criterion) [17]


RTCL: Rice-Tracey-Cockcroft-Latham (failure criterion) [12]

UTS: Ultimate Tensile Strength

REFERENCES


LS-Dynakeywordfile for preparatory analysis

Finite Element Model for preparatory analysis. This model only contains the keyword input cards for the model parameters. The node and element cards have been removed from this file.

$# LS-DYNA Keyword file created by LS-PrePost(R) V4.3 - 24Aug2016(10:00)
$# Created on Nov-02-2016 (14:23:22)
*KEYWORD
*TITLE
$# title
LS-DYNA keyword deck by LS-PrePost
*CONTROL_ENERGY
$# hgen rwen slnten rylen
 2 2 2 1
*CONTROL_HOURGLASS
$# ihq qh
 4 0.15
*CONTROL_TERMINATION
$# endtim endcyc dtmin endeng endmas
 0.15 0 0.0 0.01.000000E8
*DATABASE_ELOUT
$# dt binary lcur ioopt option1 option2 option3 option4
1.000000E-4 0 0 1 0 0 0 0
*DATABASE_GLSTAT
$# dt binary lcur ioopt
1.000000E-4 0 0 1
*DATABASE_NODFOR
$# dt binary lcur ioopt
1.000000E-4 0 0 1
*DATABASE_BINARY_D3PLOT
$# dt lcdn beam npltc psetid
1.000000E-4 0 0 0 0
$# ioopt
 0
*DATABASE_EXTENT_BINARY
$# neigh neips maxint strflg sigflg epsflg rltflg engflg
 0 0 5 1 1 1 1 1
$# cmpflg ieverp beamip dcomp shge stssz n3thdt ialemat
 0 0 0 1 1 1 2 1
$# nintsld pkp_sen sclp hydro msscl therm intout nodout

97
B. LS-Dyna keyword file for preparatory analysis

```plaintext
*DATABASE_NODAL_FORCE_GROUP

*DATABASE_NODAL_FORCE_GROUP

*DATABASE_HISTORY_SHELL_SET

*BOUNDARY_SPC_SET

*SET_NODE_LIST_TITLE

*BOUNDARY_SPC_SET

*SET_NODE_LIST_TITLE

*LOAD_RIGID_BODY

*CONTACT_AUTOMATIC_SURFACE_TO_SURFACE_ID

1 Indentor Plate contact
```
Two Plates

Plate_left

Plate_right

Indentor

Indentor (rigid)
*INITIAL_VELOCITY_RIGID_BODY
$# pid vx vy vz vxr vyr vzar icid
  3  0.0  0.0  -5.94  0.0  0.0  0.0  0

*DEFINE_COORDINATE_SYSTEM_TITLE
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  1 -0.3 -0.139843  0.0056  0.3 -0.139843  0.0056  0
$# xp yp zp
  -0.3 -0.346777  0.778372

*DEFINE_COORDINATE_SYSTEM_TITLE
Coord_plate_2
$# cid xo yo zo xl yl zl cidl
  2 -0.3  0.139843  0.0056  0.3  0.139843  0.0056  0
$# xp yp zp
  -0.3  0.346777  0.778372

*DEFINE_CURVE_TITLE
Gravity loading
$# lcid sidr sfa sfo offa offo dattyp lcint
  11  0  1.0  1.0  0.0  0.0  0.0  0
$# a1 o1
  0.0  9.81
  50.0  9.81
  100.0  9.81
  150.0  9.81
  200.0  9.81

*SET_PART_LIST_TITLE
Indentor
$# sid da1 da2 da3 da4 solver
  2  0.0  0.0  0.0  0.0 0.0MECH
$# pid1 pid2 pid3 pid4 pid5 pid6 pid7 pid8
  3  0  0  0  0  0  0  0

*SET_SHELL_LIST_TITLE
Shell_data
$# sid da1 da2 da3 da4
  1  0.0  0.0  0.0  0.0
$# eid1 eid2 eid3 eid4 eid5 eid6 eid7 eid8
  885  888  889  890  954  955  956  957
 1007 1008 1009 1010 1070 1074 1075 1076
 1109 1110 1111 1113 1116 1117 1119 1120
 1145 1146 1147 1148 1149 1150 1151 1152
 1153 1154 1155 1156 1157 1158 1159 1160
 1161 1162 1163 1164 1165 1166 1167 1168
 1169 1170 1171 1172 1173 1174 1175 1176
 1177 1178 1179 1180 1181 1182 1183 1184
 1185 1186 1187 1188 1189 1190 1191 1192
 1193 1194 1195 1196 1197 1198 1199 1200
  315  317  319  320  435  436  437  438
  535  536  539  540  647  648  649  650
  719  720  721  722  810  811  812  813
  791  792  793  794  886  887  891  892
  950  951  952  953  1043 1046 1047 1048
 1067 1068 1071 1072  0  0  0  0

*ELEMENT_SHELL
$# eid pid n1 n2 n3 n4 n5 n6 n7 n8
  1  1  3  1  140 141  0  0  0  0
2 1 72 111 142 112 0 0 0 0

$\#...$ geometry, nodes and elements are not reported in this example inputfile.

*NODE
$\# nid x y z tc rc$
1 0.3 -0.139843 0.0056 0 0
2 0.3 -0.346777 0.778372 0 0
$\#...$ geometry, nodes and elements are not reported in this example inputfile.

*TERMINATION_BODY
$\# pid stop maxc minc$
3 31.00000E21 -0.7

*END
Experiment set-up

This appendix contains drawings and illustrations of the experimental set-up used for the raking damage experiments.

Drawings:

• Complete set-up
• Specimens
• Support frame
• Indenter
Figure C.1: Overview of the experimental set-up of the raking damage experiments.
Figure C.2: Close-up of the experimental set-up of the raking damage experiments.
Figure C.3: Side view of the experimental set up with the coordinate system that is used.
Figure C.4: The specimens attached to the support frame.

Figure C.5: The drop mass with the indenter attached to it.
Specimens - Raking Damage Experiments
Top view, side view, perspective view
4x gat boren in bestaand frame

nieuwe plaat met aangelaste doorn
S355J2+N met 3.1 cert.
Checklist Raking Damage Experiments

This appendix contains the checklist that was used during the execution of the raking damage experiments. The checklist contains vital information about practicalities that are important for post-processing the experimental data (appendix E).
## Checklist

Raking damage experiments – Stan Haag

### Specimen preparation

<table>
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<th>2</th>
<th>3</th>
<th>4</th>
</tr>
</thead>
<tbody>
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<td>⑥fw</td>
<td>⑧fw</td>
</tr>
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<td>④ aft</td>
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<tr>
<td>D.I.C. scatter applied</td>
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<td>✔</td>
<td>✔</td>
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<tr>
<td>Specification specimen numbers attached</td>
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<td>✔</td>
<td>✔</td>
<td>✔</td>
</tr>
<tr>
<td>Markers and grid for D.I.C.</td>
<td>✔</td>
<td>✔</td>
<td>✔</td>
<td>✔</td>
</tr>
<tr>
<td>Markers and specimen specification infrared</td>
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<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Markers and specimen specification infrared</td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Markers for magnetic testing</td>
<td>✔</td>
<td>✔</td>
<td>✔</td>
<td>✗</td>
</tr>
</tbody>
</table>

### Drop tower set-up check

<table>
<thead>
<tr>
<th>Simultaneous impact</th>
<th>1</th>
<th>2</th>
<th>3</th>
<th>4</th>
</tr>
</thead>
<tbody>
<tr>
<td>Simultaneous impact</td>
<td>③ touch first ±3cm</td>
<td>② touch first ±3cm</td>
<td>⑥ touch first ±3cm</td>
<td>⑧ touch first ±3cm</td>
</tr>
<tr>
<td>Drop height check</td>
<td>1: 0.5 m</td>
<td>1: 1.0 m</td>
<td>1: 1.5 m</td>
<td>1: 1.5 m</td>
</tr>
<tr>
<td>Height when indenter touches both plates</td>
<td>2: 0.5 m</td>
<td>2: 1.5 m</td>
<td>3: 0.5 m</td>
<td>4: 0.5 m</td>
</tr>
<tr>
<td>Drop mass check</td>
<td>4578 kg ± 10 kg</td>
<td>4578 kg ± 10 kg</td>
<td>4578 kg ± 10 kg</td>
<td>4578 kg ± 10 kg</td>
</tr>
<tr>
<td>Grease yes/no</td>
<td>No</td>
<td>No</td>
<td>Yes</td>
<td>Yes</td>
</tr>
<tr>
<td>Take photos</td>
<td>✔</td>
<td>✔</td>
<td>✔</td>
<td>✔</td>
</tr>
</tbody>
</table>
## Measurement check

<table>
<thead>
<tr>
<th></th>
<th>1</th>
<th>2</th>
<th>3</th>
<th>4</th>
</tr>
</thead>
<tbody>
<tr>
<td>Strain gauges (+numbering and location)</td>
<td>1 ⊕ 3 4 2</td>
<td>1 ⊕ 3 4 2</td>
<td>1 ⊕ 3 4 2</td>
<td>1 ⊕ 3 4 2</td>
</tr>
<tr>
<td>Accelerometer (+numbering)</td>
<td>6 fw 5 aft</td>
<td>6 fw 5 aft</td>
<td>6 fw 5 aft</td>
<td>6 fw 5 aft</td>
</tr>
<tr>
<td>Displacement measurement laser</td>
<td>✓</td>
<td>✓</td>
<td>✓</td>
<td>✓</td>
</tr>
<tr>
<td>Measure thicknesses</td>
<td>✓</td>
<td>✓</td>
<td>✓</td>
<td>✓</td>
</tr>
<tr>
<td>Measure room temperature</td>
<td>19.4°C</td>
<td>20.5°C</td>
<td>19.7°C</td>
<td>20.0°C</td>
</tr>
<tr>
<td>High speed set-up</td>
<td>✓</td>
<td>✓</td>
<td>✓</td>
<td>✓</td>
</tr>
<tr>
<td>infra-red camera (check for reflection)</td>
<td>✓</td>
<td>✓</td>
<td>✓</td>
<td>✓</td>
</tr>
</tbody>
</table>

1. Start measurement  
2. Measurement pulse  
3. Drop!

### After drop test

<table>
<thead>
<tr>
<th></th>
<th>1</th>
<th>2</th>
<th>3</th>
<th>4</th>
</tr>
</thead>
<tbody>
<tr>
<td>Measure thicknesses</td>
<td>✓</td>
<td>✓</td>
<td>✓</td>
<td>✓</td>
</tr>
<tr>
<td>Take photos</td>
<td>✓</td>
<td>✓</td>
<td>✓</td>
<td>✓</td>
</tr>
</tbody>
</table>
Experimental Data - Accelerometers, Strain Gauges

This appendix provides the measurement data from the two accelerometers and the four strain gauges. The location of the accelerometers and the strain gauges is depicted in figure E.1. An overview of all drop tests performed for the four raking damage experiments is provided in table E.1.

Table E.1: Overview of the four raking damage experiments performed. The drop heights that are indicated are approximate drop heights that were measured from the location the indenter touches both plates.

<table>
<thead>
<tr>
<th>Experiment 1</th>
<th>Experiment 2</th>
<th>Experiment 3</th>
<th>Experiment 4</th>
</tr>
</thead>
<tbody>
<tr>
<td>0.5m (drop 1-1)</td>
<td>1.0m (drop 2-1)</td>
<td>1.5m (drop 3)</td>
<td>1.5m (drop 4)</td>
</tr>
<tr>
<td>0.5m (drop 1-2)</td>
<td>1.5m (drop 2-2)</td>
<td></td>
<td></td>
</tr>
<tr>
<td>0.5m (drop 1-3)</td>
<td></td>
<td>Reduced friction</td>
<td>Reduced friction</td>
</tr>
<tr>
<td>0.5m (drop 1-4)</td>
<td></td>
<td></td>
<td></td>
</tr>
</tbody>
</table>

(a) Locations of the strain gauges on the indenter.
(b) Accelerometer & strain gauges on the indenter.

Figure E.1: Locations of accelerometers and strain gauges.
Drop 1-1

Figure E.2: Acceleration time-trace of drop 1-1.

Figure E.3: Strain time-trace of drop 1-1.
Drop 1-2

Figure E.4: Acceleration time-trace of drop 1-2.

Figure E.5: Strain time-trace of drop 1-2.
Drop 1-3

Figure E.6: Acceleration time-trace of drop 1-3.

Figure E.7: Strain time-trace of drop 1-3.
Drop 1-4

Figure E.8: Acceleration time-trace of drop 1-4.

Figure E.9: Strain time-trace of drop 1-4.
Drop 2-1

Figure E.10: Acceleration time-trace of drop 2-1.

Figure E.11: Strain time-trace of drop 2-1.
Drop 2-2

Figure E.12: Acceleration time-trace of drop 2-2.

Figure E.13: Strain time-trace of drop 2-2.
Drop 3

Figure E.14: Acceleration time-trace of drop 3.

Figure E.15: Strain time-trace of drop 3.
Figure E.16: Acceleration time-trace of drop 4.

Figure E.17: Strain time-trace of drop 4.
Results of Displacement Filtering of Force-Displacement Data

This appendix presents the results of the displacement filtering approach presented in section 5.4.2.

So how does this displacement filtering work? The acceleration measurement data have a constant sampling frequency, meaning that all data points have an equal $\Delta t$ between them. The Force-Displacement graph is now reconstructed so that there is equal displacement, $\Delta z$, between the data points. This ‘reconstruction’ is done with linear interpolation. The displacement filtering is done on the basis of the “typical crack displacement”. This typical crack displacement is the displacement of the indenter from rupture until the end (so the displacement between the red and the blue dash-dotted lines). All these typical crack displacements are presented in table F.1. The filter is a moving average filter that filters exactly as many data points in order to represent the typical crack displacement.

The next step is to analyse the gradient of these filtered data. In section 5.2 the acceleration results showed a decrease after rupture initiation. If a decrease occurs for a displacement larger that the typical crack displacement, the plate is assumed to be ruptured. In other words, if the gradient of the filtered signal is smaller than zero, the plate has ruptured.

Table F.1: Typical crack displacements for all four raking damage experiments.

<table>
<thead>
<tr>
<th>Experiment</th>
<th>Typical Crack Displacement</th>
</tr>
</thead>
<tbody>
<tr>
<td>Experiment 1</td>
<td>0.041 m</td>
</tr>
<tr>
<td>Experiment 2</td>
<td>0.099 m</td>
</tr>
<tr>
<td>Experiment 3</td>
<td>0.071 m</td>
</tr>
<tr>
<td>Experiment 4</td>
<td>0.073 m</td>
</tr>
</tbody>
</table>
Figure F.1: Results for drop 1 of the displacement filtering of force-displacement data.

Figure F.2: Results for drop 2 of the displacement filtering of force-displacement data.
Figure F.3: Results for drop 3 of the displacement filtering of force-displacement data.

Figure F.4: Results for drop 4 of the displacement filtering of force-displacement data.
Specifications of Measurement Equipment

Specs (and settings) of the:

• Accelerometers
• Strain Gauges
• DEWETRON (data acquisition system)
• High speed cameras
• IR camera
**Accelerometers**
The accelerometers used for the raking damage experiments are 30g Endevco variable capacitance accelerometers (model 7292A-30M1). The specification sheet is attached to this appendix after this page.
The Endevco® model 7292A-XXM1 Microtron® accelerometer utilizes unique variable capacitance microsensors for the measurement of relatively low level accelerations in rugged aerospace and automotive environments. Since it can respond to DC accelerations (steady-state events), it is ideal for measuring whole body motion even after being subjected to shock motion. The 7292A-XXM1 series accelerometer is designed to withstand shock levels up to 10 000 g's with immediate recovery.

The 7292A-XXM1 features a rugged hermetic, stainless-steel package provides years of reliable service. The 10-32 mounting studs and 6-pin electrical interface make the 7292A-XM1 series of accelerometers a “plug and play” equivalent to the popular Endevco 2262CA series accelerometer.

The 7292A-XXM1 operates from an 8.5 to 30 Vdc source and provides a high level, low impedance output biased at 3.6v. The use of gas damping provides the near-critically damped characteristics found in the 2262 series of accelerometers without thermally induced changes in frequency response. The output can be fed into either a differential or single-ended amplifier, or standard bridge electronics having 10 Vdc excitation.

Endevco model 136 three-channel system, model 4430A or the Oasis 2000 computer-controlled system are recommended as signal conditioners and power supplies.

Features

- 10 to 100 g full scale
- Overrange stops
- Gas damping
- Rugged, hermetically sealed
- Optional simulated shunt calibration (M2) or temperature output (M3) also available

Description

The Endevco® model 7292A-XXM1 Microtron® accelerometer utilizes unique variable capacitance microsensors for the measurement of relatively low level accelerations in rugged aerospace and automotive environments. Since it can respond to DC accelerations (steady-state events), it is ideal for measuring whole body motion even after being subjected to shock motion. The 7292A-XXM1 series accelerometer is designed to withstand shock levels up to 10 000 g’s with immediate recovery.

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Endevco model 136 three-channel system, model 4430A or the Oasis 2000 computer-controlled system are recommended as signal conditioners and power supplies.
# Model 7292A

## Variable capacitance accelerometer

### Specifications

All values are typical at +75°F (+24°C) and 10 Vdc excitation unless otherwise stated. Calibration data, traceable to the National Institute of Standards (NIST), is supplied.

#### Performance characteristics

<table>
<thead>
<tr>
<th>Units</th>
<th>7292-2M1</th>
<th>-10M1</th>
<th>-30M1</th>
<th>-50M1</th>
<th>-100M1</th>
</tr>
</thead>
<tbody>
<tr>
<td>Range</td>
<td>g pk</td>
<td>±20</td>
<td>±30</td>
<td>±50</td>
<td>±100</td>
</tr>
<tr>
<td>Sensitivity</td>
<td>mV/g</td>
<td>1000</td>
<td>±10</td>
<td>±20</td>
<td>±40</td>
</tr>
<tr>
<td>Frequency response (±5%)</td>
<td>Hz</td>
<td>0 to 15</td>
<td>0 to 500</td>
<td>0 to 800</td>
<td>0 to 1000</td>
</tr>
<tr>
<td>Mounted resonance frequency</td>
<td>Hz</td>
<td>3000</td>
<td>5500</td>
<td>5500</td>
<td>6000</td>
</tr>
<tr>
<td>Non-linearity and hysteresis</td>
<td>%</td>
<td>±0.20</td>
<td>±0.20</td>
<td>±0.20</td>
<td>±1.0</td>
</tr>
<tr>
<td>% FSO typ</td>
<td>±0.50</td>
<td>±0.50</td>
<td>±0.50</td>
<td>±0.50</td>
<td>±0.50</td>
</tr>
<tr>
<td>% FSO [Max]</td>
<td>±1.0</td>
<td>±1.0</td>
<td>±1.0</td>
<td>±1.0</td>
<td>±1.0</td>
</tr>
<tr>
<td>Transverse sensitivity</td>
<td>%</td>
<td>1</td>
<td>1</td>
<td>1</td>
<td>1</td>
</tr>
<tr>
<td>Zero measurand output</td>
<td>mV</td>
<td>±200</td>
<td>±200</td>
<td>±200</td>
<td>±200</td>
</tr>
<tr>
<td>Damping ratio</td>
<td>4.0</td>
<td>0.7</td>
<td>0.7</td>
<td>0.7</td>
<td>0.6</td>
</tr>
<tr>
<td>Damping ratio change</td>
<td>%</td>
<td>±0.04</td>
<td>±0.04</td>
<td>±0.04</td>
<td>±0.04</td>
</tr>
<tr>
<td>From -65°F to +250°F (-55°C to +121°C)</td>
<td>%</td>
<td>±0.08</td>
<td>±0.08</td>
<td>±0.08</td>
<td>±0.08</td>
</tr>
<tr>
<td>Thermal zero shift</td>
<td>%</td>
<td>±2.0</td>
<td>±2.0</td>
<td>±2.0</td>
<td>±2.0</td>
</tr>
<tr>
<td>From 32°F to 122°F (0°C to 50°C)</td>
<td>%</td>
<td>±2.0</td>
<td>±2.0</td>
<td>±2.0</td>
<td>±2.0</td>
</tr>
<tr>
<td>From -13°F to +167°F (-25°C to +75°C)</td>
<td>%</td>
<td>±4.0</td>
<td>±4.0</td>
<td>±4.0</td>
<td>±4.0</td>
</tr>
<tr>
<td>From -65°F to +250°F (-55°C to +121°C)</td>
<td>%</td>
<td>±6.0</td>
<td>±6.0</td>
<td>±6.0</td>
<td>±6.0</td>
</tr>
<tr>
<td>Thermal sensitivity shift</td>
<td>%</td>
<td>±0.006</td>
<td>±0.006</td>
<td>±0.006</td>
<td>±0.006</td>
</tr>
<tr>
<td>From 32°F to 122°F (0°C to 50°C)</td>
<td>%</td>
<td>±0.006</td>
<td>±0.006</td>
<td>±0.006</td>
<td>±0.006</td>
</tr>
<tr>
<td>From -13°F to +167°F (-25°C to +75°C)</td>
<td>%</td>
<td>±0.006</td>
<td>±0.006</td>
<td>±0.006</td>
<td>±0.006</td>
</tr>
<tr>
<td>From -65°F to +250°F (-55°C to +121°C)</td>
<td>%</td>
<td>±0.006</td>
<td>±0.006</td>
<td>±0.006</td>
<td>±0.006</td>
</tr>
<tr>
<td>Mechanical stops, typical</td>
<td>g</td>
<td>±4.0</td>
<td>±7.0</td>
<td>±200</td>
<td>±200</td>
</tr>
<tr>
<td>Recovery time</td>
<td>μs</td>
<td>&lt; 10</td>
<td>&lt; 10</td>
<td>&lt; 10</td>
<td>&lt; 10</td>
</tr>
<tr>
<td>Threshold (resolution)</td>
<td>5</td>
<td>0.0005</td>
<td>0.0025</td>
<td>0.0075</td>
<td>0.025</td>
</tr>
<tr>
<td>Base strain sensitivity, max (A)</td>
<td>g</td>
<td>0.01</td>
<td>0.01</td>
<td>0.01</td>
<td>0.01</td>
</tr>
<tr>
<td>Magnetic susceptibility</td>
<td>%</td>
<td>±0.1</td>
<td>±0.1</td>
<td>±0.1</td>
<td>±0.1</td>
</tr>
<tr>
<td>Warm-up time (to within 1%)</td>
<td>ms</td>
<td>10</td>
<td>10</td>
<td>10</td>
<td>10</td>
</tr>
</tbody>
</table>

#### Environmental characteristics

<table>
<thead>
<tr>
<th>Acceleration limits (in any direction)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Static</td>
</tr>
<tr>
<td>Sinusoidal/random vibration</td>
</tr>
<tr>
<td>Shock (half-sine pulse)</td>
</tr>
<tr>
<td>Zero shift</td>
</tr>
<tr>
<td>Temperature</td>
</tr>
<tr>
<td>Operating</td>
</tr>
<tr>
<td>Storage</td>
</tr>
<tr>
<td>Humidity/altitude</td>
</tr>
<tr>
<td>ESD sensitivity</td>
</tr>
<tr>
<td>Case material</td>
</tr>
<tr>
<td>Electrical, connections</td>
</tr>
<tr>
<td>Mounting/torque</td>
</tr>
<tr>
<td>Mass</td>
</tr>
<tr>
<td>Supplied calibration (on shipping box)</td>
</tr>
</tbody>
</table>

#### Physical characteristics

<table>
<thead>
<tr>
<th>Case material</th>
<th>Stainless, type 304</th>
</tr>
</thead>
<tbody>
<tr>
<td>Electrical, connections</td>
<td>6-pin, 12-48 uns threaded receptacle (mates to Endevco model 3023B-30 supplied)</td>
</tr>
<tr>
<td>Mounting/torque</td>
<td>Provision for 10-32 UNC x 1/8&quot; stud. Mounting torque 18 lbf-in (2nm)</td>
</tr>
<tr>
<td>Mass</td>
<td>40 grams (cable weighs 18 grams/meter)</td>
</tr>
</tbody>
</table>

#### Environmental limits

- **Static**
  - 20 000 g
- **Sinusoidal/random vibration**
  - 100 pk g, 20 - 2000 Hz/60 g rms, 20 - 2000 Hz
- **Shock (half-sine pulse)**
  - 5000 g, 150 μsec or longer for the -2 and -10, 10 000 g, 80 μsec or longer for the -30 and -100
- **Zero shift**
  - 0.1% FSO typical at 5000 g
- **Temperature**
  - -65°F to +250°F (-55°C to +121°C)
  - -100°F to +300°F (-73°C to +150°C)
- **Humidity/altitude**
  - Unaffected. Unit is hermetically sealed.
- **ESD sensitivity**
  - Unit meets Class 2 requirements of MIL-STD-883, method 3015

#### Physical characteristics

- **Sensitivity**
0.034 μV/g Hz^-1/2
0.027 μV/g Hz^-1/2
0.018 μV/g Hz^-1/2
0.010 μV/g Hz^-1/2
0.006 μV/g Hz^-1/2

- **Maximum transverse sensitivity**
0.1% of sensitivity
0.2% of sensitivity
0.3% of sensitivity
0.4% of sensitivity
0.5% of sensitivity

#### Notes:
1. Reference frequency is 5 Hz on the 2 g range, 100 Hz for -10, -30, -100
2. Over the excitation range 10 to 0.05 Vdc
3. Full scale output (FSO) is nominally 4 volts
4. 1% is typical, 1% maximum available on special order
5. Threshold = Max. residual noise; 0.5 to 100 Hz
6. % Increase = % of sensitivity
7. Per ISA 37.2 at 250 microstrain
8. At 100 Gausses, 60 Hz
9. Current drain increases slightly with increasing excitation; typical change is +1.06 mA per volt from 8.5 to 30.0 Vdc.
10. Maintain high levels of precision and accuracy using Endevco’s factory calibration services. Call Endevco’s inside sales force at 800-982-6732 for recommended intervals, pricing and turn-around time for these services as well as for quotations on our standard products.

#### Included accessories

- 3022A-30
- 10' cable assembly
- 92981-12
- 10-32 mounting stud, hex socket

#### Optional accessories

- 2981-3
- 10-32 adaptor stud, slot
- 2981-4
- M5 x 0.8 adaptor stud
Strain Gauges
The four strain gauges used for the raking damage experiments are general use strain gauges, with a gauge length of 6 mm. The strain gauges are produced by Tokyo Sokki Kenkyujo Co., Ltd. (type: FLA-6-350-11). The strain gauges are connected to the data acquisition system (Dewetron) with a quarter bridge connection.

Figure G.3: Strain gauges FLA-6-350-11.

Figure G.4: Test data of the strain gauges.
DEWETRON
The DEWETRON is the data acquisition system that is used for the raking damage experiments. The model is a DEWE-50-USB2-8, which is an 8 channel data acquisition system that can be connected to a PC via a USB connection. The specification sheet is attached to this appendix after this page.

The DEWETRON is used in conjunction with the DEWESOFT 7.1.1 software package.
USB Devices with 24 bit Resolution

- Isolated DAQP modules
- Easy to install on your computer
- 24 bit resolution
- 204.8 kS/s per channel, simultaneous sampling
- Synchronous CAN interfaces and counter/digital inputs
- DEWESoft-7-SE and OPT-CAN included

Add your choice of signal conditioning, A/D board(s) and software to complete these systems

<table>
<thead>
<tr>
<th>Specifications</th>
<th>DEWE-50-USB2-8</th>
</tr>
</thead>
<tbody>
<tr>
<td><strong>Analog input</strong></td>
<td></td>
</tr>
<tr>
<td>Number of channels</td>
<td>8 (simultaneously sampled)</td>
</tr>
<tr>
<td>Measured values</td>
<td>According installed DAQP modules</td>
</tr>
<tr>
<td><strong>Internal A/D system</strong></td>
<td></td>
</tr>
<tr>
<td>Resolution</td>
<td>24 bit</td>
</tr>
<tr>
<td>Type of ADC</td>
<td>Sigma-Delta</td>
</tr>
<tr>
<td>Sampling rate</td>
<td>204.8 kS/s</td>
</tr>
<tr>
<td>-3 dB bandwidth</td>
<td>76 kHz @ 204.8 kS/s (consider possible limit of DAQP module)</td>
</tr>
<tr>
<td>Accuracy</td>
<td>±0.1 % of range, ±0.5 mV</td>
</tr>
<tr>
<td>Signal to noise @ fs&lt;1000 Hz</td>
<td>&lt; 100 dB</td>
</tr>
<tr>
<td>Crosstalk</td>
<td>&lt; 100 dB</td>
</tr>
<tr>
<td><strong>Counter/Digital inputs</strong></td>
<td></td>
</tr>
<tr>
<td>Number of channels</td>
<td>2 counters or 6 digital inputs (per software each counter can be selected to be 3x digital input)</td>
</tr>
<tr>
<td>Counter modes</td>
<td>Event counting, encoder input, period, pulsewidth, duty cycle, frequency measurement</td>
</tr>
<tr>
<td>Resolution</td>
<td>32 bit</td>
</tr>
<tr>
<td>Time base</td>
<td>102.4 MHz</td>
</tr>
<tr>
<td>Signal levels</td>
<td>TTL/CMOS</td>
</tr>
<tr>
<td>Input voltage protection</td>
<td>30 V</td>
</tr>
<tr>
<td><strong>CAN inputs</strong></td>
<td></td>
</tr>
<tr>
<td>Number of channels</td>
<td>2</td>
</tr>
<tr>
<td>Specification</td>
<td>CAN 2.0B, up to 1 MBaud</td>
</tr>
<tr>
<td>Physical layer</td>
<td>High speed</td>
</tr>
</tbody>
</table>

**Environmental**
- Operating temperature: 0 to 50°C
- Storage temperature: -20 to 70°C
- Relative humidity: 95 % non condensing @ 60°C
- Vibration: tbd
- Shock: tbd

**Processing**
- System: Requires PC based system with DEWESoft software
- Interface: USB 2.0

**Power requirements**
- Supply voltage (max.): 10 to 36 Vdc
- Typical power consumption: Typ. 20 W (5 W internal A/D system + DAQP modules)

**Physical**
- Dimensions (L x W x H): 230 x 181 x 104 mm (9.06 x 7.13 x 4.09 in.)
- Weight: Typ. 3 kg (2.5 kg + DAQP modules) 6.6 lb. (5.5 lb. + DAQP modules)

**Software**
- Displays: Recorder, Scope, FFT, 3D Waterfall FFT, Octave, ...
- Triggers: Edge, Filtered Edge, Window, Pulsewidth, Slope, FFT, ...
- Online standard mathematics: Formula editor, FIR-, IIR-, FFT-filter, basic statistics, reference curve
- Online special mathematics: Human Body Vibration, Order Tracking, Rotational & Torsional Vibration, Sound Level, Frequency Response Function

**System options**
- 50-8-OUT-5: 8 BNC connectors on back panel, ±5 V output of DAQx-modules
- 50-8-SYNC: Synchronization option for two DEWE-50-USB2-8. Allows using max. two units as a 16 channel system, synchronization cable 50-8-CBL-SYNC-x needs to be ordered additionally.
Analog Input

The internal A/D system offers eight analog inputs, each has its own sigma-delta A/D converter and is sampled at up to 204.8 kS/s at 24 bit resolution. Anti-aliasing filters are included for each channel.

The DEWE-50-USB2-8 offers eight slots for high performance DAQP isolated signal conditioning modules. Thus any analog sensor can be connected.

Counter/Digital Input

There are Lemo sockets where each can either be used as one counter/encoder input or as three digital inputs – this is a software selection and can be set individually for each socket. Thanks to the special DEWETRON technology, the counter/digital inputs are acquired absolutely synchronously to the analog channels. DEWETRON counters are able to perform

- Basic counting
- Gated counting
- Up/down counting
- Duty cycle
- Frequency measurement
- Pulse width measurement
- Period time measurement
- Two pulse edge separation

CAN Interface

There are two high speed CAN interfaces which are able to acquire data from vehicle CAN – or vehicle OBDII interface – as well as from any sensor outputting CAN data.

Alternatively DEWETRON CPAD2 modules can be connected to a CAN interface to acquire quasi-static thermocouple, RTD, voltage or current signals.

SYNC Interface

A SYNC interface enables combination of two units to a 16 channel system.
High Speed Cameras

The four high speed cameras that are used for the raking damage experiments are two sets of cameras produced by IDT vision:

- model NX4-S3 (2x)
- model Y4-S3 (2x)

The specification sheets of these two models are attached to this appendix after this page. The data acquisition was done at a frame rate of 2500 fps, using the ARAMIS Professional software by GOM. The same software was also used to calibrate the 3D set-up of the cameras.
3.4. Nx-series

With 6000 ISO Mono 2000 ISO Color sensitivity, 30-bit pixel depth (color), and High G resistance to shock, the NX-Series is well suited for on-board crash testing, where space is at a premium.

**NX4**: three speed grades allow customized camera performance. Crash-testing, especially in vehicle, is the quintessential NX4 application. The cameras’ compact design allows for use in tight environments or when attached to a bore-scope.

**NX5**: optimally designed for resolution, the 7 micron array of 2336 x 1728 pixels, powers the 730 fps. This sensor array benefits from improved quantum efficiency from its predecessor to reduce noise without sacrificing light sensitivity or bit depth. This makes the camera well-suited for situations requiring light weight and superb resolving power, such as component testing and microscope imaging. Special opportunities for the NX5 exist in broadcast and cinema, when the small size combined with the 4.0 megapixel resolution provides high definition imaging formats. Two cameras can be used as a compact stereoscopic assembly for 3D applications.

**NX8**: with a maximum resolution of 1600 x 1200, the NX8 can record up to 4,000 fps. Combine this with its extremely compact size and High G resistance to shock, it is well-suited for on-board crash testing, where space is at a premium.
4.1.5. NX / NX-Air series

4.1.5.1. Common specifications

<table>
<thead>
<tr>
<th>Specification</th>
<th>All models</th>
</tr>
</thead>
<tbody>
<tr>
<td><strong>Power requirement</strong></td>
<td>12 VDC (N, NR, NX)</td>
</tr>
<tr>
<td></td>
<td>18-48 VDC (NXA)</td>
</tr>
<tr>
<td><strong>Operating temperature</strong></td>
<td>-40 º to +50º C (-40 to +122º F)</td>
</tr>
<tr>
<td><strong>Memory/DRAM</strong></td>
<td>Internal (1.25, 3, 4, 5 GB)</td>
</tr>
<tr>
<td><strong>Sensor type</strong></td>
<td>CMOS-proprietary</td>
</tr>
<tr>
<td><strong>Pixel depth</strong></td>
<td>10 bit</td>
</tr>
<tr>
<td><strong>Triggers</strong></td>
<td>TTL/switch closure</td>
</tr>
<tr>
<td><strong>Sync Input</strong></td>
<td>Phase-lock TTL</td>
</tr>
<tr>
<td><strong>Sync Output</strong></td>
<td>Frame sync/Strobe</td>
</tr>
<tr>
<td><strong>IRIG</strong></td>
<td>Optional</td>
</tr>
<tr>
<td><strong>GPS Time Code</strong></td>
<td>Optional</td>
</tr>
<tr>
<td><strong>HDMI/SDI</strong></td>
<td>N/A</td>
</tr>
<tr>
<td><strong>WiFi module</strong></td>
<td>Optional</td>
</tr>
<tr>
<td><strong>Communication</strong></td>
<td>Ethernet (100-1000 BaseT)</td>
</tr>
<tr>
<td><strong>Approximate size</strong></td>
<td>See mechanical</td>
</tr>
<tr>
<td><strong>Approximate weight</strong></td>
<td>0.2 kg / 0.5 lbs (NX-NXT)</td>
</tr>
<tr>
<td></td>
<td>0.6 kg / 1.5 lbs (NXA)</td>
</tr>
<tr>
<td><strong>Shock Rating</strong></td>
<td>200G – all axes</td>
</tr>
<tr>
<td><strong>Vibration Rating</strong></td>
<td>40G – all axes</td>
</tr>
<tr>
<td><strong>Battery powered operation time</strong></td>
<td>Operation and backup up to 2 hours</td>
</tr>
<tr>
<td><strong>Lens Mount</strong></td>
<td>C-mount standard, F/PL optional</td>
</tr>
</tbody>
</table>
### 4.1.5.2. NX3 (NR3, N3 except weight, size and memory)

<table>
<thead>
<tr>
<th></th>
<th>NX3-S1</th>
<th>NX3-S2</th>
<th>NX3-S3</th>
<th>NX3-S4</th>
</tr>
</thead>
<tbody>
<tr>
<td>Max fps @ max res</td>
<td>500 @ 1280x1024</td>
<td>1030 @ 1280x1024</td>
<td>2550 fps @ 1280x1024</td>
<td>3800 fps @ 1280x1024</td>
</tr>
<tr>
<td>Minimum exposure time</td>
<td>1 µs</td>
<td>1 µs</td>
<td>1 µs</td>
<td>1 µs</td>
</tr>
<tr>
<td>Sensitivity ISO (mono)</td>
<td>3000</td>
<td>3000</td>
<td>6000</td>
<td>6000</td>
</tr>
<tr>
<td>Sensitivity ISO (color)</td>
<td>1000</td>
<td>1000</td>
<td>2000</td>
<td>2000</td>
</tr>
<tr>
<td>CFA Pattern</td>
<td>BGGR</td>
<td>BGGR</td>
<td>GBRG</td>
<td>GBRG</td>
</tr>
<tr>
<td>Sensor Size</td>
<td>15.4 x 12.3 mm</td>
<td>15.4 x 12.3 mm</td>
<td>13.9 x 11.2 mm</td>
<td>13.9 x 11.2 mm</td>
</tr>
<tr>
<td>Array Size</td>
<td>1.3 Megapixels</td>
<td>1.3 Megapixels</td>
<td>1.3 Megapixel</td>
<td>1.3 Megapixel</td>
</tr>
<tr>
<td>Pixel Size</td>
<td>12x12 µm</td>
<td>12x12 µm</td>
<td>10.85 x 10.85 µm</td>
<td>10.85 x 10.85 µm</td>
</tr>
<tr>
<td>Aspect ratio</td>
<td>5:4</td>
<td>5:4</td>
<td>5:4</td>
<td>5:4</td>
</tr>
</tbody>
</table>

### 4.1.5.3. NX4 (NR4, N4 except weight, size and memory)

<table>
<thead>
<tr>
<th></th>
<th>NX4-S1</th>
<th>NX4-S2</th>
<th>NX4-S3</th>
</tr>
</thead>
<tbody>
<tr>
<td>Max fps @ max res</td>
<td>1000 @ 1024x1024</td>
<td>2000 @ 1024x1024</td>
<td>3000 @ 1024x1024</td>
</tr>
<tr>
<td>Minimum exposure time</td>
<td>1 µs</td>
<td>1 µs</td>
<td>1 µs</td>
</tr>
<tr>
<td>Sensitivity ISO (mono)</td>
<td>6000</td>
<td>6000</td>
<td>6000</td>
</tr>
<tr>
<td>CFA Pattern</td>
<td>GBRG</td>
<td>GBRG</td>
<td>GBRG</td>
</tr>
<tr>
<td>Sensor Size</td>
<td>13.9 x 13.9 mm</td>
<td>13.9 x 13.9 mm</td>
<td>13.9 x 13.9 mm</td>
</tr>
<tr>
<td>Array Size</td>
<td>1 Megapixels</td>
<td>1 Megapixel</td>
<td>1 Megapixel</td>
</tr>
<tr>
<td>Pixel Size</td>
<td>13.68 x 13.68 µm</td>
<td>13.68 x 13.68 µm</td>
<td>13.68 x 13.68 µm</td>
</tr>
<tr>
<td>Aspect ratio</td>
<td>1:1</td>
<td>1:1</td>
<td>1:1</td>
</tr>
</tbody>
</table>
3.6. Y Series

With extremely low noise levels and high light sensitivity, Y3 models perform well for those whose needs encompass both detail and speed, such as product testing automobiles and engines.

The Y4 is the most versatile camera system, useful in production and research and development environments. This camera system can be operated in an extended dynamic range (EDR) mode to produce either 11-bit or 12-bit images.

The Y5 surpasses high definition with a 4.0 megapixel sensor capable of 730 fps at full resolution. Variable lens mounts support cinema lenses, as well as those built for Nikon and Canon SLRs. The HDMI output ensures that the image can be monitored at the full resolution before, during and after recording.

New to the Y Series, the Y7 PIV model introduces a universal integrated timing interface. This allows for synchronization of any illumination sources such as lasers or LEDs. Together with our standard 200-nanosecond inter-frame time, the camera is perfectly suited for PIV researchers as it decreases PIV system size and cost.

Y8 is our latest breakthrough in Y Series development. Utilizing the next generation of sensor technology, the Y8 provides a maximum resolution of 1600 x 1200 and comes in three speed grades with the capability of recording up to 9,300 fps offering uncompromised speed and performance.
### 4.1.2.2. Y3

<table>
<thead>
<tr>
<th></th>
<th>Y3-S1</th>
<th>Y3-S2</th>
</tr>
</thead>
<tbody>
<tr>
<td>Max fps @ max res</td>
<td>3750 @ 1280x1024</td>
<td>6030 @ 1280x1024</td>
</tr>
<tr>
<td>Minimum exposure time</td>
<td>1 µs</td>
<td>1 µs</td>
</tr>
<tr>
<td>Sensitivity ISO (mono)</td>
<td>6000</td>
<td>6000</td>
</tr>
<tr>
<td>Sensitivity ISO (color)</td>
<td>2000</td>
<td>2000</td>
</tr>
<tr>
<td>CFA Pattern</td>
<td>GBRG</td>
<td>GBRG</td>
</tr>
<tr>
<td>Sensor Size</td>
<td>13.9 x 11.2 mm</td>
<td>13.9 x 11.2 mm</td>
</tr>
<tr>
<td>Array Size</td>
<td>1.3 Megapixel</td>
<td>1.3 megapixel</td>
</tr>
<tr>
<td>Pixel Size</td>
<td>10.85 x 10.85 µm</td>
<td>10.85 x 10.85 µm</td>
</tr>
<tr>
<td>Aspect ratio</td>
<td>5:4</td>
<td>5:4</td>
</tr>
<tr>
<td>Pixel Depth</td>
<td>10-bit</td>
<td>10-bit</td>
</tr>
</tbody>
</table>

### 4.1.2.3. Y4

<table>
<thead>
<tr>
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<th>Y4-S1</th>
<th>Y4-S2</th>
<th>Y4-S3</th>
</tr>
</thead>
<tbody>
<tr>
<td>Max fps @ max res</td>
<td>3000 @ 1024x1024</td>
<td>5100 @ 1024x1024</td>
<td>7000 @ 1024x1024</td>
</tr>
<tr>
<td>Minimum exposure time</td>
<td>1 µs</td>
<td>1 µs</td>
<td>1 µs</td>
</tr>
<tr>
<td>Sensitivity ISO (mono)</td>
<td>6000</td>
<td>6000</td>
<td>6000</td>
</tr>
<tr>
<td>CFA Pattern</td>
<td>GBRG</td>
<td>GBRG</td>
<td>GBRG</td>
</tr>
<tr>
<td>Sensor Size</td>
<td>13.9 x 13.9 mm</td>
<td>13.9 x 13.9 mm</td>
<td>13.9 x 13.9 mm</td>
</tr>
<tr>
<td>Array Size</td>
<td>1 Megapixel</td>
<td>1 Megapixel</td>
<td>1 megapixel</td>
</tr>
<tr>
<td>Pixel Size</td>
<td>13.68 x 13.68 µm</td>
<td>13.68 x 13.68 µm</td>
<td>13.68 x 13.68 µm</td>
</tr>
<tr>
<td>Aspect ratio</td>
<td>1:1</td>
<td>1:1</td>
<td>1:1</td>
</tr>
<tr>
<td>Pixel Depth</td>
<td>10-bit</td>
<td>10-bit</td>
<td>10-bit</td>
</tr>
</tbody>
</table>

### 4.1.2.4. Y5

<table>
<thead>
<tr>
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<th>Y5</th>
</tr>
</thead>
<tbody>
<tr>
<td>Max fps @ max resolution</td>
<td>730 fps @ 2560x1920</td>
</tr>
<tr>
<td>Minimum exposure time</td>
<td>1 µs</td>
</tr>
<tr>
<td>Sensitivity ISO (mono)</td>
<td>3000</td>
</tr>
<tr>
<td>Sensitivity ISO (color)</td>
<td>1000</td>
</tr>
<tr>
<td>CFA Pattern</td>
<td>GRBG</td>
</tr>
<tr>
<td>Sensor Size</td>
<td>16.4 x 12.1 mm</td>
</tr>
<tr>
<td>Array Size</td>
<td>5 Megapixels</td>
</tr>
<tr>
<td>Pixel Size</td>
<td>7 x 7 µm</td>
</tr>
<tr>
<td>Aspect ratio</td>
<td>4:3</td>
</tr>
<tr>
<td>Pixel Depth</td>
<td>10-bit</td>
</tr>
</tbody>
</table>
Infra-Red Camera
The infra-red camera used in the experiment is the FLIR A325sc with data acquisition software FLIR ResearchIR. The camera captures the IR images with a maximum frame rate of 60 fps, using an uncooled microbolometer that has $320 \times 240$ pixels. The specification sheet is attached to this appendix after this page.
FLIR A325sc
Thermal imaging camera for real-time analysis

EXCELLENT IMAGE QUALITY AND THERMAL SENSITIVITY
FLIR A325sc is equipped with an uncooled Vanadium Oxide (VOx) microbolometer detector that produces thermal images of 320 x 240 Pixels. These pixels generate crisp and clear detailed images that are easy to interpret with high accuracy. The FLIR A325sc will make temperature differences as small as 50 mK clearly visible.

FAST DATA TRANSFER
FLIR A325sc comes with a RJ-45 Gigabit Ethernet connection which supplies 14-bit 320 x 240 images at rates as high as 60 Hz.

GIGE VISION™ STANDARD COMPATIBILITY
GigE Vision allows fast image transfer using low cost standard cables up to 100 meters. With GigE Vision, hardware and software from different vendors can integrate seamlessly over gigabit ethernet connections.

GENICAM™ PROTOCOL SUPPORT
GenICam creates a common application programming interface (API) for cameras regardless of the interface technology or features implemented. Because the API for GenICam cameras will always be the same, cameras like the A325sc can be easily integrated into third party software.

SOFTWARE
FLIR A325sc camera works seamlessly with FLIR ResearchIR Max software enabling intuitive viewing, recording and advanced processing of the thermal data provided by the camera. A Software Developers Kit (SDK) is optionally available.

MATHWORKS® MATLAB
Control and capture data directly into MathWorks® Matlab software for advanced image analysis and processing.

KEY FEATURES
• Uncooled microbolometer: 320 x 240 pixels
• Gigabit ethernet interface
• Close-up and telephoto lenses available
• ResearchIR max software included
• Matlab compatible

www.flir.com
Imaging Specifications

<table>
<thead>
<tr>
<th>Detector</th>
<th>FLIR A325sc</th>
</tr>
</thead>
<tbody>
<tr>
<td>Detector Type</td>
<td>Uncooled Microbolometer</td>
</tr>
<tr>
<td>Spectral Range</td>
<td>7.5 – 13.0 μm</td>
</tr>
<tr>
<td>Resolution</td>
<td>320 x 240</td>
</tr>
<tr>
<td>Detector Pitch</td>
<td>25 μm</td>
</tr>
<tr>
<td>NETD</td>
<td>&lt;50 mK</td>
</tr>
</tbody>
</table>

Electronics / Imaging

| Time Constant     | <12 ms                |
| Frame Rate        | 60 Hz                 |
| Dynamic Range     | 14-bit                |
| Digital Data Streaming | Gigabit Ethernet (60 Hz) |
| Command & Control | Gigabit Ethernet       |

Measurement

| Standard Temperature Range | -20°C to 120°C (-4°F to 248°F) |
| Optional Temperature Range | Up to 2,000°C (3,632°F) |
| Accuracy                  | ±2°C or ±2% of Reading |

Optics

| Camera f/#          | f/1.3               |
| Integrated Lens     | 18 mm (25°)         |
| Available Lenses    | 76 mm (6°), 30 mm (15°), 10 mm (45°), 4 mm (90°) |
| Close-up Lenses / Microscopes | Close-up 25 μm, 50 μm, 100 μm |
| Focus               | Automatic or Manual (Motorized) |

Image Presentation

| Digital Data Via PC | Using ResearchIR Software |

General

| Operating Temperature Range | -15°C to 60°C (5°F to 122°F) |
| Storage Temperature Range  | -40°C to 70°C (-40°F to 158°F) |
| Encapsulation              | IP 40 (IEC 60529) |
| Bump / Vibration           | 25 g (IEC 60068-2-29) / 2 g (IEC 60068-2-6) |
| Power                       | 12/24 VDC, 24 W Absolute Max. |
| Weight w/Lens              | 0.7 kg (1.54 lb) |
| Size (L x W x H) w/Lens    | 170 x 70 x 70 mm (6.7 x 2.8 x 2.8 in) |
| Mounting                   | ¼"-20 (on three sides), 2 x M4 (on three sides) |

Specifications are subject to change without notice
©Copyright 2014, FLIR Systems, Inc. All other brand and product names are trademarks of their respective owners. The images displayed may not be representative of the actual resolution of the camera shown. Images for illustrative purposes only. (Edited 08/14)

www.flir.com
This chapter presents the DIC strain results of the raking damage experiments. Table H.1 shows an overview of the various strains that are reported in this appendix. The rupture strains for drop 1 (specimen 3) could not be used and are therefore not reported.

Table H.1: An overview of the strains measured with DIC, that are reported in this appendix, for the corresponding specimen numbers of the raking damage experiments.

<table>
<thead>
<tr>
<th>&quot;Fw&quot;</th>
<th>&quot;Aft&quot;</th>
</tr>
</thead>
<tbody>
<tr>
<td>Experiment 1</td>
<td>1 - Von Mises strain (drop 1-1 only)</td>
</tr>
<tr>
<td>Experiment 2</td>
<td>2 - all strains (drop 2-1 &amp; 2-2 combined)</td>
</tr>
<tr>
<td>Experiment 3</td>
<td>3 - all strains</td>
</tr>
<tr>
<td>Experiment 4</td>
<td>4 - Von Mises strain (drop 2-1 only)</td>
</tr>
<tr>
<td></td>
<td>5 - Von Mises strain</td>
</tr>
<tr>
<td></td>
<td>6 - all strains</td>
</tr>
<tr>
<td></td>
<td>7 - Von Mises strain</td>
</tr>
</tbody>
</table>

For the "Fw" plates - the plate that ruptures in each experiment - all relevant strains are reported at the moment briefly before rupture occurs. This is the same moment in time that is used to determine the rupture strains reported in section 5.1.

For the "Aft" plates, only the Von Mises strains are reported, which are the strains that are used for the IR minus DIC measuring method that was introduced in section 5.3.3 and is elaborated upon in appendix K. These strains are taken at the end of the drop test (the last frame of the high speed footage used for the DIC). For Experiment 1 and Experiment 2 only the first drop test is used for the IR minus DIC method.
Experiments 1

Figure H.1: Von Mises strain of drop 1-1 (specimen 1), at the end of the drop test.
Experiment 2

Figure H.2: Strains briefly before rupture for experiment 2 (specimen 2). Drop 2-1 & drop 2-2 are combined.
Figure H.3: Von Mises strain of drop 2-1 (specimen 4), at the end of the drop test.
Experiment 3

Figure H.4: Strains briefly before rupture for experiment 3 (specimen 6).
Figure H.5: Von Mises strain of drop 3 (specimen 5), at the end of the drop test.
Experiment 4

Figure H.6: Strains briefly before rupture for experiment 4 (specimen 8).
Figure H.7: Von Mises strain of drop 3 (specimen 7), at the end of the drop test.
This appendix contains the thickness measurements that were performed on the specimens of the raking damage experiments. Thickness measurements were performed both before and after the raking damage experiments. Both measurements are presented in the pictures in this appendix. The measurements before were done with an ultrasonic thickness measurement device. The thickness measurements after the experiments were performed both with a ball-point micrometer as with the ultrasonic thickness measurement device. The thickness measurements close to the crack were done with the micrometer and the thickness measurements far away from the crack were done with the ultrasonic thickness gauge (see appendix I).

![Ultrasonic thickness gauge and Ball-point micrometer.](image)

Figure I.1: Measurements devices used to measure the thicknesses of the specimens.

The thicknesses measured after the experiment were converted to through-thickness strain values that were used to check validity and accuracy of the DIC strain measurements. Table N.2 gives the results of the comparison with DIC. The comparison should be done with pt.2 and pt.3 from the DIC measurements as these correspond with the locations the (approximate) minimum thickness was measured with the ball-point micrometer.
Table I.1: Engineering Strains at failure from DIC and ball-micrometer thickness measurements of the cracked specimens.

<table>
<thead>
<tr>
<th></th>
<th>$\epsilon_3$ pt.2/3</th>
<th>measured $\epsilon_3$</th>
</tr>
</thead>
<tbody>
<tr>
<td>Experiment 2</td>
<td>-0.37</td>
<td>-0.38</td>
</tr>
<tr>
<td>Experiment 3</td>
<td>-0.32</td>
<td>-0.30</td>
</tr>
<tr>
<td>Experiment 4</td>
<td>-0.28</td>
<td>-0.33</td>
</tr>
</tbody>
</table>

The pages hereafter present the thickness measurements visualised on a picture of each of the specimens after the raking damage experiments. Table I.2 reports which specimen was used for each of the raking damage experiments.

Table I.2: Specimen numbers that were used for each of the raking damage experiments. "Fw" was the specimen that was hit first due to a slight misalignment of the indenter. The "Fw" specimen was also the only specimen that ruptured in each experiment.

<table>
<thead>
<tr>
<th></th>
<th>&quot;Fw&quot;</th>
<th>&quot;Aft&quot;</th>
</tr>
</thead>
<tbody>
<tr>
<td>Experiment 1</td>
<td>3</td>
<td>1</td>
</tr>
<tr>
<td>Experiment 2</td>
<td>2</td>
<td>4</td>
</tr>
<tr>
<td>Experiment 3</td>
<td>6</td>
<td>5</td>
</tr>
<tr>
<td>Experiment 4</td>
<td>8</td>
<td>7</td>
</tr>
</tbody>
</table>
Original Thickness

Thickness after experiment
Original Thickness

Thickness after experiment
Original Thickness

Thickness after experiment
This appendix contains the temperature results as recorded by the IR camera. Table J.2 shows an overview of all the IR results that are presented in this appendix. The temperature difference is presented for the first drop test of each experiment. This $\Delta C$ is used for computation of the energy dissipation in appendix K. For the other drop tests, only the maximum temperature is reported in this appendix.

The camera recorded the IR images with 60 fps, and the settings of the temperature calculation of the IR camera are shown in figure J.1.

![Figure J.1: Settings in the IR software for calculation of the temperatures.](image)
Calibration
The IR camera measurements have been calibrated using the simple set-up with two calibrated thermocouples and a heat gun shown in figure J.2. The calibration results are shown in table J.1.

Figure J.2: Photo of the IR calibration set-up.

Table J.1: Results of the calibration of the IR camera. The tabulated temperatures are the average temperatures of the two spot measurements of the thermocouples.

<table>
<thead>
<tr>
<th>Thermo Couple</th>
<th>IR camera</th>
</tr>
</thead>
<tbody>
<tr>
<td>01 26.7</td>
<td>26.5</td>
</tr>
<tr>
<td>02 17.8</td>
<td>18.2</td>
</tr>
<tr>
<td>03 43.5</td>
<td>44.1</td>
</tr>
<tr>
<td>04 22.6</td>
<td>22.8</td>
</tr>
</tbody>
</table>

Results
Table J.2: Overview of the IR results as presented in this appendix. The IR camera was only used for the "Aft" specimens. The temperature difference is presented for the first drop test of each experiment. The maximum temperature is presented for the other drop tests.

<table>
<thead>
<tr>
<th>&quot;Fw&quot;</th>
<th>&quot;Aft&quot;</th>
<th>ΔT</th>
<th>(maximum) T</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td></td>
<td>1</td>
<td>Drop 1-1, 1-2, 1-3 &amp; 1-4</td>
</tr>
<tr>
<td>4</td>
<td></td>
<td>5</td>
<td>Drop 2-1, 2-2</td>
</tr>
<tr>
<td>5</td>
<td></td>
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<td>7</td>
<td></td>
<td></td>
<td>Drop 4</td>
</tr>
</tbody>
</table>
When looking at the IR images in this appendix, keep in mind that the pictures are rotated 90 degrees, and that the top of the specimen is on the left side of the picture (i.e. the direction in which the drop mass falls is from left to right).

**Experiment 1**

![Figure J.3: Temperature difference (dC) of drop 1-1, recorded with the IR camera. The dT is between before the drop and the maximum recorded temperature.](image1)

![Figure J.4: The maximum recorded temperature of drop 1-2.](image2)
Figure J.5: The maximum recorded temperature of drop 1-3.

Figure J.6: The maximum recorded temperature of drop 1-4.
Experiment 2

Figure J.7: Temperature difference (dC) of drop 2-1, recorded with the IR camera. The dT is between before the drop and the maximum recorded temperature.

Figure J.8: The maximum recorded temperature of drop 2-2.
Experiment 3

Figure J.9: Temperature difference (dC) of drop 3, recorded with the IR camera. The dT is between before the drop and the maximum recorded temperature.

Experiment 4

Figure J.10: Temperature difference (dC) of drop 4, recorded with the IR camera. The dT is between before the drop and the maximum recorded temperature.
IR and DIC - friction energy measurements and calculations

- Explanation of idea
- Method of calculation and assumptions
- Nice pictures
- Some results
- Small discussion

The aim of this part of the raking damage research is to quantify the amount of energy dissipation through friction in an independent way. This is done through measurements with the Infra-Red (IR) camera, which are combined with the DIC measurements (appendix K). The IR camera provides an optical temperature measurement of 320 x 240 pixels, with each pixel being an independent temperature measurement. The idea is that the IR camera captures the total amount of energy dissipation, as all friction energy is dissipated in the form of heat. The DIC measurements capture the strains, from which the energy dissipation due to plastic deformation can be calculated. Now the energy dissipation due to plastic deformation is known, and from the IR measurements the total energy dissipation is known.

\[ E_{\text{diss,friction}} = E_{\text{diss,total}} - E_{\text{diss,plastic}} = E_{\text{IR}} - E_{\text{DIC}} \]  

(K.1)

With these assumptions the energy dissipations can be calculated from the IR and DIC measurements using equation (K.1). The separate contributions of equation (K.1) are calculated with equation (K.2) and equation (K.3).

\[ E_{\text{DIC}} = \varepsilon_{VM} \cdot V \cdot \sigma_{\text{yield}} \]  

(K.2)

\[ E_{\text{IR}} = M \cdot \Delta T \cdot c_{\text{thermal}} \]  

(K.3)

Equation (K.2) and equation (K.3) are calculated using constant values for the yield stress (\(\sigma_{\text{yield}}\)) and the specific heat (\(c_{\text{thermal}}\)) shown in table K.1.

| \(\sigma_{\text{yield}}\) | 303 MPa (tensile testing, section 5.1.1 & appendix M) |
| \(c_{\text{thermal}}\) | \(434 \text{ J kg}^{-1} \text{K}^{-1}\) (using [17] table A.1 - AISI 1010 steel) |

With the constants reported in table K.1, the energies in figure K.1 can be calculated.
The principle of calculating the energy contribution due to friction. The total energy dissipation is calculated using the IR images by equation (K.3). The energy dissipation due to the plastic deformation is calculated with the DIC strains (appendix H) by using equation (K.2).

The nice pictures in figure K.2 and figure K.3 show the energy density of one of the drop tests. Eventually the energy dissipation of the entire plate is to be determined, because this allows for comparison of the different contributions to the total energy dissipation. In order to determine the total energy dissipation the energy density is summed over the “measurement volume”. The results of this summation are presented in table K.2.
Table K.2: IR minus DIC energy results.

<table>
<thead>
<tr>
<th>IR energy</th>
<th>DIC energy</th>
<th>Friction energy</th>
</tr>
</thead>
<tbody>
<tr>
<td>Drop 1-1</td>
<td>13 kJ</td>
<td>7 kJ</td>
</tr>
<tr>
<td>Drop 2-1</td>
<td>19 kJ</td>
<td>12 kJ</td>
</tr>
<tr>
<td>Drop 3</td>
<td>27 kJ</td>
<td>18 kJ</td>
</tr>
<tr>
<td>Drop 4</td>
<td>25 kJ</td>
<td>18 kJ</td>
</tr>
</tbody>
</table>

The values of energy dissipation from table K.2 are not to be compared directly to the values determined in section 5.3.2 due to the following reasons:

- The measurement volume does not span the entire plate, only the region of interest.
- The IR camera only measured one of the two plates in each experiment.
- For experiment 1 and experiment 2 only the first drop test was used.

Table K.3: Comparison of dissipated energy through friction.

<table>
<thead>
<tr>
<th>IR minus DIC</th>
<th>Energy results from section 5.3</th>
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</thead>
<tbody>
<tr>
<td>$E_{IR}$</td>
<td>$E_{diff, total}$</td>
</tr>
<tr>
<td>Drop 1-1</td>
<td>13 kJ</td>
</tr>
<tr>
<td>Drop 2-1</td>
<td>19 kJ</td>
</tr>
<tr>
<td>Drop 3</td>
<td>27 kJ</td>
</tr>
<tr>
<td>Drop 4</td>
<td>25 kJ</td>
</tr>
</tbody>
</table>
Figure K.4: Top view of the IR minus DIC image of drop 1-1.
Figure K.5: Top view of the IR minus DIC image of drop 2-1.
Figure K.6: Top view of the IR minus DIC image of drop 3.
Figure K.7: Top view of the IR minus DIC image of drop 4.
Material Certificate of the Specimens
Material requirements and customer information

<table>
<thead>
<tr>
<th>Product:</th>
<th>Plate</th>
</tr>
</thead>
<tbody>
<tr>
<td>Steel standard and grade:</td>
<td>LR/A</td>
</tr>
<tr>
<td>Delivery condition:</td>
<td>As rolled (AR)</td>
</tr>
<tr>
<td>Customer name and address:</td>
<td>De Boer Staal B.V.</td>
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<tr>
<td></td>
<td>Postbus 17</td>
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<tr>
<td></td>
<td>Molenstraat 28</td>
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<tr>
<td></td>
<td>1911 DA Uitgeest</td>
</tr>
<tr>
<td></td>
<td>NETHERLANDS</td>
</tr>
</tbody>
</table>

Surface tolerance: EN 10163-2 B3
Length tolerance: EN 10029 Table 3
Width tolerance: EN 10029 Table 2
Thickness tolerance: EN 10029 Class B
Flatness tolerance: EN 10029 Table 4 Class N

Supplementary information: Fully Killed and Fine Grain

Visual examination and dimensional checking: Satisfactory. The results of tests performed are in compliance with the requirements.

Details of supplied materials dimensions, weights and pieces

<table>
<thead>
<tr>
<th>Heat/Slab No.</th>
<th>Plate No.</th>
<th>Item</th>
<th>Thickness (mm)</th>
<th>Width (mm)</th>
<th>Length (mm)</th>
<th>Pieces</th>
<th>Gross Weight (kg)</th>
<th>Hard Stamp</th>
<th>Stamp Location</th>
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<td>LR A</td>
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Third party inspection: Simeon Tulip
<table>
<thead>
<tr>
<th>Heat/Slab</th>
<th>Plate No.</th>
<th>Item</th>
<th>Thickness (mm)</th>
<th>Width (mm)</th>
<th>Length (mm)</th>
<th>Pieces</th>
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<tbody>
<tr>
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<td>LR A</td>
<td>Head</td>
<td>1670900</td>
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</table>

**Total:** 14 22,929
## Chemical composition (heat analysis)

<table>
<thead>
<tr>
<th>Heat No. (6d7)</th>
<th>C</th>
<th>Mn</th>
<th>Si</th>
<th>P</th>
<th>S</th>
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</tr>
</tbody>
</table>

### Supplementary information (C96)

CEV = C + Mn/6 + (Cr + Mo + V)/5 + (Ni + Cu)/15
1 = Basic Oxygen Steel, 2 = Electric Arc Furnace, 3 = Ladle Refined, 4 = Calcium Treated, 5 = Vacuum Degassed, 6 = Continuous Cast, 7 = Ingot
## Inspection Certificate

**Date of creation:** 16.04.2014  
**Certificate No.:** 018337  
**Order registration date:** 14.03.2014  
**Date of dispatch:** 15.04.2014

### Tensile testing

Tensile tests were performed in accordance with EN 1002/ISO 6992-1 with results as stated below:

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<th>Plate ID</th>
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<th>Loc.</th>
<th>Dir.</th>
<th>Yield MPa</th>
<th>Yield type</th>
<th>UTS Rm MPa</th>
<th>Elong. type</th>
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<td>T</td>
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<td>0.69</td>
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<td>H</td>
<td>T</td>
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<td>47</td>
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</tr>
</tbody>
</table>

**Supplementary Information (C09)**

- **Loc.:** (C01) H = head, T = tail  
- **Dir.:** (C02) T = transversal, L = longitudinal  
- **Shape:** (C16) Ø = round, R = rectangular  
- **Original gauge length:** 200 mm

---

**Third party inspection (C03)**  
**Inspection representative NLMK DanSteel A/S (A05)**

---

**Signature**

[Signature]

---

**Lloyd's Register EMEA**

---

**Copenhagen Office**
### Impact testing

Impact tests were performed in accordance with EN 10045/ISO 148-1 with results as stated below:

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<td>L</td>
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<td>H</td>
<td>L</td>
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<td>7966S-2-1</td>
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<td>CV</td>
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<td>H</td>
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<td>L</td>
<td>20</td>
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<td>75</td>
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<tr>
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<td>CV</td>
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<td>20</td>
<td>85</td>
<td>89</td>
<td>80</td>
<td>85</td>
</tr>
</tbody>
</table>

**Supplementary information:**

- **Position:**
  - (C01) 1 = surface, 2 = middle, 3 = 1/3 of thickness, 4 = 1/4 of thickness
- **Notch:**
  - (C40) CU = Charpy U-notch, CV = Charpy V-notch, CVA = Charpy V-notch (ASTM)
- **Loc.:**
  - (C21) H = head, T = tail
- **Dir.:**
  - (C22) T = transversal, L = longitudinal

---

**Lloyd's Register EMEA**

---

Third party inspection (203)

**LR**

Inspection representative NLMK DanSteel A/S (A05)

**Simeon Tulip**
Ship Steel or Steel for Ships, Boiler, Pressure Vessel and for welded Machinery Structures. We hereby certify that the Plates have been made by an approved process in accordance with, the rules of the stated Classification Society and the stated Steel Grade. Testing of the hereby certified materials has been carried out with satisfactory results in the presence of the Classification Society's Surveyor.
Tensile Test Data

Tensile tests are performed by Element Amsterdam (company) according to the international standard NEN-EN-ISO-6892-1:2016 [14]. This appendix presents the results of the tensile tests. In this appendix:

- Official tensile testing report by Element
- Stress - strain curves of the tensile tests.
TEST REPORT

DESTRUCTIVE TESTING OF WELDED PRINTED PLATE

WPS number
Item description: 6 Tensile samples
Material: S235 (according EN 10025-2)
Condition: As delivered
Testing in accordance with: Customer

DESTRUCTIVE TEST

TENSILE TEST

<table>
<thead>
<tr>
<th>Specimen</th>
<th>Size [mm]</th>
<th>Cross section [mm²]</th>
<th>Yield strength ReH [MPa]</th>
<th>Yield strength Rp0.2 [MPa]</th>
<th>Tensile strength Rm [MPa]</th>
<th>Elongation [%] After fracture A5</th>
<th>E-modulus [GPa]</th>
</tr>
</thead>
<tbody>
<tr>
<td>F20354-1</td>
<td>20.06x6.08</td>
<td>122.0</td>
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<td>456</td>
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<td>174</td>
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<td>32.5</td>
<td>190</td>
<td></td>
</tr>
</tbody>
</table>

CONCLUSIONS/REMARKS

No requirements available

Element Materials Technology

[Signature]

All information of the above object(s) have, as far as accessible and relevant, been verified by Element Materials Technology Amsterdam b.v (Element). Other information was provided by the purchaser. This information was verified as far as possible and has been copied into this report, unchanged. Element does not bear responsibility for the correctness of the submitted information. We hereby certify that the reported test data is correct and that the above object(s) was (were) tested/determined in accordance with purchaser's requirements and/or the above procedure(s) and/or code(s)/specification(s). On occasion a test is subcontracted by Element, the accreditation number of the subcontracted party is reported. Interpretations, opinions, conclusions and advice are partly based on the examination results and partly on information supplied by the purchaser. This report has legal value only when furnished with an authorized signature. If, upon reproduction, only part of this report is copied, Element will not bear any responsibility for content, purport and conclusions of that reproduction.
Tensile Test Data

Figure M.1: Tensile test measurement data - all six tensile tests.

Figure M.2: Tensile test measurement data - results of the three tests in the "main" direction (L)
Figure M.3: Tensile test measurement data - results of the three tests in the "perpendicular" direction (D)
True Stress-Strain Extrapolation -
Ballpoint Micrometer Measurements

Tensile tests were performed after the experiments (figure 5.5). The results are shown in figure 5.7 and an extensive report of the results and findings from the tensile testing can be found in appendix M. This appendix describes the method employed to derive a true stress-strain curve from the tensile test data and from measuring the fracture surface specimens.

In this appendix:

- Ball-point micrometer measurement procedure
- Measured thicknesses and widths of the fracture surfaces
- Strains derived from the measured fracture surfaces

![Figure N.1: Photo of the tensile test specimens after the test.](image)

All tensile test specimens, after testing, are shown in figure N.1. From this picture it can be seen that all tensile test specimen fractured near the centre of the parallel length (the testing area). This indicates that the tensile test results are valid and can be compared with one another.
All specimens in figure N.1 show signs of localisation (necking) near the fracture surface. Figure N.4 shows where to look for the necked region on the specimens. The necking behaviour, but mainly the 45 degree slip plane of the fracture surface which can be seen in figure N.2(a), are signs that ductile behaviour initiated failure. However, the centre of the fracture surface shows brittle fracture. This means that fracture initiation of the tensile test specimens can be classified as semi-ductile fracture.

Figure N.2: Close-up pictures of the fracture surface of one of the tensile test specimens.

From figure N.2(b) it can be seen that the cross-sectional area of the fracture surface can be represented by two trapezoids. Therefore, thicknesses are measured on the locations indicated in figure N.3.

Figure N.3: Locations on the cross section of the fracture surface where the thicknesses and the width are measured. Figure N.2(b) shows a picture of the actual cross section, and table N.1 reports the measured values for thicknesses and widths.
Figure N.4: Indication of the locations where thicknesses and widths of the fracture surface are measured on both the left and the right part of the broken specimen. Also the necked region of the broken specimen is indicated.

Table N.1: Ball-point micrometer measurement results of the tensile test specimen. The locations of the measurements are indicated in figure N.3 and figure N.4. The average of the left and the right part of the broken specimen are used to calculate the cross-sectional surface area.

<table>
<thead>
<tr>
<th></th>
<th>left</th>
<th>right</th>
<th>average</th>
</tr>
</thead>
<tbody>
<tr>
<td>D1</td>
<td>b</td>
<td>13.86</td>
<td>13.58</td>
</tr>
<tr>
<td></td>
<td>t1</td>
<td>3.94</td>
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<tr>
<td></td>
<td>t2</td>
<td>3.42</td>
<td>2.94</td>
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<td></td>
<td>t3</td>
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<td>3.85</td>
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<tr>
<td>D2</td>
<td>b</td>
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<td></td>
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<td></td>
<td>t1</td>
<td>3.94</td>
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</tr>
<tr>
<td>L1</td>
<td>b</td>
<td>13.93</td>
<td>13.94</td>
</tr>
<tr>
<td></td>
<td>t1</td>
<td>4.02</td>
<td>4.00</td>
</tr>
<tr>
<td></td>
<td>t2</td>
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<td>3.57</td>
</tr>
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<td></td>
<td>t3</td>
<td>4.08</td>
<td>3.92</td>
</tr>
<tr>
<td>L2</td>
<td>b</td>
<td>13.95</td>
<td>13.98</td>
</tr>
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<td></td>
<td>t1</td>
<td>4.15</td>
<td>4.00</td>
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<td></td>
<td>t2</td>
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<td></td>
<td>t3</td>
<td>4.08</td>
<td>3.98</td>
</tr>
<tr>
<td>L3</td>
<td>b</td>
<td>14.06</td>
<td>14.03</td>
</tr>
<tr>
<td></td>
<td>t1</td>
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</tr>
<tr>
<td></td>
<td>t3</td>
<td>4.18</td>
<td>4.10</td>
</tr>
</tbody>
</table>

From the thickness and width measurements in table N.1 the cross-sectional surface area is calculated through equation (N.1). This equation is based on the observation from figure N.2(b) and figure N.3 that the cross-sectional area of the fracture surface is represented by two trapezoids. The average of the left and the right part of the broken specimen, in table N.1, are used to calculate the cross-sectional surface area.

\[
S_u = \left(\frac{t_1 + t_2}{2}\right) \frac{b}{2} + \left(\frac{t_2 + t_3}{2}\right) \frac{b}{2} \tag{N.1}
\]
In order to derive true stress-strain values, presented in table N.2 from the tensile test data, the following procedure is followed: The total volume remains constant, which requires that, for a tensile test, equation (N.2) applies:

\[ S_0 L_0 = S L \]  

(N.2)

From the constant volume requirement in equation (N.2) and the relation for the engineering strain the relations in equation (N.3) can readily be derived:

\[ \frac{S_0}{S} = \frac{L}{L_0} = \frac{L_0 + \Delta L}{L_0} = \frac{\Delta L}{L_0} + 1 = (\varepsilon_{\text{eng}} + 1) \]  

(N.3)

The definition of true strain requires that the strain increment is used, rather than the total elongation after the tensile test. This is illustrated in equation (N.4), where the true strain is eventually expressed in terms of the final reduction in cross-sectional area of the tensile test specimens.

\[ \varepsilon_{\text{true}} = \sum_{L_0}^L \frac{\Delta L}{L} \approx \int_{L_0}^L \frac{dL}{L} = \ln \left( \frac{L}{L_0} \right) = \ln (\varepsilon_{\text{eng}} + 1) = \ln \left( \frac{S_0}{S} \right) \]  

(N.4)

The definition of the true strain as \( \ln (\varepsilon_{\text{eng}} + 1) \) is only valid up till the point that localisation (necking) occurs. After this point, the engineering stresses decreases. This implies a decrease of the stresses while in reality strain hardening occurs, which means an increase of the stress rather than the apparent decrease that is seen in the engineering stress-strain curve. This means that the actual surface area \( S \) or \( S_u \) needs to be taken into account instead of the original surface area \( S_0 \). The true stress is therefore defined as:

\[ \sigma_{\text{true}} = \frac{F}{S} \]  

(N.5)

From the tensile testing data, unfortunately, the instantaneous cross-sectional area \( S \) is unknown. However, from the broken tensile test specimen, the ultimate cross-sectional area at failure \( S_u \) can be obtained. By careful measurements with a ball point micrometer, the cross-sectional area close to the fracture surface can be measured. The results of this procedure are presented in table N.1. With \( S_u \) known, the true stress at failure can be calculated using equation (N.6):

\[ \sigma_{\text{true}} = \frac{\sigma_{\text{eng}} S_0}{S_u} \]  

(N.6)

With \( S_u \) known, also the true strain at failure can be computed by using the relations derived in equation (N.4). With the above described methodology, both true stress and true strain have been computed for the tensile tests. The results hereof are shown in table N.2.

Using the values from table N.2, a true stress-strain curve can be constructed. In the most simple and straightforward way, this is done by making a linear extrapolation from the point of localisation to the true stress-strain at failure. This can be seen in figure N.5, in which the cross at the end of the true stress-strain curve is the value from table N.2.

Table N.2: Conversion to true stress-strain values at failure of the tensile test specimens.

<table>
<thead>
<tr>
<th>Elongation [-]</th>
<th>( S_0 [mm^2] )</th>
<th>( S_u [mm^2] )</th>
<th>( \sigma_{\text{true}} [MPa] )</th>
<th>( \varepsilon_{\text{true}} [-] )</th>
</tr>
</thead>
<tbody>
<tr>
<td>F20354-D1</td>
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<td>122.0</td>
<td>48.4</td>
<td>826</td>
</tr>
<tr>
<td>F20354-D2</td>
<td>0.33</td>
<td>122.1</td>
<td>46.3</td>
<td>852</td>
</tr>
<tr>
<td>F20354-D3</td>
<td>0.33</td>
<td>122.4</td>
<td>47.9</td>
<td>823</td>
</tr>
<tr>
<td>F20354-L1</td>
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<td>122.0</td>
<td>51.4</td>
<td>829</td>
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<tr>
<td>F20354-L2</td>
<td>0.32</td>
<td>122.9</td>
<td>51.9</td>
<td>818</td>
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<tr>
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<td>53.4</td>
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<tr>
<td>average</td>
<td>0.33</td>
<td>122.3</td>
<td>49.9</td>
<td>824</td>
</tr>
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</table>
Figure N.5: Stress-strain curves from tensile testing. The linear extrapolation of the true stress-strain curve is done from the point localisation occurs, towards the average stress-strain at failure.

In table N.3 and table N.4 the engineering and true strains have been determined from measuring the fracture surface in the manner described in this appendix. The true strains have been calculated with the relations described in equation (N.4) and using the micrometer measurement results reported in table N.1. The volume check is based on the principle of conservation of volume as it is described in equation (N.7).

\[(1 + \epsilon_{\text{length}})(1 + \epsilon_{\text{width}})(1 + \epsilon_{\text{thickness}}) = 1\]  

(N.7)

<p>| Table N.3: Engineering strains at failure from measuring the fracture surface of the tensile test specimens. |
|--------------------------------------------------|-----------------|-----------------|-----------------|-----------------|</p>
<table>
<thead>
<tr>
<th>Engineering</th>
<th>(\epsilon_{\text{length}})</th>
<th>(\epsilon_{\text{width}})</th>
<th>(\epsilon_{\text{thickness}})</th>
<th>check volume</th>
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</thead>
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</tr>
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<tr>
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</tr>
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</tr>
<tr>
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<td>1.46</td>
<td>-0.31</td>
<td>-0.41</td>
<td>1.00</td>
</tr>
</tbody>
</table>

<p>| Table N.4: True strains at failure from measuring the fracture surface of the tensile test specimens. These true strains are calculated from the micrometer measurement results with the formulations derived in equation (N.4). |
|----------------------------------|-----------------|-----------------|-----------------|-----------------|</p>
<table>
<thead>
<tr>
<th>True</th>
<th>(\epsilon_{\text{length}})</th>
<th>(\epsilon_{\text{width}})</th>
<th>(\epsilon_{\text{thickness}})</th>
<th>check volume</th>
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<tbody>
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</tr>
<tr>
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<td>-0.28</td>
<td>-0.35</td>
<td>0.90</td>
</tr>
<tr>
<td>F20354-L1</td>
<td>0.86</td>
<td>-0.26</td>
<td>-0.33</td>
<td>0.91</td>
</tr>
<tr>
<td>F20354-L2</td>
<td>0.86</td>
<td>-0.27</td>
<td>-0.33</td>
<td>0.91</td>
</tr>
<tr>
<td>F20354-L3</td>
<td>0.83</td>
<td>-0.26</td>
<td>-0.32</td>
<td>0.92</td>
</tr>
<tr>
<td>average</td>
<td>0.90</td>
<td>-0.27</td>
<td>-0.34</td>
<td>0.91</td>
</tr>
</tbody>
</table>
Friction Coefficient Testing - Results

In this appendix the results of the separate friction tests are reported. Two friction scenario’s were tested, the normal situation - with friction - and a reduced friction situation with a polished and lubricated plate surface.

The static friction coefficient of both steel on steel and the lubricated steel on steel is determined using the set-up shown in figure O.1 and figure O.2.

![Figure O.1: Photo of the test set-up to determine the friction coefficient.](image)
The specimens for the friction tests (figure O.3) are placed in the enclosures in the two sliding plates. Cylinder A applies a force of 50kN and the force needed to slide the lower clamping plate out then determines the friction coefficient from equation (O.1).

\[
\mu = \frac{F_{\text{normal}}}{F_{\text{friction}}} \tag{O.1}
\]
Figure O.4: Results of the friction coefficient testing. The dashed lines indicate the average friction coefficient determined from the steady state values of observed from the friction coefficient testing results.

Table O.1: Friction coefficients from the friction coefficient tests figure 5.18.

<table>
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<tr>
<td>steel-steel</td>
<td>0.28</td>
</tr>
<tr>
<td>greased</td>
<td>0.03</td>
</tr>
</tbody>
</table>
Figure O.5: Result of the friction coefficient test steel-steel 1.

Figure O.6: Result of the friction coefficient test steel-steel 2.

Figure O.7: Result of the friction coefficient test steel-steel 3.
Figure O.8: Result of the friction coefficient test greased 1.

Figure O.9: Result of the friction coefficient test greased 2.

Figure O.10: Result of the friction coefficient test greased 3.
Figure O.11: Result of the friction coefficient test greased 4.

Figure O.12: Result of the friction coefficient test greased 5.

Figure O.13: Result of the friction coefficient test greased 6.
Exploratory Literature Review

This appendix contains the draft version of the exploratory literature review that was performed at the start of this graduation project. The literature review was never fully finished, therefore the draft version is presented in this appendix. Readers of this thesis should keep in mind that the literature review in this appendix, provided the inspiration for the presented concept and the background information on the subject of ship grounding analysis.
Ship Grounding

Literature Review

S.R. Haag

Literature review about damage estimate methods and simulation methods used in ship grounding analysis
Ship Grounding

Literature Review

by

S.R. Haag

in partial fulfilment
to obtain the degree of Master of Science
at the Delft University of Technology.

Student number: 4087860
Project duration: April 18, 2016 –
Preface

Salvage of ships has fascinated me since my second year of studying marine engineering. It is from this fascination that I designed my own minor program about salvage and wreck removal in the third year. The same fascination landed me an internship at Ardent Global, a renowned salvage company in IJmuiden, The Netherlands. And now my fascination for salvage has brought me to the Structural Dynamics department of TNO where I hope to conduct research that will enrich the knowledge and knowhow applicable for marine salvage.

The idea for the newly proposed damage estimation method introduced in section 1.4 originally came from my supervisors at TNO, Martijn Hoogeland and Lex Vredeveldt. For the coming 6 months I am going to try to make this idea come to life and this literature review is the first step in that process. It has been written from a personal motivation since a literature review is not required for the completion of my master thesis. I hope to give a clear insight in what has been written and researched about grounding analysis over the years and to build a solid foundation for the continuation of my graduation project.

S.R. Haag
Delft, June 2016
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1

Introduction

This literature review is a part of the graduation project of Stan Haag. This project is a partial fulfilment in order to obtain the degree of Master of Science at the Delft University of Technology. The study program followed is that of Marine Engineering with the master track “Science” and a specialisation in “Ship & Offshore Structures”. This literature review has been initiated on personal account and is not a formal requirement for the master thesis.

This literature review hopes to provide a clear overview of ship grounding analysis and the research related to this discipline. The introduction will first provide basic background information on grounding in general, the history of grounding analysis and some basic principles related to grounding analysis. Finalising the introduction a proposal will be put forth for a new and alternative method to estimate damage in a real ship grounding accident. Hereafter the report is subdivided into the three chapters representing the three main categories for grounding analysis research found in literature: Experiments, analytical methods and finite element analysis. Each chapter is concluded with a discussion on the literature that has been covered in that chapter. The general discussion will then bundle the major items that followed from each individual chapter discussion. The literature review will conclude with a set of research questions and a plan that will form the guidelines for the MSc thesis project of the author.

1.1. Ship Grounding

Despite ongoing efforts to minimize the risks, maritime accidents keep occurring. Major accidents with ships can (roughly) be divided in the following categories:

- Collision
- Grounding\Stranding
- Contact
- Fire\Explosion
- Flooding\Loss of stability

Of which collision, grounding and contact make up the majority of the marine accidents. This literature review will focus solely on grounding of ships and the associated damage to the ship structure due to the grounding accident. Grounding can be subdivided into two scenarios: powered grounding and stranding. Powered grounding differentiates from stranding in the sense that it is mainly governed by the momentum of the vessel, whereas in stranding the vessel is assumed to settle on the sea floor without any influence of sway or surge motions.

A ship running aground is a potentially perilous situation. Most often the state of the bottom (soft ground or hard rock) is unknown. Depending on the topology of the seabed the ship may either tear open or slide over the ground. At that point the vessel gets stuck on the obstruction it collided with or it sails on with a damaged (possibly breached) hull. Often the damage due to grounding is initially
underestimated. This was the case with the Costa Concordia disaster. The actual damage occurs under water, it is difficult to carry out inspections and the ship is often damaged over a significant length of the vessel. The sheer size of vessels nowadays makes that a major grounding accident with significant structural damage may feel like a little bump in the road to the ship’s crew.

1.1. Grounding Accidents
Maritime accidents usually only become known to the public when there is extensive media attention. Usually this concerns disasters with a lot of damage to the environment or loss of lives. However, the majority of grounding accidents happen unnoticed by the crowds. The potential risks involved with these grounding accidents are large (see previous section 1.1). In 2015 the European Maritime Safety Agency published an annual overview of marine casualties and incidents [20] in which statistics about the different “natures of occurrence” are included. These statistics (figure 1.0) clearly show the large number of grounding casualties compared to other types as well as the relative severity of grounding casualties.

In 2016 the International Union of Marine Insurers (IUMI) published their annual “Casualty and World Fleet Statistics” [23]. From their report the contribution of grounding incidents to the number of total losses clearly shows (figure 1.1). Not only the number of total losses is large compared to other accident types, also the consequences for the environment are often significant. In figure 1.2 the percentage of oil spilled has been graphed as a function of the cause.

Well Known Grounding Accidents In (recent) history there have been some grounding accidents that have gotten a large media coverage. The most well-known example is probably the grounding of
the Costa Concordia near the island of Giglio, Italy. Another notorious grounding was the Exxon Valdez in 1986. This grounding drastically changed the way we designed ships and especially tanker vessels.

1.1.2. Parties Interested in Grounding Damage

Depending on the ground type or obstruction the vessel has run into, the ship structure will sustain a certain amount of damage. The extent of damage determines the severity of the situation. Information about the state of the vessel is of importance for numerous parties that are involved with the ship and the surroundings in which the ship has run aground.

Emergency Response  In the first and most imminent way the captain or commanding officer of the vessel wants to know the state of the vessel to determine an emergency response aboard. This requires information about water ingress, the damage and the extent of the damaged area. In an emergency situation this information is not easily obtained and it often requires visual inspection which might be difficult or even impossible.

Salvage  In a later stadium of the grounding parties involved with helping the casualty from freeing it from its critical situation are also in need to know the extent of the damage to the vessel. In this case the salvage companies, class societies, port and coastal authorities, ship owners, insurers and many more, are all in need for information on the status of the structural damage of the casualty. This information is most often not available. In practice it proves to be more difficult to carry out visual or dive inspections of a vessel in a grounded position.

Repairs  In the latest stadium of emergency response the vessel has been freed from its precarious position and brought back to safety. In this stage the extent of the damage is required by ship owners, insurers and repair yards to make an estimate on the cost. In this stage it is usually possible to conduct thorough surveys and inspections before and/or after the vessel enters a dry-dock.
Regulations and Design of Ships  From the perspective of the regulators and designers of ships it is also interesting to study and understand ship grounding and the associated damage to the ship structure. The regulators and class societies want to be able to prescribe the minimum requirements set to vessels to minimize damage to the vessel and to its environment as a consequence of ships running aground. The designers on their end want to develop a ship that is resistant to the most obvious forms of grounding damage. Being able to model or simulate a ship running aground is in that respect an important design tool.

1.2. History of Grounding Analysis

Over the years the analysis of grounding and collision events has evolved to a serious science. In general the research and the demand for knowledge in this area increases whenever a catastrophic event occurs and receives an abundant amount of media attention.

In general the focus on regulations and design for accidents has been developing from a prescriptive manner based on historic knowledge to a more probabilistic risk analysis. The field of crash and grounding analysis evolves along these developments.

In current literature one of the first pioneers to be found is Minorsky in the 1950’s. He compared different ship crashes and the reported damages. From this he derived a statistical empirical relation between the ship speed, mass and the damaged material volume (see section 3.1). In later years the focus turned to small scale experiments, mainly in the form of plate cutting experiments. These experiments were used to validate simplified analytical methods to estimate energy dissipation of the tearing deformation mode which is commonly found in grounding. After the disaster with the Exxon Valdez a strong call for improved tanker safety was answered by an increase in research. Especially spectacular were several large scale grounding and collision experiments conducted in the early 1990’s. In the late 1980’s and beginning of 1990’s the the developments in finite element analysis (FEA) started to make FEA a useful tool for grounding and collision analysis. The emphasis has been on improving these analysis tools since. Hereby the focus either lies on improving the accuracy or finding simpler ways and smaller models for grounding analysis.

![NSWC large scale grounding experiment](image-url)

This literature review will provide a general overview for all methods available in literature today. It will outline the link between the knowledge present in the research community and the needs that arise from the operational perspective of grounding and emergency response.
1.3. Understanding Grounding Analysis

The mechanics of ship grounding are complex and even nowadays not fully understood. As the "Damage Assessment Following Accidents Committee" of the ISSC 2012 put it:

"The collision (or grounding) event is a complex interaction between vessel motion, offshore structure motion (or sea bottom), interaction with the fluid, global hull response in the ship, inelastic deformations in both structures, friction, etc. A common, simplified, approach is to split the problem into two uncoupled analyses; external mechanics and internal mechanics.

The external mechanics uses global inertia forces and hydrodynamic effects to estimate the amount of kinetic energy available to be dissipated to strain energy during collision (or grounding).

The internal mechanics analysis calculates the energy dissipation and distribution of damage in the two structures." [16]

Figure 1.4: TNO-ASIS large scale grounding experiment. Internal Mechanics (structural damage) and external mechanics (acceleration). [54]

Structural Damage Damage related to grounding is usually in the bottom of the hull and is characterized by a damage of considerable length along the ships hull often referred to as "raking damage. This can clearly be seen in figure 1.5 where the damage after grounding on a small granite pinnacle (figure 1.6) has been reported. Interesting about this particular grounding: the master thought the rumble and shuddering vibration had been caused by something stuck in the propeller and he continued the voyage to port without further inspection unaware of the damage to his vessel. [3]

The famous grounding of the Costa Concordia was in a large part also cause by a relatively small rock compared to the size of the vessel (see figure 1.7). The raking damage caused in this case however, had deadly consequences. The rock caused a breach in the double hull over a length of four watertight compartments. As the vessel was designed for only two breached compartments, the water ingress was too much for the vessel to handle [2].

Uncertainties and unknown parameters in grounding analysis To analyse the grounding of ships there are many unknowns and uncertainties one has to deal with. The accidental nature of ship grounding makes it difficult to gather the required information for an accurate assessment of the vessel. When dealing with a grounding in practice it is therefore often an educated or experience based estimate at best for the input required. Important and uncertain parameters for analysis of ship grounding are:

- Obstruction size and kind
- Location of impact
- Penetration depth into hull
- Length and width of the penetration
1. Introduction

(a) Grounding damage with a ruptured ballast tank.

(b) Grounding damage consisting of scratching and plastic deformation mainly

Figure 1.5: Structural damage to the Commodore Clipper after grounding (2014) [3]

Figure 1.6: The rock the Commodore Clipper grounded upon with a speed 18.2[kts] causing the damage seen in figure 1.5

• Ship particulars
• Ship loading (tanks, cargo, drafts, etc.)
• Initial ship speed
• Technical state of the ship structure

1.4. Newly Proposed Damage Estimation Method

This thesis project also has the aim to establish a new method to estimate the damage to the ship structure in a grounding situation. The need for information directly after a grounding accident is high. Many parties rely on the scarcely available and unreliable information at their disposal. Improving the accuracy of this information whilst also speeding up the process would benefit all parties involved.

The idea is that it should be possible to estimate the structural damage in a grounding event from measuring the global motions of the vessel only. So would it be possible to deduce the damage in figure 1.4a from the acceleration time trace in figure 1.4b alone.
Figure 1.7: The rock stuck in the hull of the Costa Concordia. [2]
Experimental Grounding

The most obvious way to extensively study ship grounding and its mechanics is to conduct grounding experiments. In the automotive it is standard procedure that each new series of vehicles is put through extensive crash tests before approval. Clearly, this approach would be prohibitively expensive and impractical for ships as these are mostly uniquely build or in small series at best.

Even still, the best way to get to understand and analyse the behaviour of a ship structure in a grounding accident is by experimental research. In this chapter an overview of the relevant experimental studies will be given and discussed.

2.1. Large Scale Experiments

Large scale grounding experiments are from a research point of view the most useful experimental test set-ups. The different scaling laws for plasticity and fracture make test results of small scale tests difficult to interpret as will be explained in section 2.5. Large scale grounding experiments refer to entire or largely scaled ships or ship sections that have been subjected to some form of grounding (experiment). In public literature there is only one account of a large scale grounding experiment which has been conducted by the Naval Surface Warfare Centre - Carderock Division (U.S.) in the 1990’s [40][41][42]. Another set of large scale grounding experiments that can not be found in public literature has been performed also in the 1990’s by TNO in cooperation with ASIS, the Japanese Association for the Structural Improvement of the Shipbuilding Industry. [54]

2.1.1. NSWC Experiments

A series of one-fifth scale oil tanker grounding experiments using a specially designed grounding test machine (figure 2.1).

"The overall objective and scope of this research is to understand qualitatively the structural failure mechanisms associated with grounding events for candidate double hull tanker structures." [40]

The test sections were varied to be able to test different types of double bottom section. The dimensions of the "conventional double hull section" can be seen in figure 2.2 and the angle of collision and intrusion hight of the rock pinnacle can be seen in figure 2.3.
2. Experimental Grounding

Figure 2.1: Photograph of the experiment set-up of the large scale grounding experiments conducted by the NSWC [41]

Figure 2.2: Technical drawing of the conventional double hull test section and grounding set-up for the large scale grounding experiments conducted by the NSWC [40]
Rodd indicated two parameters of interest: the amount of vertical penetration which can be tolerated without rupture of the inner shell and the amount of energy dissipation which is a characteristic of a given structure. The focus in these experiments was on rupture of the inner shell. Three types of inner shell rupture initiation mechanisms were identified (figure 2.4) using high speed cameras:

I Simple transverse plate penetration.

II Transverse plate tearing away from its intersection with a longitudinal web.

III The aft end of a longitudinal web tearing away as the rock pushes transverse plate material aftward from it.

The measurements of vertical and horizontal grounding force as well as the velocity of the sled were published for the various hull shapes. In figure 2.5 these plots are shown for the conventional hull.
section. These figures give an indication for the second parameter of interest, the amount of energy dissipation during grounding.

Figure 2.5: Force and velocity plots of the NSWC grounding experiments for the conventional hull section (figure:2.2) [40]

2.1.2. TNO - ASIS Experiments
In the same timeframe the Japanese Association for the Structural Improvement of the Shipbuilding Industry (ASIS) did similar experiments compared to the NSWC experiments (section 2.1.1). The main difference is the fact the in the TNO experiments the ship sections were fixed in a floating support ship as seen in figure 2.6. The double hull test sections were one-fourth scale.

Figure 2.6: Experimental test set-up for the TNO-ASIS large scale grounding experiments [54]

An extensive measurement and data acquisition system was deployed during the experiments to measure speed, forces, penetration and the motions of both the striking ship and the rock. The results have not been made publicly available but have been printed in Vredeveldt et al [54]. The measured experiment data are still available within TNO. Figure 1.4a and the cover image show photographic image of the grounded situation of these experiments. Figure 2.7 shows a plot of both the horizontal acceleration and the grounding force.
The data from the experiments have been compared with finite element calculations to be able to predict the grounding force and try and understand the energy dissipation mechanisms that occur during grounding. These are one of the first FEM calculations on crash and grounding analysis compared to ones found in literature. In chapter 4 there is more on FEM calculations for grounding analysis. Figure 2.8 shows an impression of these finite element calculations and clearly shows failing elements being deleted.
2.2. Small Scale Experiments
Small scale grounding experiments are few in number. In recent literature there is only one account of true small scale grounding experiments to be found. Other accounts of small scale experiments are described in sections 2.3 Indentation Experiments and 2.4 Cutting Experiments.

2.2.1. Muscat-Fenech & Atkins Glancing Collision Experiments
Muscat-Fenech and Atkins conducted a series of experiments to simulate “glancing collisions” such as occur in grounding. They investigated the different types of denting, scoring and fracture between sheet steel of 0.8 mm thickness and different types of objects (balls, cones, pyramids and oblong blocks all with varying dimensions). The difference between these experiments and the usual treatments of indentation and perforation is that the contact has motion parallel to the sheet as well as normal to it.

"To simulate a ship hitting a rock when underway the apparatus shown in figure 2.9 was built. The main aim was to determine the sort of horizontal and vertical forces experienced as sheet is dented, stretched and perforated as it passes over an obstacle." [31]

In figure 2.9 the set-up of the experiment is shown. The penetration depth could be varied as well as the shape of the "tool indentor rock".

![Figure 2.9: Glancing collision experiment set-up][31]

Figure 2.9 shows the side view of a bulged and then torn specimen by a spherical indenter. It clearly shows the 30° angle for the approach phase. One also notes the grid drawn on the plate to conduct strain measurements of the experiments. In figure 2.11 a typical force displacement trace for a similar indenter with the same penetration depth.

![Figure 2.10: Side view of a sheet bulged and then torn by a spherical obstacle][31]
The strain measurements have been represented in a so-called forming limit diagram (figure 2.12). More theory about this type of failure criterion and the representation of major strain ($\epsilon_1$) and minor strain ($\epsilon_2$) is given in section 4.3. The authors identify the perhaps most important conclusion from this set of experiments according to them; "When cracking occurs by in-plane stretching, the biaxially-dependent fracture strains are those given by the conventional FFLD (figure 2.12) even though there are tractions parallel to the surface of the sheet in glancing collisions (the tractions did however seem to delay necking)." [31]

The continuation of the experiments is to provide an analytical way to predict the force before the onset of rupture. In order to do this accurately, first the force component related to friction needs to be separated from the total force.
Friction  This paper is the first experimental research encountered in literature to explicitly try and derive a form of friction resistance. It recognizes the difficulties in modelling the friction and postulates two alternatives: Coulomb slipping friction or constant inter-facial shear stresses. However, no independent measurements of friction coefficients or surface traction stresses have been carried out for these experiments. Therefore these parameters become disposable and are used as "calibration" to make the theory match the experimental outcome.

2.3. Indentation Experiments

Indentation experiments are such that a plate or plated structure is loaded in a perpendicular fashion, most often in a quasi-static manner. In this section three recent indentation experiments will be discussed. The first is a set of experiments by Simonsen & Lauridsen (2000) [48] on lateral indentation of thin ductile metal plate (sheets). The second is a series of nine experiments carried out by Wang et al (2000) [56] that varied the cone radius in order to model several accident scenarios. These lateral indentations tests were carried out on a scaled version of a double hull section. The third set of experiments by Hagbart & Amdahl (2008) [5] is on the indentation of both unstiffened plates and panels with one and two stiffeners in ‘longitudinal’ direction.

2.3.1. Simonsen & Lauridsen Indentation Experiments

These experiments are part of a larger study presenting analytical theories and finite element calculations as well. The lateral indentation tests were carried out on mild steel sheet of 1 mm thickness. The specimens had circular, square and rectangular geometries and the penetrators varied in radius as well (figure 2.13).

Figure 2.14 shows a fractured round specimen by a penetrator with a radius of 25 mm. Figure 2.15 shows the comparison between the derived analytical, the FEM calculations with LS-DYNA3D and the experimental measurements. Note with this comparison is that the analytical equations leading to the strain-hardening relation have been curve-fit using the experimental results.

Figure 2.13: Graphical representation of the plate specimens and penetrators used for the indentation experiments. The crosses on the specimens indicate the points of loading. [48]
2.3. Indentation Experiments

A series of nine tests was conducted on a scaled version of a double hull section (figure 2.17). The double hull was penetrated by a rigid cone (figure 2.16) of which the radius of the spherical nose was changed. These test had been designed to simulate various accident scenarios. From grounding against a very sharp underwater rock to stranding on a relatively flat seabed. Along with these experiments the research had the goal to develop new theoretical models for damage mechanisms in grounding and to develop an analytical method to predict the behaviour of double hulls.
The load-displacement curves (figure 2.18) for the tested sections with different cone nose radii showed a similar loading and failure path. In figure 2.19 the results of one of these experiments is displayed. First the load increases until outer shell rupture occurs at point a. After point a the load decreases until at c the main supporting members take up and the load increases again.
2.3. Indentation Experiments

Figure 2.18: Load-displacement curves for various spherical nose radii (300, 200, 100, 50 and 10 mm)

2.3.3. Alsos & Amdahl Indentation Experiments

Alsos & Amdahl (2008) carried out a series of five indentation experiments with unstiffened plated and stiffened panels (figure 2.20). These panels are loaded laterally by a cone shaped indenter (figure 2.21) until fracture occurs (figure 2.22). The focus of this paper and the experiments is on the indentation resistance of hull panels during ship grounding [5]. In another paper [7] that will be covered in chapter 4, they deal with a numerical reconstruction of these experiments.

Figure 2.20: Experimental set-up and test specimen dimensions for Alsos & Amdahl (2008) indentation experiments [5]

Figure 2.19: Results for the indenting experiments with nose radius of 50 mm [56]
Based on the observations and test results a few interesting conclusions have been drawn. First of all the stiffeners cannot be treated by means of beam theory in the range of large (plastic) deformations. In all experiments with stiffeners they started tripping and folding to one side (figure 2.22) before excessive straining in the flange could occur. Also an interesting observation from figure 2.23 is that stiffened panels dissipate less energy prior to fracture than unstiffened plates.

"The unstiffened panel exhibits the most "ductile behaviour". This indicates an interesting trend: increasing the number of stiffeners and/or adding stronger stiffeners, yield redu-
2.4. Cutting Experiments

Plate cutting refers to the energy absorbing mechanism that is strongly related to the raking damage often seen in grounding situations. Because cutting experiments can easily be scaled and are relatively easy to perform these have been an extensive part of the experimental research related to grounding accidents. Early experiments were often performed with drop hammer tests. Later series of experiments resolved to quasi static or steady-state plate cutting to eliminate dynamic effects which are difficult to interpret.

In general all plate cutting experiments have a similar set-up. In figure 2.24 the usual set-up of plate cutting experiments is shown. In this case it is a steady state "pushing machine". In the drop tower experiments the hydraulic cylinder is replaced by a mass that is hoisted above the specimen.
Figure 2.24: Cutting experiment set-up generally looks like this. These images are from a series of experiments conducted by Paik (1994) [35] in which he cut several stiffened steel panels rather than plain steel plates.

Cutting often manifests itself in several forms. The most obvious one is the clean curling cut (figure 2.25a) in which the plate is separated at the tip and rolls/folds to the same side. The other ‘extreme’ is the concertina tearing mode (figure 2.25b) in which the plate folds back and forth in front of the wedge in a localized buckling way. A form that has properties of both these cutting modes may be called braided cutting in which the plate does separate at the wedge tip but the deformed flaps fold back and forth.

Figure 2.25: Different modes of cutting in Paik’s experiments (1994) [35]

Simonsen (1997) [47] presented a very extensive and proper literature review of all plate cutting experiments and their related prediction formulae. In figure 2.26 a comparison is made between the plate cutting prediction models, compared with two different cutting experiments.
2.5. Scaling of Experimental Results

The applicability of test/experiment results is largely dependent on the scalability of these same test results. Whereas many of the theories for force and work predictions developed lack generality because of similar issues with the scaling of results. As Simonsen (1997) [45] put it:

"Model scale tests may seem to be the obvious approach, see e.g. references [31][35], but small scale grounding tests may be difficult to interpret due to different scaling laws for plasticity and fracture. Several empirical or simplified theoretical models have been developed for the problem of cutting of very simplified structures - see e.g. references [25][49][53] - but it is difficult to extend these results to real assembled ship bottom structures."

In this chapter several approaches to solve this scaling problem in scaling of experiments and results of experiments will be discussed.

Booth et al (1983) [13] showed that a series of similar impact experiments done at different scales yield different results. Their observation in general were that the deformations between the different scales did not correspond. It was found that deformations at full scale were 2.5 times larger than could be expected from the 0.25 scale model. In the same manner did the impact times and accelerations not correspond in a linearly scaled way. This deviation was not to be explained by differences between static material strength and ductility or by overall geometry of the specimen. It was deemed a possibility that a "fundamental difference in toughness between scales contributed to the deviation".

Atkins (1988) [10] did a more thorough research into the governing scaling laws in combined plastic flow and fracture. Atkins made an attempt to separate the different energy dissipating mechanisms into plastic flow and fracture. By analysis of the experimental results of Jones & Jouri (1987) [25] and Lu & Calladine (1990) [28] he deduced the following scaling relation for the work done:

\[
\frac{W_p}{W_m} = \lambda^2 (\lambda \xi + 1) \left( \xi + 1 \right) \tag{2.1}
\]

where \( \xi = \left( \frac{I_{safe}}{R} \right) \left( \frac{V_{plastic}}{\lambda_{crack}} \right) \) and...
2. Experimental Grounding

\[ W \]

Work Prototype

\[ W_m \]

Work model

\[ \lambda \]

Linear scale factor

\[ \sigma \]

Normal true stress

\[ \varepsilon \]

Normal true strain

\[ R \]

Fracture toughness

\[ V_{\text{plastic}} \]

Plastically deformed volume

\[ A_{\text{crack}} \]

Crack area

In which he assumes the fracture toughness of both materials to be equal.

Rodd (1996) \[40\] in his large scale experiments at the NSWC disregarded any of these theoretical notions on the scalability of experimental results. Due to the previously described difficulties in scaling of results he only discussed the results of his model tests without venturing into assumptions about the consequences for the results in full scale.

Table 2.1: Linear scaling relationships for Rodd’s large scale tanker experiments [40]

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Relationship</th>
<th>Scale Factor</th>
</tr>
</thead>
<tbody>
<tr>
<td>Length</td>
<td>[ L_p = \lambda L_m ]</td>
<td>( \lambda = 5.33 )</td>
</tr>
<tr>
<td>Elastic Modulus</td>
<td>[ E_p = E_m ]</td>
<td>1 to 1</td>
</tr>
<tr>
<td>Deflection</td>
<td>[ \delta_p = \lambda \delta_m ]</td>
<td>( \lambda = 5.33 )</td>
</tr>
<tr>
<td>Strain</td>
<td>[ \varepsilon_p = \varepsilon_m ]</td>
<td>1 to 1</td>
</tr>
<tr>
<td>Stress</td>
<td>[ \sigma_p = \sigma_m ]</td>
<td>1 to 1</td>
</tr>
<tr>
<td>Velocity</td>
<td>[ V_p = V_m ]</td>
<td>1 to 1</td>
</tr>
<tr>
<td>Section Area</td>
<td>[ A_p = \lambda^2 A_m ]</td>
<td>( \lambda^2 = 28.4 )</td>
</tr>
<tr>
<td>Force</td>
<td>[ F_p = \lambda^2 F_m ]</td>
<td>( \lambda^2 = 28.4 )</td>
</tr>
<tr>
<td>Mass</td>
<td>[ m_p = \lambda^3 m_m ]</td>
<td>( \lambda^3 = 151.7 )</td>
</tr>
<tr>
<td>Displacement</td>
<td>[ \delta_p = \lambda^3 \delta_m ]</td>
<td>( \lambda^3 = 151.7 )</td>
</tr>
<tr>
<td>Dissipated Energy</td>
<td>[ E_p = \lambda^3 E_m ]</td>
<td>( \lambda^3 = 151.7 )</td>
</tr>
<tr>
<td>Time</td>
<td>[ T_p = \lambda T_m ]</td>
<td>( \lambda = 5.33 )</td>
</tr>
<tr>
<td>Acceleration</td>
<td>[ a_p = \lambda^{-1} a_m ]</td>
<td>( \lambda^{-1} = 0.188 )</td>
</tr>
</tbody>
</table>

Shen (1998) \[44\] did a series of experiments to try and clarify the scaling that governs cutting of metal plates. He conducted a series of 53 cutting experiments. He divided the total energy for cutting into three components:

\[ W = W_{\text{cutting}} + W_{\text{bending}} + W_{\text{friction}} \] (2.2)

It was concluded that the scaling law is violated for cutting energy but not bending deformation and frictional energies. The frictional resistance was found to be dominant and greatly affected the by a change in the geometry of the cut plate.

2.6. Discussion

A number of grounding experiments or grounding related experiments have been presented in this chapter. These experiments have been divided into large scale, small scale, indentation and cutting experiments. A summary of these grounding experiments is provided in table 2.2 with a plate thickness to give an indication of the scale of the experiment.
Table 2.2: Comparison of discussed grounding (related) experiments

<table>
<thead>
<tr>
<th>Experiment</th>
<th>Type</th>
<th>$t_{plate}$</th>
<th>Description</th>
</tr>
</thead>
<tbody>
<tr>
<td>NSWC [40]</td>
<td>Large scale</td>
<td>3 mm</td>
<td>Qualitative focus, double bottom crack initiation mechanism.</td>
</tr>
<tr>
<td>TNO - ASIS [54]</td>
<td>Large scale</td>
<td>5 mm</td>
<td>Real grounding situation, comparison with FEA.</td>
</tr>
<tr>
<td>Muscat-Fenech [31]</td>
<td>Small scale</td>
<td>0.8 mm</td>
<td>Focus on different rock topologies and penetration depths.</td>
</tr>
<tr>
<td>Simonsen &amp; Lauridsen [48]</td>
<td>Indentation</td>
<td>1 mm</td>
<td>Different rock radii, different plate geometries.</td>
</tr>
<tr>
<td>Wang et al [56]</td>
<td>Indentation</td>
<td>2.3 mm</td>
<td>Scaled double hull sections.</td>
</tr>
<tr>
<td>Alsos &amp; Amdahl [5]</td>
<td>Indentation</td>
<td>5 mm</td>
<td>Unstiffened, single and double stiffened panels, combined with FEA.</td>
</tr>
<tr>
<td>Vaughan [53]</td>
<td>Cutting</td>
<td>0.75-1.87 mm</td>
<td>Drop-hammer tests, variation of inclination angles.</td>
</tr>
<tr>
<td>Jones &amp; Jouri [25]</td>
<td>Cutting</td>
<td>1.50-5.95 mm</td>
<td>Drop-hammer tests, variation of inclination angles.</td>
</tr>
<tr>
<td>Atkins [10]</td>
<td>Cutting</td>
<td>-</td>
<td>Scaling, comparison between data in literature.</td>
</tr>
<tr>
<td>Lu &amp; Calladine [28]</td>
<td>Cutting</td>
<td>0.72-2.0 mm</td>
<td>Quasi-static, various inclination angles.</td>
</tr>
<tr>
<td>Paik [35]</td>
<td>Cutting</td>
<td>3.4-7.8 mm</td>
<td>Quasi-static, stiffened panels, various inclination angles.</td>
</tr>
<tr>
<td>Shen et al [44]</td>
<td>Cutting</td>
<td>0.95-3.97 mm</td>
<td>Scaling laws.</td>
</tr>
</tbody>
</table>

These experiments all aim to clarify various mechanisms that occur during grounding. Most of them are also used to support empirical or analytical models and some of these experiments are cited numerously throughout literature related to grounding. In fact, only the NSWC experiments are a series that is cited with a great regularity. It is nearly the only experiment that is often used to validate either analytical methods or Finite Element Analysis result for prediction or simulation of grounding damage. This can be seen as a rather limited amount of experimental applicable for validation and verification.

Astrup (1994) [9] noticed the distinctive difference in cutting resistance of thick plates as opposed to thin plates. In other researches Booth et al (1983) [13], Astrup (1988) [9] and Shen et al [44] noted the difficulties in scaling of experimental results. This leads to question the applicability of most of the experimental results which are all derived with small plate thicknesses. It is clear that steel plate used in ship building is more often than not of greater thickness and therefore might behave quite different from experimental data often used in maritime crash and grounding analysis.

In section 2.2 it is mentioned that the experiments of Muscat-Fenech & Atkins are the only true small scale grounding experiments to be found in literature. The difference in a true grounding load on a panel or plate is that the contact has motion parallel as well as normal to it. This makes for a fine combination of bi-axial stretching and crack initiation, followed by crack growth. Such an experiment has only been conducted once and with a very small plate thickness. The fold in the plate that accommodates the approach also makes these specific results difficult to interpret.

In general the following three remarks about the current experimental research can be made:

- The number of grounding (related) experiments conducted is limited. The number that is referenced regularly in literature is even more limited.

- The scale and plate thicknesses used in most experiments is small. This makes interpreting the results and the conclusions drawn questionable for application in ship grounding.

- All experiments but a few lack either motion parallel or motion normal to the plate.
Analytical / Empirical Analysis

Before Finite Element Analysis, analytical and empirical analysis of grounding has been the only way to predict damage to the ship structure. These methods were developed to aid regulatory bodies and designers of ships and offshore structures.

They are all closed-form formulas that can be solved relatively easily when the parameters are known. Most of them give a measure of the force or work required to produce a certain extent of damage. Depending on the approach and the desired accuracy they usually split the energy dissipation into various energy absorbing mechanisms.

This chapter will deal with the development of these damage prediction methods. It is divided into three parts: Empirical, (simplified) analytical and FEA based methods. In the latest section the advantages and shortcomings of these methods will be discussed.

3.1. Empirical Methods

Empirical methods refer to formulas to predict the damage to the ship structure and/or the energy absorption in a grounding event that are derived from experiments or statistical data. The first one known to literature who was to venture into damage prediction for grounding and collision was Minorsky (1958) [30]

Minorsky (1957) [30] analysed 26 full-scale ship accidents and collisions and developed the formula:

\[ E = 47.2V_{\text{plastic}} + 32.7 \]

This equation relates the energy dissipation during the accident to the volume of plastically deformed material. Because of its simplicity and limited number of required parameters it has been used a lot in collision and grounding analysis. The drawback of this empirical formula is its same simplicity. It is only applicable to ship types similar as the ones Minorsky analysed and it only works well for a certain range of energy absorptions.

Cutting experiments from section 2.4 also yield several empirical methods to estimate the energy and the cutting force. A number of these experiments have been plotted by Simonsen (1997) [47] in figure 2.15.


\[
E = 1.7 \cdot 10^8 l^{1.5} + 4.4 \cdot 10^9 l^2 t^2 \tan \theta \\
E = 1.7 \cdot 10^8 l^{1.5} + 8.8 \cdot 10^9 l^2 t^2 \tan \theta
\] (3.2)

Jones & Jouri [25] in their turn proposed the following formulas for cutting with \( \alpha_w = 0^\circ \) and \( 2\theta = 30^\circ, 45^\circ \) and \( 60^\circ \):

\[
E = 8.1 \cdot 10^7 l^{1.44} \\
F = 8.1 \cdot 10^7 t^{1.44}
\] (3.4)

for \( t = 1.50mm \) and \( \sigma_0 = 255MPa \) and

\[
E = 5.9 \cdot 10^7 l^{1.305} \\
F = 5.9 \cdot 10^7 t^{1.305}
\] (3.6)

for \( t = 3.25, 4.955, 5.95 mm \) and \( \sigma_0 = 398.5MPa \). The force is therefore not dependent on the length of the cut whereas in earlier formulations this was the case.

Lu & Calladine (1990) [28] turned to quasi static cutting tests and derived similar formulas which are only valid for \( 5 < \frac{t}{l} < 150 \):

\[
E = C_{1.3} \sigma_y l^{1.3} t^{1.7} \\
F = 1.3 C_{1.3} \sigma_y l^{0.3} t^{1.7}
\] (3.8)

where \( C_{1.3} \) is an empirical constant depending on the wedge and tilt angles.

Paik (1994) [35] then turned towards thicker plates as well as including longitudinal stiffeners by using an area equivalent plate thickness. He derived empirical solutions for \( t = 3.4 \sim 7.8 mm \) and \( 2\theta = 15^\circ, 45^\circ, 60^\circ \):

\[
E = C_{1.5} C_f \sigma_0 l^{1.3} t_{eq}^{1.5} l^{1.5} \\
F = 1.5 C_{1.5} C_f \sigma_0 l^{1.3} t_{eq}^{1.5} l^{1.5}
\] (3.10)

with \( C_{1.5} = 1.112 - 1.156 \theta + 3.760 \theta^2 \) and \( C_f = 1.0 - 0.042 v + 0.001 v^2 \).

Astrup (1994) [9] performed cutting experiments with thick plates \( (t = 15, 20mm, 2\theta = 60^\circ, \alpha_w = 10^\circ) \) and found that reaction forces in thick plates where generally 60 - 75% higher than predicted by the formulas 3.8 and 3.9 as can be seen in figure 2.26.

Pedersen & Zhang (2000) [37] proposed formulas for three different energy absorption mechanisms (figure 3.1):

1. Plastic tension damage mode (equation (3.12))
2. Crushing and folding damage mode (equation (3.13))
3. Tearing damage mode (equation (3.14))
These failure mechanisms lead to the following energy formulations for energy absorption:

\[ E_1 = 0.77 \varepsilon_c \sigma_0 V_{plastic} \]  
\[ E_2 = 3.50 \left( \frac{t_{av}}{B} \right)^{0.67} \sigma_0 V_{plastic} \]  
\[ E_3 = 3.21 \left( \frac{t_{eq}}{l} \right)^{0.6} \sigma_0 V_{plastic} \]

<table>
<thead>
<tr>
<th>Symbol</th>
<th>Unit</th>
<th>Description</th>
</tr>
</thead>
<tbody>
<tr>
<td>( \varepsilon_c )</td>
<td>[-]</td>
<td>Critical failure strain</td>
</tr>
<tr>
<td>( \sigma_0 )</td>
<td>[MPa]</td>
<td>Energy equivalent flow stress</td>
</tr>
<tr>
<td>( V_{plastic} )</td>
<td>[m³]</td>
<td>Volume of deformed material</td>
</tr>
<tr>
<td>( t_{av} )</td>
<td>[mm]</td>
<td>Average plate thickness</td>
</tr>
<tr>
<td>( t_{eq} )</td>
<td>[mm]</td>
<td>Equivalent plate thickness</td>
</tr>
<tr>
<td>( b )</td>
<td>[mm]</td>
<td>Average width of plates</td>
</tr>
<tr>
<td>( l )</td>
<td>[mm]</td>
<td>Length of tear</td>
</tr>
</tbody>
</table>

The equations have been compared with a number of different crash and grounding situations and a couple of documented tests. The methods is seen to correlate well with the experimental results. One of the comparisons of the energy absorption can be seen in figure 3.2 where two of the TNO-ASIS test results [54][55] have been compared with the proposed methodology.

This method provides an improvement over Minorsky's classical energy absorption method as it takes into account several energy absorption mechanisms. Hence it could provide a translation for modern vessel designs which have not been examined by Minorsky.

Simonsen & Lauridsen (2000) [48] conducted experiments of lateral indentation of a rigid sphere into a thin ductile metal plate (see section 2.3.1). These methods lead to an empirical formulation for
penetration and absorbed energy up to plate failure:

\[ \delta_f = 1.41n^{0.33}R^{0.48}R_b^{0.52} \]  

\[ E = \pi C_0 t_0 R R_b \left\{ 0.318 \left( \frac{R_b}{R} \right)^{0.607-0.387 \left( \frac{R_b}{R} \right)} + 0.067(n-0.2) \right\} \]  

<table>
<thead>
<tr>
<th>( \delta_f ) [mm]</th>
<th>Penetration to failure</th>
</tr>
</thead>
<tbody>
<tr>
<td>( n ) [-]</td>
<td>Material power law exponent</td>
</tr>
<tr>
<td>( R ) [mm]</td>
<td>Plate radius</td>
</tr>
<tr>
<td>( R_b ) [mm]</td>
<td>Indentor radius</td>
</tr>
<tr>
<td>( E ) [J]</td>
<td>Energy</td>
</tr>
<tr>
<td>( C_0 ) [MPa]</td>
<td>Power law coefficient</td>
</tr>
<tr>
<td>( t_0 ) [mm]</td>
<td>Initial plate thickness</td>
</tr>
</tbody>
</table>

These indentation tests were also modeled numerically using LS-DYNA3D (a commercially available FEA package). The comparison of this empirical method with the FEA and the experiments can be seen in figure 2.15. The proposed approach predicts the penetration to plate failure very well and the results agree for axis-symmetric cases of loading. For non-symmetric loading it underestimates the energy to failure of the plate. The experiments were conducted using a thin plate, ensuring all could be modelled as a membrane, whereas in real impact problems bending and shear could potentially have a large influence.

Zhang (2002) [62] developed a combination of analytical and empirical formulae to estimate grounding force, energy dissipation and damage length to tanker vessels. The analytical formula is a combination of analytically derived formulations for plastic resistance force (bending and membrane) and friction force for the initial cutting of plates. These components added together amounts to:

\[ F_H = 1.924\sigma_0 t^{1.5}l^{0.5} \epsilon_c^{0.25} (\tan \theta)^{0.5} \left( 1 + \frac{\mu}{\tan \theta} \right) \]  

\[ E = \int_0^l F_H dl = 1.295\sigma_0 t^{1.5}l^{1.5} \epsilon_c^{0.25} (\tan \theta)^{0.5} \left( 1 + \frac{\mu}{\tan \theta} \right) \]  

<table>
<thead>
<tr>
<th>( F_H ) [N]</th>
<th>Horizontal grounding/cutting force</th>
</tr>
</thead>
<tbody>
<tr>
<td>( \sigma_0 ) [MPa]</td>
<td>Energy equivalent flow stress</td>
</tr>
<tr>
<td>( t ) [mm]</td>
<td>Plate thickness</td>
</tr>
<tr>
<td>( l ) [mm]</td>
<td>Cut length</td>
</tr>
<tr>
<td>( \epsilon_c ) [-]</td>
<td>Critical failure strain</td>
</tr>
<tr>
<td>( 2\theta ) [deg]</td>
<td>Wedge angle</td>
</tr>
<tr>
<td>( \mu ) [-]</td>
<td>Coulomb friction coefficient</td>
</tr>
<tr>
<td>( E ) [J]</td>
<td>Energy dissipation</td>
</tr>
</tbody>
</table>

These expressions for force and energy compare well with experiments. They are however, only applicable to initial cutting of steel plating. To be able to predict or estimate the horizontal grounding force in a real grounding, a semi-empirical formula has been developed based on this analytical approach:

\[ F_H = 3.58\sigma_0 t^{0.6} b^{0.4} \]  

<table>
<thead>
<tr>
<th>( F_H ) [N]</th>
<th>Horizontal grounding/cutting force</th>
</tr>
</thead>
<tbody>
<tr>
<td>( \sigma_0 ) [MPa]</td>
<td>Energy equivalent flow stress</td>
</tr>
<tr>
<td>( t ) [mm]</td>
<td>Plate thickness</td>
</tr>
<tr>
<td>( t_{eq} ) [mm]</td>
<td>Equivalent plate thickness</td>
</tr>
<tr>
<td>( b ) [mm]</td>
<td>Damage width</td>
</tr>
</tbody>
</table>

Using this semi-empirical and combining this with rules of class societies and statistical investigation of
1700 oil tankers with lengths of 190–360 m Zhang derived an approximation formula for the grounding resistance force and the damage length for double hull tankers:

\[
F_H = 44.5 \left( \frac{b}{B} \right)^{0.4} \left( \frac{L}{300} \right)^{2.04}
\]

\[
L_{DH} = 175 \left( \frac{b}{B} \right)^{0.4} \left( \frac{L}{300} \right)^{1.5} \left( \frac{V_{grad}}{15} \right)^{2}
\]

<table>
<thead>
<tr>
<th>Symbol</th>
<th>Description</th>
</tr>
</thead>
<tbody>
<tr>
<td>(F_H)</td>
<td>Horizontal grounding/cutting force</td>
</tr>
<tr>
<td>(b)</td>
<td>Damage width</td>
</tr>
<tr>
<td>(B)</td>
<td>Ship breadth</td>
</tr>
<tr>
<td>(L)</td>
<td>Ship Length</td>
</tr>
<tr>
<td>(L_{DH})</td>
<td>Horizontal damage length</td>
</tr>
<tr>
<td>(V_{grad})</td>
<td>Grounding speed</td>
</tr>
</tbody>
</table>

The latter damage estimate formulas have not been verified and is only applicable for double hull oil tankers. It only takes damages into account that do not breach the tanktop i.e. only outer bottom is breached.

### 3.2. (Simplified) Analytical Methods

Analytical methods are often similar in form and shape as the empirical methods. They distinguish different failure modes and energy absorption methods and try to solve their contributions analytically. This section will give a brief overview of developed methods and some of the eventual formulas. For the derivation of each of the individual methods reference is made to the original research paper.

**Peer (1991)** [39] developed a computational model based on analytical formulas as a part of the joint MIT-industry program on tanker safety. In his approach he took the ship rigid-body-motions into account in a quasi-static manner thus coupling the local deformations with the global motions of the vessel. Unfortunately his model does not allow analytical solution and needs an iterative parameter variation procedure performed by a self written Fortran code. The results are also somewhat questionable and the report is not cited by any other literature. However, his approach is interesting and probably useful for the proposed damage estimation method.

**Wierzbicki & Thomas (1993)** [58] developed an analytical model for the steady state cutting of thin metal sheets. They derived an expression for predicting the cutting force which is based on the experimental results as presented by Lu & Calladine (1990) [28] (see section 2.4).

\[
F_H = 3.28 \sigma_0 \mu^{0.4} l^{0.4} t^{1.6} \delta_t^{0.2}
\]

It is the first publication about cutting of steel plates in which the friction appears explicitly. Equation 3.22 is valid for \(0.1 < \mu < 0.4\) and \(10^\circ < \theta < 30^\circ\).

**Wang et al (1997)** [55] proposed simple and closed form analytical solutions distinguishing four different structural failure modes:

1. Transverse structural member failure

\[
F = \frac{2 \sigma_0 B t \Delta}{L}
\]
2. Bottom plate failure immediately behind transverse member

\[ F_m = \left( \frac{2.32}{\lambda} \right) \sigma_0 (2b)^{0.33} t^{1.67} \]  \hspace{1cm} (3.24)

3. Bottom plating failure

\[ F = 1.51 \sigma_0 t^{1.5} l^{0.5} (\sin \theta)^{0.5} \left( 1 + \frac{\mu}{\tan \theta} \right) \]  \hspace{1cm} (3.25)

and for fractured bottom plating the constant tearing load \( F_s \) is calculated according to a formula developed by Ohtsubo & Wang (1995) [34]:

\[ F_s = 1.51 \sigma_0 t^{1.5} l^{0.5} (\sin \theta)^{0.5} \left( 1 + \frac{\mu}{\tan \theta} \right) \]  \hspace{1cm} (3.26)

When a concertina tearing mode is present in the bottom plating the formula developed by Wierzbicky (1995) [57] is applied:

\[ F_m = \frac{3\sigma_0 (2b)^{0.33} t^{1.67}}{\lambda} + 2Rt \]  \hspace{1cm} (3.27)

4. Stiffeners are smeared to cross-sectional area of the bottom plating.

---

<table>
<thead>
<tr>
<th>Symbol</th>
<th>Description</th>
</tr>
</thead>
<tbody>
<tr>
<td>( F )</td>
<td>Resistance force</td>
</tr>
<tr>
<td>( \sigma_0 )</td>
<td>Yield stress</td>
</tr>
<tr>
<td>( B )</td>
<td>Width of beam model</td>
</tr>
<tr>
<td>( t )</td>
<td>Plate thickness</td>
</tr>
<tr>
<td>( \Delta )</td>
<td>Deflection</td>
</tr>
<tr>
<td>( L )</td>
<td>Half length of beam model</td>
</tr>
<tr>
<td>( F_m )</td>
<td>Mean force</td>
</tr>
<tr>
<td>( \lambda )</td>
<td>Factor for effective crushing length</td>
</tr>
<tr>
<td>( 2b )</td>
<td>Width of failure mechanism</td>
</tr>
<tr>
<td>( l )</td>
<td>Tearing length</td>
</tr>
<tr>
<td>( 2\theta )</td>
<td>Wedge angle</td>
</tr>
<tr>
<td>( \mu )</td>
<td>Coulomb friction coefficient</td>
</tr>
<tr>
<td>( F_s )</td>
<td>Steady state resistance force tearing mode</td>
</tr>
<tr>
<td>( R )</td>
<td>Fracture resistance parameter</td>
</tr>
</tbody>
</table>

This simplified method has been tested in two case studies, comparing the TNO-ASIS experiments with the proposed methodology, and by comparison with a real grounding accident. It is claimed that the proposed methodology works well compared to these two case studies with predicted energy dissipation within 82\% - 115\% of the initial kinetic energy. From figure 3.3 it can be seen that the theory accommodates transverse members and therefore displays a jump at each cross member.
Simonsen (1997) [45] developed an extensive and oft quoted method to estimate the energy absorption of a ship grounding on a hard rock pinnacle. The crux of his theory was formulated as follows:

"When external loads are applied to a deformable structure, the power (the work rate) of these loads must be equal to the incremental energy stored elastically or dissipated in the structure. If a rigid-plastic structure is assumed, no elastic energy can be stored and the power of the external loads thus equals the rate of energy dissipated by plastic deformations, fracture and frictional effects on the surface of the structure. This can be expressed as (equation (3.28))" [45]

\[
F_{H}V = \dot{E}_p + \dot{E}_c + \dot{E}_f = F_{p}V + \int_{S} p\mu\nu_{rel}dS
\]  
(3.28)

The idea is to postulate the displacement and strain field of the ship bottom structure over a pre-determined rock and then, using equation (3.28), find the grounding force.

For deriving \(F_p\) not only friction and the mechanics of plate plasticity, fracture and tearing/cutting are taken into account. Also the contribution of far field plasticity, longitudinal stiffeners and transverse stiffeners are dealt with. This results in a set of closed form solutions for individual structural members that can be used to analyse an entire bottom structure subjected to the postulated displacement and strain field. In chapter 4 of the paper "Ship Grounding on Rock - I. Theory" these formulas and their derivation can be found. It distinguishes the following structural members for the analysis:

- Intact hull plating
- Fractured hull plating
- Longitudinal web girders
- Longitudinal bulkheads
- Longitudinals
• Folding of transverse members
• Fractured transverse members

In a second paper, Simonsen describes the validation and application of the developed analytical methods [46]. This is done by comparing with the large scale grounding experiments conducted by the NSWC [40]. The comparison in figure 3.4 seems to indicate that the method makes good comparison with the experimental results. Errors of the energy absorption are reported to be less than 10% in all four NSWC tests and the penetration to fracture of the inner shell is predicted with errors of 10-15%. However, Simonsen notes in the discussion:

"Due to the simplified energy approach taken the theory should not be expected to give a very accurate prediction of the distribution between plasticity and friction even though it predicts the total energy dissipation well." [46]

Indicating that even though the theoretical analysis strives to make a clear distinction between friction energy and plastic deformation energy the results do not seem to correspond to that ambition. Despite the fact that the total energy dissipation is predicted rather well, this simple discrepancy indicates that this theory lacks generality in its application to any kind of ship double bottom structure.

Wang et al (2000) [56] used a set of analytical formulas for prediction of the force needed for indentation and perforation of a stiffened panel. He compared the prediction with the results of indentation experiments (see section 2.3).

Up to failure (the forming of a crack) of the plate the indentation force is described by a simple plate punch model on a circular plate:

\[ F \approx \pi \sigma_0 t \Delta \quad (3.29) \]

\[ \epsilon \approx 0.5 \sqrt{\frac{A}{R}} \quad (3.30) \]

Failure of the plate is determined by the rupture strain which is set at \( \epsilon_c = 0.2 \). Beyond the rupture strain the plate is treated as a cracked plate and the force needed to drive the indenter through the plate is
3.3. FEA Based Methods

determined by equation (3.26) derived by Ohtsubo & Wang (1995) [34]. As this formula is derived for plate cutting instead of indentation it does not account for the resistance from the petals and the multiple cracks that grow after initial cracking. For this purpose the formula for indentation force is extended into:

\[
F = 1.51 \sigma_0 t^{1.5} \varepsilon^{0.3} n \sqrt{\sin \left( \frac{(n-2)\pi}{2n} \right) (\tan \theta + \mu)}
\]

(3.31)

<table>
<thead>
<tr>
<th>Symbol</th>
<th>Description</th>
</tr>
</thead>
<tbody>
<tr>
<td>(F)</td>
<td>Punch load</td>
</tr>
<tr>
<td>(\sigma_0)</td>
<td>Flow stress</td>
</tr>
<tr>
<td>(t)</td>
<td>Plate thickness</td>
</tr>
<tr>
<td>(\delta)</td>
<td>Indentation</td>
</tr>
<tr>
<td>(\varepsilon)</td>
<td>Strain in radial direction</td>
</tr>
<tr>
<td>(R)</td>
<td>Radius of the plate</td>
</tr>
<tr>
<td>(l)</td>
<td>Length of the tear</td>
</tr>
<tr>
<td>(n)</td>
<td>Number of cracks</td>
</tr>
<tr>
<td>(2\theta)</td>
<td>Apex angle of the cone</td>
</tr>
<tr>
<td>(\mu)</td>
<td>Coulomb friction coefficient</td>
</tr>
</tbody>
</table>

Along these equations a similar method to account for the main structural members in folding and crushing mode has been developed. In figure 2.19 a comparison of this methodology with the experimental results can be seen.

### 3.3. FEA Based Methods

The set-up of FEA based methods is in essence similar to that of the empirical methods. Using a Finite Element Analysis rather than an experiment or real life event is the distinguishing factor in the development of these methods.

**Simonsen et al (2009)** [50] developed an expression for the grounding resistance force and derived the grounding damage length from this horizontal resistance force.

\[
L_d = \frac{0.5MV^2}{F_H}
\]

(3.32)

The grounding length is determined using the horizontal resisting force of the double bottom. The horizontal grounding force formula is a FEA fit to a simplified expression:

\[
F_H = 0.41k_m \sigma_0 t_{eq}^{1.17} B_d^{0.83}
\]

(3.33)

or using the fracture strain rather than the material ductility parameter:

\[
F_H = 0.77 \sigma_0 \varepsilon_{f_{eq}}^{1.17} B_d^{0.83}
\]

(3.34)
With $t_{eq} = t + k_s \left[ \frac{A_s}{\sqrt{a}} + \frac{0.1T}{T_s} + \frac{0.1T_{ed}}{S} \right]$

<table>
<thead>
<tr>
<th>Symbol</th>
<th>Description</th>
</tr>
</thead>
<tbody>
<tr>
<td>$L_d$</td>
<td>Damage length</td>
</tr>
<tr>
<td>$M$</td>
<td>Vessel mass (displacement)</td>
</tr>
<tr>
<td>$V$</td>
<td>Vessel impact speed</td>
</tr>
<tr>
<td>$F_{ht}$</td>
<td>Horizontal grounding force</td>
</tr>
<tr>
<td>$k_m$</td>
<td>Material ductility parameter (steel: $k_m = 1$)</td>
</tr>
<tr>
<td>$\sigma_0$</td>
<td>Average between yield and ultimate tensile stress $\sigma_0 = \frac{\sigma_{YS} + \sigma_{UTS}}{2}$</td>
</tr>
<tr>
<td>$B_d$</td>
<td>Width of the damage</td>
</tr>
<tr>
<td>$\epsilon_f$</td>
<td>Fracture strain</td>
</tr>
<tr>
<td>$t$</td>
<td>Plate thickness</td>
</tr>
<tr>
<td>$k_s$</td>
<td>Effectiveness ratio for bottom stiffening</td>
</tr>
<tr>
<td>$A_s$</td>
<td>Cross sectional area of longitudinal stiffening</td>
</tr>
<tr>
<td>$s$</td>
<td>Spacing between longitudinal stiffening</td>
</tr>
<tr>
<td>$T$</td>
<td>Design draught of the vessel</td>
</tr>
<tr>
<td>$t_t$</td>
<td>Thickness of transverse stiffeners</td>
</tr>
<tr>
<td>$l$</td>
<td>Frame spacing</td>
</tr>
<tr>
<td>$t_g$</td>
<td>Thickness of longitudinal girders</td>
</tr>
<tr>
<td>$S$</td>
<td>Spacing between longitudinal girders</td>
</tr>
</tbody>
</table>

The aim of these formulations is to combine these simplified formulas with a stochastic methodology to create a probabilistic method for the crash performance of vessels, in particular high speed craft. This can in turn be used to develop crash-performance based class rules for damage stability requirements.

Validation of these relatively simple formulae is done by comparing the results with extensive FEA simulations of various vessels as well as comparing the results with the NSWC crash test results in figure 3.5 (see section 2.1.1). The methodology seems to compare well with these specific test results. It is concluded that these results make the method "useful for the development of more rational damage stability rules for high speeds craft as well as conventional ships." [50]

Heinvee & Tabri (2015) [21] derived a set of simplified formulas based on a series of numerical simulations conducted with tankers of varying dimensions and altering the penetration depths and rock topologies. They derived this formula by combining the results of a series of numerical grounding simulations.

$$A(a, \delta, h_{db}) = \begin{cases} \frac{4}{3} \sqrt{a} \delta^{(\frac{2}{3})} & \text{for } \delta \leq h_{db} \\ \frac{4}{3} \sqrt{a} \left[ \delta^{(\frac{2}{3})} - (\delta - h_{db}^{(\frac{2}{3})}) \right] & \text{for } \delta > h_{db} \end{cases}$$ (3.35)
\[ P^i_H = P^i A = \frac{\varepsilon^i_T}{2} (1.8 \cdot 10^{-3} a^2 - 7.4 \cdot 10^{-2} a + 1.2) A \] (3.36)

Table 3.1: Structural resistance coefficient \( c_T^i \) values for various tankers [22]

<table>
<thead>
<tr>
<th>Tanker</th>
<th>( c_T^i )</th>
</tr>
</thead>
<tbody>
<tr>
<td>T120</td>
<td>1.42 \cdot 10^6</td>
</tr>
<tr>
<td>T190</td>
<td>1.44 \cdot 10^6</td>
</tr>
<tr>
<td>T260</td>
<td>1.92 \cdot 10^6</td>
</tr>
</tbody>
</table>

\[ l_{dam} = \frac{(1 + m_{a,x}) \Delta v^2}{2F_H^i} \] (3.37)

- \( A \): Contact area between rock and ship
- \( a \): Rock size parameter
- \( \delta \): Vertical penetration depth
- \( h_{DB} \): Double bottom height
- \( F_H \): Average grounding force
- \( p^i \): Function of contact pressure for ship \( i \)
- \( l_{DM} \): Damage length
- \( m_{a,x} \): Surge added mass
- \( v \): Ship’s speed

These formulas were combined with an estimate for the damage width based on the paraboloid rock topology used. A lower bound failure criterion for the inner bottom was determined as \( \delta \geq 1.05 h_{DB} \).

By comparison with numerical simulation this simplified approach performs well for penetration depths above 0.5m. The deviation is reported to be less than 25% to the numerically derived values. The main drawback is the limited number of ship geometries used for the derivation. This is particularly important when evaluating the structural resistance coefficient \( c_T \).

### 3.4. Application

These analytical or empirical methods are often applied during the design of ships or for regulatory processes such as probabilistic risk assessments. For these purposes experiments do not suffice and simulations using FEA are too time consuming. Analytical methods are characterized by their simple and closed form and a fixed set of parameters, making them ideal for variation of the parameters and calculating many different scenarios. An example of such a method is MARCOL, developed by MARIN in the Netherlands.

**MARCOL** [12] is a collision damage tool that can be used for quantitative risk analysis for LNG import terminals. It can perform a safety assessment for new LNG terminals based on calculation of the risks for many scenarios. It combines the probability of an accident with the probability of a certain damage (tank rupture and LNG outflow). The probability of an accident is realized through coupling with a nautical database tracking the traffic movement in harbours.

The extent of the damage is then modelled using several principles. First the analysis is split in external and internal mechanics. The external mechanics are governed by the approach developed by Zhang (1999) [61] based on the conservation of momentum with restricted in-plane motions (surge, Zway and Yaw only). The internal mechanics are again split in the striking vessel and the struck vessel. The striking vessel is modelled using several predefined bow shapes and applies the bow crushing formulas developed by Pedersen et al (1993) [38]. The struck vessel is modelled using super elements that employ several of the formerly mentioned analytical formulations for failure of structural members: Zhang (1999) [61], Paik & Pedersen (1996) [36], Amdahl (1983) [8], Wierzbicki & Thomas (1993) [59] and Kaminski (1992) [26]. Figure 3.6 shows the simulation of a ship collision in MARCOL.
A number of analytical, empirical or FEA based grounding damage estimation methods have been presented in this chapter. A summary of these methods has been provided in table 3.2.
### Table 3.2: Comparison of discussed empirical and/or analytical methods

<table>
<thead>
<tr>
<th>Method</th>
<th>Type</th>
<th>Damage model</th>
<th>Description</th>
</tr>
</thead>
<tbody>
<tr>
<td>Cutting experiments</td>
<td>Empirical</td>
<td>Cutting force/energy</td>
<td>Several closed form solutions. Varying (but limited) applicabilities and validities.</td>
</tr>
<tr>
<td>Simonsen &amp; Lauridsen [48]</td>
<td>Empirical</td>
<td>Penetration energy</td>
<td>Experiments with thin plates (ensuring membrane action) combined with FEA and empirical model.</td>
</tr>
<tr>
<td>Zhang [62]</td>
<td>Analytical /Empirical</td>
<td>Cutting force and damage length</td>
<td>Combination of cutting experiments, analytical formulae and statistical regression analysis.</td>
</tr>
<tr>
<td>Wierzbicki &amp; Thomas [58]</td>
<td>Analytical</td>
<td>Cutting</td>
<td>Closed form solution for cutting resistance (first to take friction into account separately).</td>
</tr>
<tr>
<td>Simonsen [45][46]</td>
<td>Analytical</td>
<td>Plastic, crack and friction</td>
<td>Closed form solutions for various structural members. Validated with several experiments. Individual energy contributions not predicted very accurately.</td>
</tr>
<tr>
<td>Simonsen et al [50]</td>
<td>FEA</td>
<td>Damage length and grounding force</td>
<td>Usage in probabilistic grounding damage prediction. Validation with NSWC experiments.</td>
</tr>
<tr>
<td>Heinvee &amp; Tabri [21]</td>
<td>FEA</td>
<td>Damage length and grounding force</td>
<td>Various rock topologies and penetration depths. Derived for oil tankers &gt; 120m. Only validated using FEA.</td>
</tr>
</tbody>
</table>

These analytical, empirical or FEA based are easy in their use. They often give a reliable idea of the forces and energies involved with a grounding incident and most of the methods give a proper indication of the extent of damage as well. Without using a finite element analysis or extensive experiments these are proper tools. They are usually easy and understandable, which makes them fast in their use. It also makes them of very good use in the design stage of vessels or for probabilistic damage assessments either in design or during regulatory assessments.

However, there are some difficulties with these methods. Most of them are based on either simplistic assumptions, or a limited set of experiments or real situations. This makes them difficult to apply on any ship type and decreases the accuracy of the methods when used in a real accident situation. Due to tuning of the parameters it is often not entirely clear for which types of vessels or situations these methods do apply. Simonsen (1997) [46] (one of the most advanced methods developed) states that his methods does not predict well the energy contributions of plasticity and friction separately but the total energy dissipation compares very good with specific experimental results. However, the essence of his theory is that a breakdown of the energy dissipation is analytically derived in order to predict the separate contributions more accurately.
The FEA based methods have another drawback on top of those already mentioned. Depending on the validation, they are derived from a finite element analysis. It will be discussed in chapter 4 that these methods also have a certain inaccuracy. The FEA based method might therefore introduce a method with a certain inaccuracy based on inaccurate FEA results.

In general one needs to know all parameters in order for the methods to work. Proper assumptions can be made in the design stage of the vessel or for regulatory purposes. During a grounding accident these parameters are usually unknown. Any assumption on these parameters will likely result in a larger inaccuracy than any of the involved aiding parties is willing to work with. In general this makes these kind of methods not useful in a real grounding accident.
FEM Analysis

Finite Element Models (FEM) or Finite Element Analysis (FEA) is a very powerful tool to analyse any kind of structural problem. The internal mechanics and even the external mechanics in case of a coupled analysis, can be very conveniently modelled using FEA. Since the 1990’s the computational possibilities have increased significantly and many researchers in the field of maritime crash and grounding analysis have been developing FEA simulations and methods to be able to model, assess and predict the phenomena that occur during grounding. One of the early examples is the FEM made at TNO in order to assess their TNO-ASIS grounding experiments (section 2.1.2). Later, Kitamura (2002) [27] has been one of the adopters of early research into FEA for crash and grounding analysis.

This chapter will discuss some of the latest efforts in grounding analysis by FEM. These have to do with either simplifying models in order to use less time and computational capacity or with improving the accuracy of the grounding model, for instance by coupling internal and external mechanics. One of the key problems, modelling the structural failure by defining failure criteria, will be specifically addressed in this chapter as well.

4.1. Simplifying Models

Simplifying models refers to research efforts to make models less complex and to be able to use larger elements in FEA without losing accuracy. This is relevant because of increased simplicity of putting together the model, as well as reducing computational time which decreases significantly with reduced element sizes, especially for dynamic simulations. In this section a selection of papers has been made to reflect several efforts that aim towards simplifying FEA models for grounding analysis.

Paik & Pedersen (1996) [36] developed a method based in the Idealised Structural Unit Method (ISUM). The idea of ISUM is to model the structure with very large sized structural units and thus drastically reducing the number of degrees of freedom. In turn this reduces modelling efforts and computing times drastically. For the numerical analysis an idealised rectangular plate unit was used mainly (figure 4.1) with equally spaced stiffeners assumed. These plate units are capable of modelling non-linear loads such as buckling, yielding, crushing and rupture.
Along these rectangular plate units membrane tension plates are employed in several locations where only large in plane loadings are to be expected. To model the gap as a consequence of the rupture gap elements are used. To model the striking ship contact elements are used. The material model includes strain rate sensitivity according to Cowper-Symonds constitutive equations.

For verification the ISUM model has simulated two experiments, both indentation experiments of scaled double hull sections. The comparison with one of the experiments can be seen in figure 4.2. The ISUM method seems to correspond well to the experimentally obtained results. According to the authors this is also achieved with relatively short computing times.

Figure 4.1: Rectangular plate unit used by Paik & Pedersen (1996) [36] ● indicates nodes

Figure 4.2: Comparison of the ISUM FEA results with indentation experiments from Amdahl & Kavlie. Force / absorbed energy - indentation curve [36]
Nguyen et al (2011) [32] provided a simple procedure for estimating the damage to a ship bottom and to estimate the associated seabed topology. The simplification lies in the formulas for contact forces. With these contact forces the combination of a limited model of a ship section to estimate associated grounding forces which is combined with simple equations of motion for a rigid body to derive the response of the vessel in a grounding situation. This has the aim to create a dynamic simulation without the need of realizing the full coupling as usually seen in coupled (internal and external mechanics) analysis. These results have been compared with a self devised static incremental approach as can be seen in figure 4.3.

From figure 4.3 it is assumed that the proposed static procedure is determines energy and grounding forces well enough. Following this a procedure is proposed for identifying the bottom damage and the seabed topology. Various seabed topologies and penetration depths are calculated using the proposed procedure (figure 4.4).

Hereafter which the shape that minimizes the error in energy dissipation with the initial kinetic energy is selected as the most probable seabed topology combined with a penetration depth (figure 4.5).
The ultimate goal of this study is to provide a near real-time prediction of the risk of rupture of cargo tanks and hull girder failure. However, it is realized that the accuracy of these calculations become very questionable given the uncertainties related to any grounding event. These uncertainties mainly lie in the complicated seabed which is unknown more likely than not. Also determination of the stopping length and motion associated energies seem to be questionable. The iterative method itself is refreshing though. The methodology seems very promising and the opportunity to create several scenarios and choose the one with the smallest error seems to reduce at least some uncertainties. In light of the proposed methodology to predict the grounding damage of a ship this method might prove to be a good direction.

Ehlers & Tabri (2012) [18] published work which is strongly related to the earlier mentioned article from Heinvee & Tabri (2015) [21]. Using numerical quasi-static collision simulations to estimate the non-linear behaviour of the ship’s structure provides a force-penetration curve. These curves for several striking locations are subsequently used to calibrate semi-analytical formulas which are then used to estimate damage length en penetration (figure 4.6).
It is argued that this approach does not satisfy for non-symmetric collision or grounding scenarios due to the averaged structural behaviour. However, this uncoupled approach is capable of predicting the symmetric collisions and grounding damage sufficiently well. It should be noted that such a semi-analytical approach always reflects a certain scenario with predefined parameters, making this only applicable to similar scenarios as well. The decrease in computational times is however very encouraging.

Figure 4.7: Force-penetration curve obtained with the semi-analytical approach. [18]

Yu & Amdahl (2016) [60] proposed a similar method as Nguyen et al (2011) [32] for coupling rigid body dynamics to a grounding situation. They used a load subroutine in the FEA program LS-DYNA to implement standard equations of motion based on Newton’s law for all six degrees of freedom. Then
running several simulations the motions of a vessel during grounding was studied according to the FEM simulations with implemented dynamic coupling.

Figure 4.8 shows the motions and the associated grounding forces for three of the examined cases of grounding. The angle indicates the slope angle for the grounding (so 75° being more of a collision than a real grounding). It can be observed that the peaks in the grounding forces are not caused by oscillations of rigid body modes (especially pitch) of the vessel, but rather by structural elements passing the rock obstruction.

4.2. Improving Accuracy

Improving accuracy refers to researchers that are searching to improve the accuracy of failure and damage prediction in grounding analysis. Often this is done without explicitly considering efficiency or computation times. This does not imply that reducing these is not an improvement in itself, however it is found that improving accuracy in numerical modelling often means that details in the structural model needs to be included, which in turn decreases efficiency and computational times.

The largest part of increasing accuracy finds its place in improving prediction of failure in an FEA. This part will be discussed in section 4.3.

Alsos et al (2009) [7] provides a comparison between two of the most used fracture criteria in FEA of maritime crash and collision analysis. It is the second paper that reproduces the experiments published in [5] by using two different failure criteria: the RTCL damage criterion and the BWH instability criterion. These failure criteria will be discussed in detail in section 4.3.
4.2. Improving Accuracy

(a) Simulations using the BWH instability criterion.  
(b) Simulations using the RTCL damage criterion.

Figure 4.9: Force-Indentation graph for an unstiffened panel from experiments [5] comparing two different failure criteria with various mesh sizes. [7]

The comparison for these two different failure criteria included a mesh size sensitivity study. In figure 4.9 the comparison between the two it can be seen that both failure criteria are conservative, and the RTCL criterion appears to be mesh-size sensitive. This is explained by the adopted failure scaling law for the RTCL criterion which takes the element size into account. The scatter seems to be explained by the scaling rather than the RTCL criterion itself. In other simulations both criteria showed good results and predicted failure of the panels satisfactorily.

Although recognizing that coarse meshes are often used in maritime crash and grounding analysis it is concluded that as small elements as possible should be used in order to capture strain concentrations well enough. Several alternatives are mentioned to be able to capture local necking, fracture and excessive straining in coarser meshes. In general the BWH criterion works good and needs little input parameters, whereas the RTCL criterion yields more accurate results but also requires far more input parameters.

AbuBakar & Dow (2013) [4] conducted a similar exercise as Alsos et al now adopting the converged mesh of 15mm element size with a scaled RTCL failure criterion for an entire ship double bottom section. These results were compared with a FLD failure criterion (see section 4.3).

Figure 4.10: Comparison of FEA simulation (A) with experimental results (B) from the NSWC large scale grounding experiment. [4]

In general the failure mechanisms observed correspond with the experiment conducted (figure 4.10). Also the force displacement plots (figure 4.11) correspond very well with the experimentally obtained values.
These results show that FEA has become a very proper tool to model grounding accidents. However, the mesh size chosen (15mm per element) is rather small. This makes modelling effort and computation times significantly large for these explicit dynamic simulations. In many situations this is not desirable and often mesh sizes are chosen much larger. It was also found that the estimated onset of material rupture is very sensitive to the material failure model adopted.

4.3. Failure Criteria in FEA

The prediction of fracture in FEA is one of the most relevant research topic regarding Finite Element Analysis for maritime crash of grounding accidents. Calle & Alves presented a very complete and comprehensive review of all material failure models used and put this as follows:

"One of the major challenges in modelling the collapse of naval structures is the formulation of an adequate failure criterion which conciliates the micro-scale physical aspects associated to crack initiation in ductile materials with the mandatory use of large shell element sizes in the large-scale naval structure models." [15]

The basic idea of a failure criterion is that once the stress or strain in an element of the ship model exceeds a certain limit, this element is deleted from the model for the next load step. This limit is determined by the failure criterion. There are various types of failure criteria either based on the strain state, which is the most obvious choice in analysing plastic behaviour, or based of the stress state of the element. In order to improve the accuracy of failure criteria bi-axial or tri-axial strain or stress states have been developed. In general the failure criteria can be subdivided into simple strain based criteria, Stress tri-axiality based criteria and Forming-Limit-Diagram based criteria.

In this chapter several of the most used failure criteria in maritime crash and grounding analysis will be presented. A comparison between two often used criteria (RTCL and BWH criteria) is presented in Alsos et al (2009) [7] and a benchmark study of several failure criteria is provided by Ehlers et al (2008) [19]. A more thorough review of all failure criteria used can be found in Calle & Alves (2015) [15].

4.3.1. Strain Based Criteria

Strain based criteria are the most common criteria used in ship grounding analysis. Local material rupture is assumed when the equivalent plastic strain reaches a a predefined critical value often called failure strain.

GL- criterion is an often used criterion. Failure strain in material certificates is often based on uniaxial tensile testing. Using these test results and accounting for the size of the elements in FEA (which
are assumed to be square) equation (4.1) has been developed and is known as the GL criterion.

\[ \epsilon_f(l_e) = \epsilon_g + \epsilon_e \frac{t}{l_e} \]  

\[ \epsilon_f \] Failure strain  
\[ \epsilon_g \] Structural uniform strain  
\[ \epsilon_e \] Necking strain  
\[ t \] Element thickness  
\[ l_e \] Element edge length

For ordinary shipbuilding steel several values for \( \epsilon_g \) and \( \epsilon_e \) have experimentally been determined of which a couple from engineering standards are shown above. After the failure strain has been exceeded, the element is deleted from the model in most FEA procedures.

### 4.3.2. Stress Tri-Axiality Based Criteria

Stress tri-axiality (equation (4.2)) is a commonly used parameter to describe a state of three-dimensional stress.

\[ \eta = \frac{\sigma_m}{\bar{\sigma}} = \frac{\sigma_1 + \sigma_2 + \sigma_3}{3\bar{\sigma}} \]  

\[ \eta \] Stress tri-axiality  
\[ \sigma_m \] Hydrostatic stress  
\[ \bar{\sigma} \] Von Mises stress or equivalent stress  
\[ \sigma_1, \sigma_2, \sigma_3 \] 1st, 2nd and 3rd principal stresses

The RTCL criterion \(^{(52)}\) is a failure criterion that combines the Rice-Tracey and the Cockcroft-Latham failure criteria. The idea is that this combined criterion is capable of predicting failure over a large range of tri-axialities. The Cockcroft-Latham criterion works good for ductile shear fracture (low tri-axiality range) and the Rice-Tracey works good for void growth in (high tri-axialities).

\[ \dot{D} = \begin{cases} 0 & \text{for } \eta < -\frac{1}{3} \\ \frac{\sigma_m}{\bar{\sigma}} \dot{\varepsilon}_{eq} & \text{for } -\frac{1}{3} \leq \eta < \frac{1}{3} \\ \exp\left(\frac{3n-1}{2}\right) \dot{\varepsilon}_{eq} & \text{for } \eta \geq \frac{1}{3} \end{cases} \]  

\[ D = \frac{1}{\epsilon_{cr}} \int \dot{D} dt \]  

\[ \dot{\varepsilon}_{eq} \] Rate of equivalent plastic strain  
\[ \epsilon_{cr} \] Critical equivalent plastic strain in uni-axial tension

This criterion is to be scaled using a similar scaling relationship as the strain based failure criterion which can be used in combination with uni-axial failure strain test results.

\[ \epsilon_{cr} = n + (\epsilon_n - n) \frac{t}{l_e} \]  

\[ n \] Power law (strain hardening) exponent \( (\sigma = k\epsilon^n) \)  
\[ \epsilon_n \] Failure strain from uni-axial tensile testing
Extended Mohr-Coulomb criterion derived by Bai & Wierzbicki (2010) [11] is a representation of the failure in an extended range of tri-axialities. The failure criterion is a function of stress-tri-axiality ($\eta$), Lode-angle ($\bar{\theta}$) and failure strain ($\bar{\varepsilon}_f$) and can be determined by extensive material testing. The result is a failure surface as a function of aforementioned parameters which can be seen in figure 4.12.

This failure criterion is very theoretical and involves many different parameters. For practical use in crash analysis it has not yet been synthesized in a generally accepted form. Above all, the derivation of these failure surfaces for various tri-axialities and Lode-angles involves extensive testing which makes the failure criterion impractical in its use for crash and grounding analysis.

4.3.3. FLD Based Criteria

The Forming Limit Diagram (FLD) is widely used in sheet metal forming. It relates major strain with minor strain in metal sheet or plating during forming. The Forming Limit Curve (FLC) then depicts the limiting strains that a sheet metal can undergo under various forming conditions before failure (yielding, necking, fracture, wrinkling, etc.). In metal forming the onset of necking or excessive thinning is usually considered as the forming limit. In figure 4.13 a typical forming limit diagram is depicted as well as several limit curves for various conditions.

The FLCs are commonly determined through experiments or testing according to standardised pro-
4.3. Failure Criteria in FEA

Various analytical methods to determine the FLC are available as well. In maritime grounding and crash analysis the use of the FLC or FFLC (Fracture Forming Limit Curve) is gaining popularity as a method to determine failure of plating. Abubakar & Dow (2013) [4] used the FLD to determine maximum deformation before the onset of necking. They used a formulation for maximum allowable strain based on formulations by Jie et al (2009) [24].

BWH criterion developed by Alsos et al (2008) [6] is an example of a oft used failure criterion in maritime grounding and crash analysis. This criterion is stress based instead of strain based. This has the advantage that it accommodates varying loading paths, whereas the regular FLD assumes proportional strain paths (i.e., the ratio between major and minor strain remains constant). The BWH failure criterion is then a combination of the Bressan & Williams shear stress criterion and the Hill instability criterion.

\[
\sigma_1 = \begin{cases} 
\frac{2K}{\sqrt{3}} \left( \frac{1+\frac{1}{2}n}{\sqrt{\beta^2 + \beta + 1}} \right)^{\frac{n}{2}} & \text{if } \beta \leq 0 \\
\frac{2K}{\sqrt{3}} \left( \frac{\sigma_0}{\sqrt{1 - (\beta^2)}} \right)^{\frac{n}{2}} & \text{if } \beta > 0
\end{cases}
\] (4.6)

Where \( \beta \) is the degree of bi-axial straining defined as \( \beta = \frac{\varepsilon_2}{\varepsilon_1} \approx \frac{\varepsilon_2}{\varepsilon_1} \).

\( \sigma_1 \) 1st principal stress

\( K \) Power law parameter (\( \sigma = Ke^n \))

\( n \) Power law (strain hardening) exponent (\( \sigma = Ke^n \))

\( \bar{n} \) Element dependent power law (strain hardening) exponent

\( \varepsilon_1, \varepsilon_2 \) 1st & 2nd principal strain rates

\( \varepsilon_1, \varepsilon_2 \) 1st & 2nd principal strains

4.3.4. Crack Propagation

Most of the failure criteria are meant for modelling crack initiation in shell elements. In general they work accurately for the removal of the first element but do not capture crack growth very accurately. One of the ways to accommodate crack growth more accurately is element softening or erosion. In this method the strength and stiffness characteristics of the element are incrementally decreased before deletion. Abubakar & Dow (2013) [4] employed such a method in conjunction with the RTCL and the BWH failure criteria. One of the latest suggestions is an extension to the BWH failure criterion.

An extension to the BWH criterion proposed by Storheim et al (2015) [51] involves a post-necking damage model in order to accurately capture failure of an element after the onset of necking without resorting to mesh-refinement. The key of modelling the post-necking behaviour is element erosion rather than element deletion after the onset of necking. This is achieved through the introduction of the damage function in equation (4.7).

\[
1 - D \equiv \frac{\varepsilon_1}{h} = \frac{\exp((1 - (-\beta))\Delta\varepsilon_1)}{1 + \frac{l_a}{\varepsilon_1} \exp((1 - (-\beta))\Delta\varepsilon_1) - 1}
\] (4.7)

\( D \) Material damage (reduced capacity of the shell section due to thinning)

\( h \) Current thickness of virtual neck

\( t \) Current element thickness

\( \beta \) Degree of bi-axial straining \( \beta = \frac{\varepsilon_2}{\varepsilon_1} \approx \frac{\varepsilon_2}{\varepsilon_1} \)

\( \frac{l_a}{\varepsilon_1} \) Ratio of element length vs. thickness

\( \Delta\varepsilon_1 \) Major principle strain

The result is of the element erosion is a more smooth transition from necking towards failure which is especially relevant when using the larger sized elements as is customary in crash and grounding analysis. During erosion (increase of the damage) additional strain will be introduced in the surrounding
elements as can be seen from figure 4.14a. The element is deleted after a critical strain in through-thickness direction is reached. Instead of introducing failure directly after the onset of necking failure is now introduced gradually resulting in a higher strain resistance (see figure 4.14b).

Figure 4.14: Graphical representation of the damage function and its effect on the FLD. [51]

The results of this extension to the BWH failure criterion seem to be more accurate than the original BWH failure criterion. The simulation of a uni-axial tensile test (figure 4.15) shows good correlation to experimental values and an increased accuracy in the prediction of failure.

Figure 4.15: Simulation of uni-axial tensile test using the BWH failure criterion with damage model (thick lines), BWH without damage model (thin lines) and simulation without damage or failure (dashed lines). [51]

4.4. Discussion

Despite all the research efforts of increasing both simplicity and accuracy of FEA it remains difficult to incorporate Finite Element Analysis into the rescue/response for actual grounding accidents.

Simplified models often employ larger element sizes or simplifications for rigid body motions. These effectuate less input effort and reduces computing times. They do tend to reduce the accuracy drastically and especially localised phenomena which introduce failure are not captured by these larger elements. An option to incorporate local failure is using element subroutines to predict these localised phenomena and use them to introduce failure. One example of such a method (not discussed in this literature review) is given by Dragt et al (2015) [17] who developed a way to incorporate necking in large
shell elements. However, until such methods are generally applicable for ship grounding analysis and
gain more accuracy independent of the validation method or model, these simplified methods simply
lack the accuracy for the rescue/response in a real grounding accident.

The more accurate finite element models are often large and require a lot of input data and structural
details increasing the accuracy of the analysis and simulations. The finer meshes and more advanced
element types are very well suited to capture many different failure mechanisms that might occur.
However, the costs in computational power and computing times are high. Especially in explicit dynamic
modelling of the ship grounding event, smaller elements reduce the time step and thus come with high
computational costs for explicit dynamic simulation. The modelling effort and large computation times
make these more advanced models not well suited for grounding analysis in the case of a real grounding
accident.

Despite the increase in accuracy of the finite element analysis it remains difficult to properly validate
and verify large models and new failure criteria. The amount of real accident data is very limited and
if present the data are generally not useful. The number of experiments performed is limited as well
(see section 2.6), especially taking into account the scaling difficulties encountered when considering
plastic deformation and crack growth. More experimental data would certainly benefit the development
of finite element analysis for ship grounding analysis.

Failure criteria for maritime crash and grounding analysis experience a strong development and are
discussed and debated in literature extensively. A lot of the ongoing research focuses on accurately
capturing the various failure mechanisms that occur during a grounding accident especially by incor-
porating 2 or 3 dimensional stresses and/or strains. These criteria often require numerous parameters
to be accurate and are therefore difficult to use in analysis of grounding where these parameters are
not readily available. Because of these practical drawbacks the most simple criterion of them all, the
maximum plastic strain, is used mostly despite lacking accuracy in some areas. The quest is to develop
an easy to calibrate failure criterion that captures all failure modes encountered in maritime crash or
grounding. The RTCL and the BWH criterion seem to be much used but the FLD approach has rising
popularity due to its simplicity.

In general FEA is a very powerful tool in grounding analysis. Experiments are expensive and analy-
tical methods lack both accuracy and generality. The development of more accurate methodologies
for grounding analysis continues and models get better with each new development. Even still FEA is
difficult to use in a real grounding scenario due to the extremely limited time available for the analysis
and the many unknowns regarding the casualty and its condition.
This literature review presented the state of the art regarding grounding analysis. Three main ways to analyse a grounding accident have been discussed: Experiments, analytical methods and finite element analysis. Each of the methods presented has certain advantages and disadvantages. In the introduction of this literature review a new method is proposed (section 1.4) to estimate the damage shortly after a grounding occurs. The proposed methods uses measurements of the global ship motions only. This discussion will also be in the light of this newly proposed damage estimation method.

Experiments are the most obvious way to analyse ship grounding. Several experiments have been conducted with the purpose of either understanding grounding in general or a specific phenomenon that occurs during grounding. The cost of grounding experiments prohibits large scales for the experiments and the few experiments using larger scales that have been conducted are not a full representation of grounding lacking either motion parallel or perpendicular to the plate. The little amount of experimental results also makes it difficult to validate or verify new methods or computational models. More experimental results specifically aimed at grounding of ships would be a great addition to literature and the development of ship grounding analysis.

Analytical & empirical methods have been developed extensively by numerous researchers. First because it was the only way to analyse ship grounding in the absence of FEA, later to be used in a design or regulatory set-up. Due to simplification or derivation from specific vessel types or ship structures the results are often difficult to interpret. The consequence is that most of these method do not attain the required precision for analysis of a real grounding accident. Also the general applicability to any kind of ship or ship structure is problematic. Lastly these methods often require a great amount of input in the form of parameters of the ship’s structure which are usually not readily available in reality. Overall these methods perform fine and give a proper idea of related forces, energies and deformations that occur during ship grounding events but they are not generally applicable for an estimate of any kind of grounding accident.

Finite Element Analysis (FEA) has proved itself over the past 15-20 years as a solid and reliable tool for ship grounding analysis. With the rise of computational power it has become easier to perform extensive simulations and accuracy of the results has increased. However, time is still a limiting factor for FEA. The time to build and run a complex and accurate model is far too long to be practicable in real grounding situations. Simpler models still lack the accuracy to predict localised failure mechanisms which are the driving factor behind more global failure. Furthermore the discussion on a reliable and practicable failure criterion has not closed yet. Some very accurate failure criteria have too many input parameters and/or require too much testing. Others are still used a lot because of their simplicity but actually lack the desired accuracy. The FLD approach seems to be best of both worlds in that respect. For either case the research towards failure criteria could do with more experimental results to be
able to validate and verify their analytical and/or numerical findings. In general FEA is undeniably a very powerful tool for ship grounding analysis that goes without the large cost of experiments but with largely increased accuracy compared to analytical methods. For real grounding accident analysis the time constraint is still pressing but it might be used beforehand to construct grounding resistance curves or acceleration time traces for several typical scenarios.

**Concluding** this discussion it can be said that recent research efforts in ship grounding analysis have focused around understanding the structural response in grounding and being able to model this more accurately either using simplified methodologies or very complex coupled analysis. No research in predicting or estimating the grounding damage in real grounding scenarios have been encountered. Therefore the proposed method for estimating grounding damage can be seen as an enrichment to current knowledge.
Research Questions

The research question for this thesis project will come in the form of a design brief for a method to estimate the damage after grounding from measuring the global motions of the vessel only. From the design brief one or several specific aspects will later be chosen as full subjects to study within the timespan of the graduation project.

6.1. Goal of this Thesis

The goal of this master thesis project is to see whether it is possible to estimate/predict the damage to a ship’s double bottom structure, from measuring the global motions of the vessel only. How can rupture of a 6 mm ordinary steel plate, in a raking damage scenario with a hard spherical object, be distinguished from acceleration data?

1. Which phenomena occur in such a raking damage scenario? And what is the influence of each of these phenomena?
2. How is failure of the plate initiated? And what is a practical but accurate way to predict plate failure?
3. What is the ratio, in terms of energy dissipation, in which these phenomena occur?
4. What is the role of friction in a raking damage scenario?

6.2. Plan for graduation project

The focus will lie on local failure and the failure criterion associated with grounding damage. By conducting experiments the local failure of a plate subjected to grounding damage will be studied and compared with FEA simulation. Following this procedure a new simulation of the TNO-ASIS experiments (see section 2.1.2) will be done to compare the simulation with the recorded experiment results. In short:

1. Design of experiments
2. Conduct experiments
3. Compare experiment data with FEA simulation
4. Perform FEA simulations for large scale experiments (TNO-ASIS experiments)
5. See whether the work fits in the bigger picture of the proposed damage estimate method
Bibliography


