SHIP VULNERABILITY TO FLOODING

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ABSTRACT

Design of a safe ship is by far the most fundamental goal of a naval architect. Determination of what is safe, however, has been the subject of centuries-long and relentless endeavours by the profession as well as relevant lobbies representing diverse interests and public concerns. Flooding of ship void spaces and thus possibly rapid depletion of the physical basis providing a ship with stability has been one of the major vulnerabilities, protection against which has been regulated by ever-more advanced standards to ensure a safe design. However, even the latest of regulatory instruments to address damaged ship stability, due to be adopted in January 2009, is fraught with questions as to whether it constitutes a "sufficient" standard. Attempting to provide some answers to these questions, this paper presents a case study of a modern vessel with large undivided spaces and long lower hold, which is used as a basis for examining the meaning of damaged ship stability standards and for making recommendations for careful reflection by IMO.

INTRODUCTION

The first recorded attempts to express principles of stability were made by Archimedes ca 250 B.C., [1][2]. However, the first attempts to crystallize these principles were only made in the 17th/18th century, when a concept of ship stability measure, the metacentric height, or **GM** as it is known today, was introduced by Paul Hoste in 1698, [5][2][3]. This concept was later elaborated further in a more widely acknowledged exposition by Pierre Bouguer, [6], who introduced in 1746 the actual term "metacentre". Leonhard Euler focused in 1749 on the righting moment at a particular angle of heel as a better measure of stability, [7], but it was George Atwood who eventually demonstrated in 1798 that such measure can be derived for any angle, [8], inventing thereby the **GZ** curve.

Such a progress were these works perceived as at those times that Augustin Creuze stated in 1841 that *"the labours of numerous men of science … have left but a few of the abstract principles [of the theory of the ship] un-investigated"*.

However, ships have continued to be lost to this day, still, as a result of insufficient stability.

A case to note was the HMS CAPTAIN in 1870, Figure 1, as it was her loss that has so clearly highlighted the inability of the GM to reflect upon vessel stability sufficiently comprehensively.

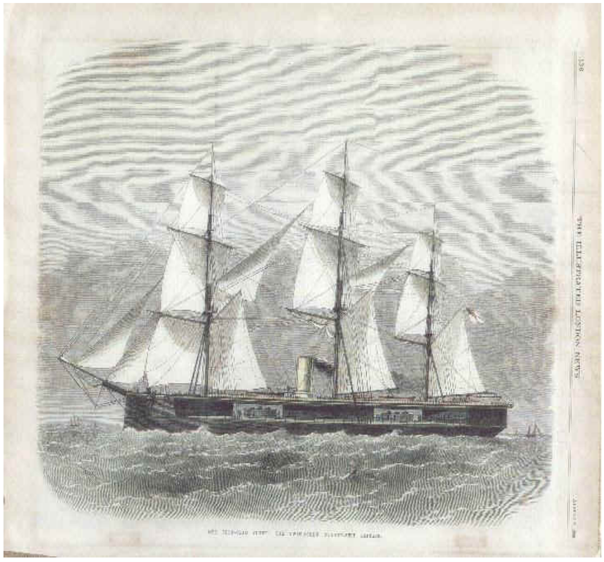

Figure 1 HMS Captain, 97.5m, commissioned on 30th April 1870 and lost on 7th September 1870 with 480 lives perished, due to inadequate (range of) "stability" as found by The Court Martial[1].

It seems that not until the work of Jaakko Rahola in 1939, [10], had any more advanced measures of stability seriously been considered, even though the **area** under the GZ curve was proposed by Canon Moseley to be a better measure of ship stability already in 1850, [4].

[1] *"The court, having heard the statement, …, and having deliberately weighed and considered the whole of the evidence before them, do find that Her Majesty's Ship Captain was capsized on the morning of the 7th September 1870 by pressure of sail, assisted by the heave of the sea, and that the sail carried at the time of her loss was insufficient to have endangered a ship endued with the proper **amount of stability**. … It further appearing in evidence, ..., that her **stability proved to be dangerously small**, …"*, 1870, www.wikipedia.com

In fact, it seems that the proposition by Rahola proves to have been a turning point in the history of stability quantification, as his approach survives to this day, [11]. His use of GZ curve properties to quantify stability are the core of most modern criteria of stability for a ship in damaged state, [20], [18]. This subtlety can easily escape attention, since the overall framework of stability assessment of a damaged ship, based on the Kurt Wendel's concepts of probabilistic index of subdivision **A**, [16], [17], is rather a complex mathematical construct, with the basic details not discernible. The framework is also a major step-change in the philosophy of stability standardisation.

However, it seems that such an implicit reliance on Raholas measures is a major obstacle for practical disclosure of the meaning of stability standards, as no common-sense interpretations are possible, regardless of the acclaimed rationality of the overall framework. Rahola himself has stressed: *"When beginning to study the stability arm curve material … in detail, one immediately observes that the quality of the curves varies very much. One can therefore not apply any systematic method of comparison but must be content with the endeavour to determine for certain stability factors such values as have been judged to be sufficient or not in investigations of accidents that have occurred*". Today's standards are not that different, the profession seems to be content with simply a comparative criterion, whereby a required index **R** is put forward as the acceptance instrument (ultimately as a stability measure), without clear explanation as to what is implied if the criterion is met.

In essence, the question is: what does **A=R** mean?

This article sets to explore the meaning of current damaged ship stability standards, by re-examining the relationships between the sea environment, ship design/operation parameters, the random nature of damage occurrences and the random process of eventual ship capsize when damaged.

TIME TO CAPSIZE

As a first step in this exploration, let a hypothesis be put forward that there exists a function defining how the probability mass is distributed for joint occurrence of events of loading $W = w$, flooding extent $D = d$, environment $E = e$ and time to capsize $T = t$, and let this function be denoted as:

$$p_{W\&D\&E\&T}(w \cap d \cap e \cap t) \quad (1)$$

The event $w \cap d \cap e \cap t$, can also be referred to as a "compound event" or a "**scenario**", and is regarded as **un**conditional[2].

[2] by "unconditional" is implied unconditional on any of the four events of loading W, flooding, D, environment, E, or time to capsize, T, with that the only underlying condition being the occurrence of a collision with hull breach event

Considering now that the events of loading and flooding extent can be considered independent, as well as applying the chain rule of probability calculus (Bayes theorem), allows for expressing equation (1) as follows:

$$\begin{aligned} & p_{W\&D\&E\&T}(w \cap d \cap e \cap t) = \\ & = p_W(w) \cdot p_D(d) \cdot p_{E|D}(e|d) \cdot p_{T|W\&D\&E}(t|w \cap d \cap e) \equiv \\ & \equiv p_W \cdot p_D \cdot p_{E|D} \cdot p_{T|W\&D\&E} \end{aligned} \quad (2)$$

The mass function of **un**conditional[2] probability that an event of time to capsize t occurs can be obtained by marginalization, as follows:

$$\begin{aligned} & p_T(t) = \sum_{\Omega \setminus T} p_{W\&D\&E\&T}(w \cap d \cap e \cap t) = \\ & = \sum_W \sum_D \sum_E p_W \cdot p_D \cdot p_{E|D} \cdot p_{T|W\&D\&E} \end{aligned} \quad (3)$$

Where $\Omega = \{W, D, E, T\}$.

The mass function of **un**conditional[2] probability that a joint event of specific loading condition $W = w$ as well as the time to capsize $T = t$ occurs can be obtained by marginalization, as follows:

$$\begin{aligned} & p_{W\&T}(w \cap t) = \sum_{\Omega \setminus (W\&T)} p_{W\&D\&E\&T}(w \cap d \cap e \cap t) = \\ & = p_W \cdot \sum_D \sum_E p_D \cdot p_{E|D} \cdot p_{T|W\&D\&E} \end{aligned} \quad (4)$$

The mass function of conditional probability that an event of time to capsize $T = t$ occurs, given specific loading $W = w$ occurred can be expressed as:

$$\begin{aligned} & p_{T|W}(t|w) = \frac{p_{W\&T}(w \cap t)}{p_w(w)} = \\ & = \sum_D \sum_E p_D \cdot p_{E|D} \cdot p_{T|W\&D\&E} \end{aligned} \quad (5)$$

The mass function of **un**conditional[2] probability that a joint event of specific loading condition $W = w$, damage extent $D = d$, as well as the time to capsize $T = t$ occur can be obtained by marginalization, as follows:

$$\begin{aligned} & p_{W\&D\&T}(w \cap d \cap t) = \\ & = \sum_{\Omega \setminus (W\&D\&T)} p_{W\&D\&E\&T}(w \cap d \cap e \cap t) = \\ & = p_W \cdot p_D \cdot \sum_E p_{E|D} \cdot p_{T|W\&D\&E} \end{aligned} \quad (6)$$

The mass function of conditional probability that an event of time to capsize $T = t$ occurs, given specific loading $W = w$ as well as specific flooding extent, $D = d$, occurred can be found as:

$$p_{T|W\&D}(t|w \cap d) = \frac{p_{W\&D\&T}}{p_{W\&D}} = \\ = \frac{p_W \cdot p_D \cdot \sum_E p_{E|D} \cdot p_{T|W\&D\&E}}{p_W \cdot p_D} = \\ = \sum_E p_{E|D} \cdot p_{T|W\&D\&E} \qquad (7)$$

Let the notation convention (8) similar to that of the SOLAS Ch II set of regulations [18] be adopted. Let us also assume that the event of specific environmental condition occurring at the instant of a collision event is independent of the characteristics of the damage itself as shown in (8).

$$\begin{aligned} p_W(w) &= w_i \\ p_D(d) &= p_j \\ p_{E|D}(e|d) &= e_k \\ \frac{p_{T|W\&D\&E}(t|w \cap d \cap e)}{dt} &= c_{i,j,k}(t) \end{aligned} \qquad (8)$$

This allows equation (3) to be re-written as the following equation (9), as proposed by [24]:

$$f_T(t) = \sum_i^3 \sum_j^{n_{flood}} \sum_k^{n_{Hs}} w_i \cdot p_j \cdot e_k \cdot c_{i,j,k}(t) \qquad (9)$$

Where:

$$c_{i,j,k}(t) = -\ln(\varepsilon_{i,j,k}) \cdot (\varepsilon_{i,j,k})^{\frac{t}{t_0}} \cdot t_0^{-1} \qquad (10)$$

$$t_0 = 30 \text{ min} \qquad (11)$$

The terms w_i and p_j are the probability mass functions of the 3 specific loading conditions and n_{flood} number of flooding extents, respectively, calculated according to the harmonised probabilistic rules for ship subdivision, [18]. The term e_k is the probability mass function derived from (12) for the sea state Hs_k, where $0 < Hs_k \le 4m$, $Hs_k = k \cdot 4 \cdot n_{Hs}^{-1}$ and n_{Hs} is the number of sea states considered.

$$F_{Hs_{collision}} = e^{-e^{0.16-1.2 \cdot Hs_{collision}}} \qquad (12)$$

The term $\varepsilon_{i,j,k}$ (with σ_r) represents the phenomenon of the capsize band, Figure 2, that is the spread of sea states where the vessel might capsize. These can be estimated as follows:

$$\varepsilon_{i,j,k} = 1 - p_f = 1 - \Phi\left(\frac{Hs_k - Hs_{crit}(s_{ij})}{\sigma_r(Hs_{crit}(s_{ij}))}\right) \qquad (13)$$

$$\sigma_r(Hs_{crit}) = 0.039 \cdot Hs_{crit} + 0.049 \qquad (14)$$

Where $\Phi(.)$ is the cumulative standard normal distribution. The $Hs_{crit}(s)$ is calculated from equation (15). The s_{ij} is the probability of survival, calculated according to [18].

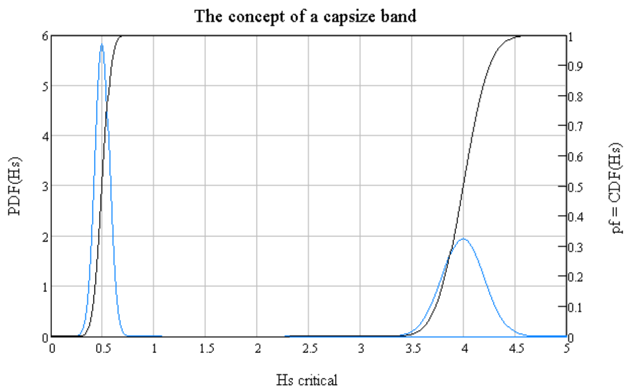

Figure 2 The approximated spread in the rate of capsizing, p_f, within a capsize band.

The relationship between the significant wave heights at the instant of collision, here equal to the critical sea state[3], and the "s" factors is given by equation (15), [20], which is derived from equation (12).

$$Hs_{crit}(s) = Hs_{collision}(s) = \frac{0.16 - \ln(-\ln(s))}{1.2} \qquad (15)$$

The cumulative probability distribution function for time to capsize (i.e. probability that capsize occurs within time t) can then be obtained as shown in equation (16):

$$F_T(t) = \int_0^t d\tau \cdot f_T(\tau) = \sum_i^3 \sum_j^{n_{flood}} \sum_k^{n_{Hs}} w_i \cdot p_j \cdot e_k \cdot \left(1 - \varepsilon_{i,j,k}^{\frac{t}{t_0}}\right) \qquad (16)$$

It is hereby proposed that equation (16), referred hereafter as UGD, [21] to [25], is used as the quantitative measure of ship stability, or indeed, **ship vulnerability to flooding**. The meaning of this measure is explained in the following chapters.

It is useful, however, to offer some validation of the model (16). This is based on e.g. assessments of $F_T(t)$ by independent expert judgment, as is shown in e.g. Figure 3, or experimental data available as shown in e.g. Figure 4.

[3] a sea state causing the vessel capsizing during about half of the 30minutes scaled model tests, the damage opening modelled was that known as SOLAS damage

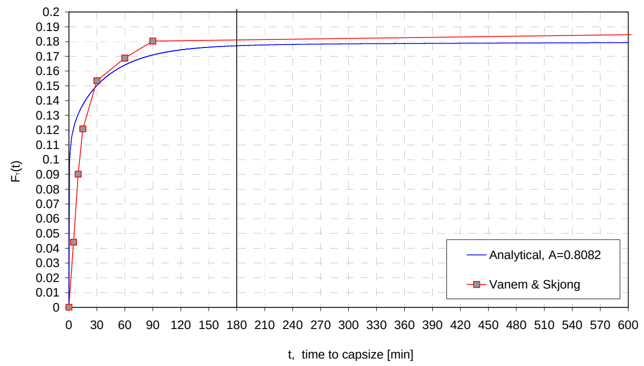

Figure 3 Comparison of the marginal probability of time to capsize after event of collision-and-flooding, assessed according to the method adopted in equation (16), denoted as “Analytical (A=0.8082)”, and the method based on expert judgement derived in [19] for A=0.8082, denoted “Vanem & Skjong”. The agreement is most remarkable considering the vast difference in methods employed.

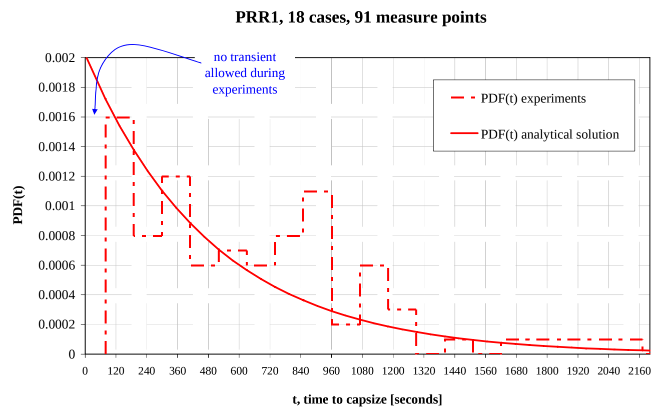

Figure 4 PRR1, probability density for time to capsize recorded during experiments. Comparison with model (10) for $p_f \sim 1.0$, $t_0 = 2160s$.

CASE STUDIES

The case study presented in the paper has been prompted by the expressed concern that a ship with long lower hold (LLH) could result in a rapid (catastrophic) capsize if the LLH were flooded, which is perceived as an unacceptable state of affairs, [27].

For this reason a sample passenger RoRo vessel PRR19, fitted with long lower hold, has been investigated, [27], to address this perceived vulnerability. Summary details of the vessel are given in Table 1 and Figure 5 and Figure 6.

Table 1 PRR19, ship particulars.

L_{bp}	157.65m
B	23.4m
Displ (1.025t/m^3)	14215.4 tonnes
Draught	6.0m
GM	1.439m
KG	10.83m
No of passengers	144
R (required index of subdivision)	0.682
A (attained index of subdivision)	0.682

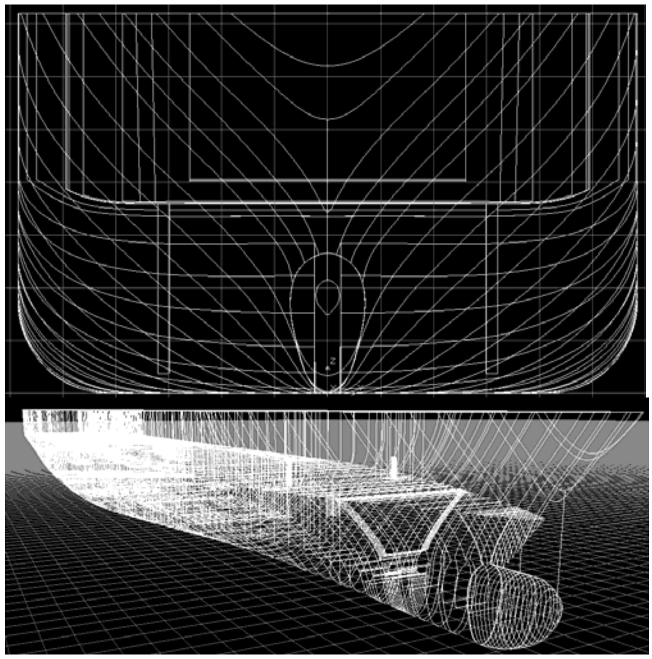

Figure 5 Overview of geometry of PRR19 (Ver C).

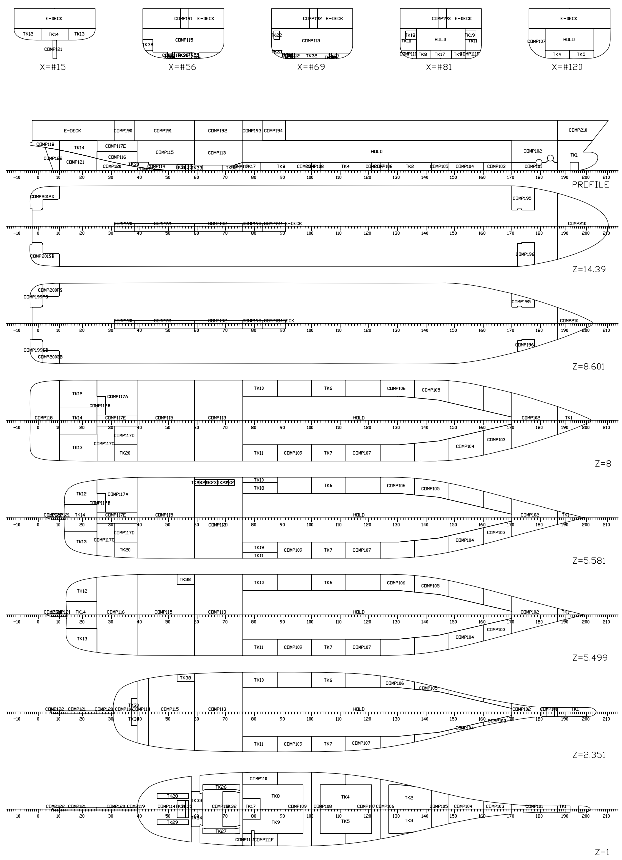

Figure 6 Subdivision of PRR19 (Ver C, “basis ship”).

Physical Model Experiments

The first element of the study comprised physical model experiments. The model was constructed in scale 1:40 by using GRP, plexiglas and foam. The shell of the hull was made of 3mm thick GRP. The skeg was modelled, but other appendages such as rudders, thrusters and fin stabilisers were not modelled.

A comparative study on the behaviour of the vessel in two damage cases, Dam1 and Dam2, with and without flooding of long lower hold, respectively, shown in Figure 7, has been undertaken.

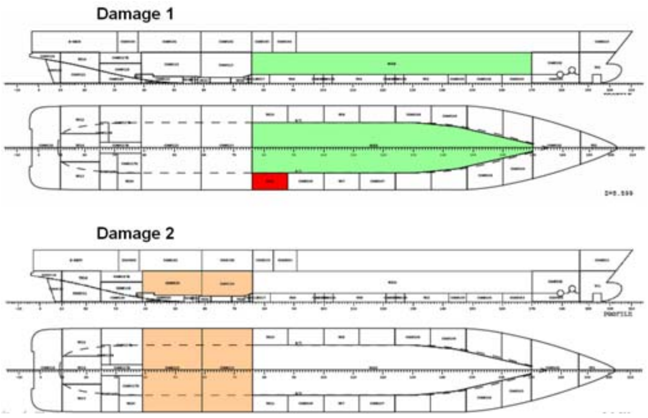

Figure 7 PRR19, two damage cases used for physical model testing.

The series of tests performed is summarised in Table 2 below, with a graphical comparison of roll records for both damages with Hs=1.5m shown in Figure 8.

Table 2 Experimental test matrix

	Significant Wave Height (m)	Result
Dam 1	1.25	SURVIVE
Dam 1	1.50	CAPSIZE
Dam 2	1.00	SURVIVE
Dam 2	1.10	SURVIVE
Dam 2	1.25	CAPSIZE
Dam 2	1.50	CAPSIZE

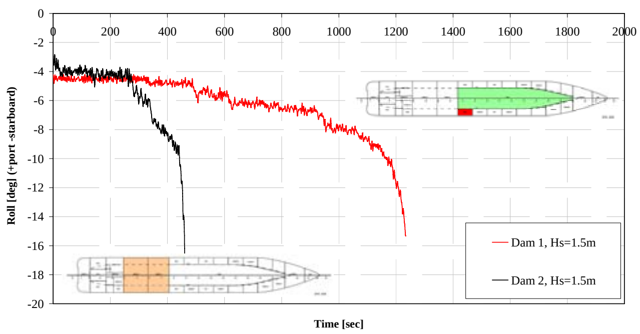

Figure 8 Comparison of the time series of ship roll response recorded for the two damage cases tested in a sea state with Hs=1.5m. Either damage is critical for those onboard. It is perhaps noteworthy that the flooding case without damage to LLH, i.e. the Dam 2 (p_i=0.04596) is some 92 times more likely to occur than Dam 1 (p_i=0.0005)!

As can be seen from Figure 8, either damage will result in a rapid ship capsize within 10-20 minutes from the instant of hull breach occurrence. These results serve to signify the importance of the original concern that have prompted this research in the first instance, namely occurrence of damage **scenarios**, see equation (1), leading to rapid capsize with possible substantial loss of life and which intuitively was only expected to occur when the long lower hold space of the vessel were flooded. As it appears from the two cases of Dam 1 and Dam2, such rapid capsize can result in flooding cases with the long lower hold remaining intact. In fact, it should be noted that of the two damage cases selected for experimentation, Dam2 is some 92 times more likely to occur than is Dam1.

Obviously the question still remains if "overall" such critical damage cases will involve more often flooding of the long lower hold, or that "overall" a comparative ship without long lower hold is less likely to capsize as a result of a collision breach than is a ship fitted with LLH.

To attain an understanding on this question, specifically pertinent to the mentioned and commonly used term "overall" the following analysis has been performed.

Study on time to capsize

Model (16) has been used to quantify the "overall" vulnerability of the ship to collision damage. Typical results of the calculation is the probability distribution for time to capsize t after flooding commenced, $F_T(t)$, see Figure 9.

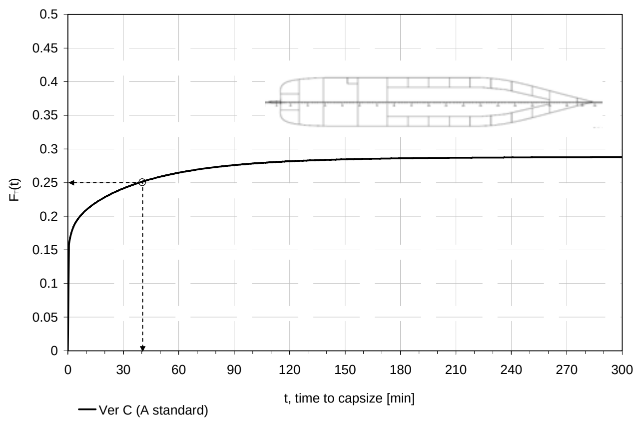

Figure 9 Distribution of **un**conditional[2] probability for time to capsize t, see equation (3) or (16). Interpretation example for $t = 40\,\text{min}$: consider that the PRR19 ship suffers a collision damage every year. $F_T(40\,\text{min}) = 25\%$ implies that every 4th year[4] the accident will be catastrophic, ending with the ship capsizing sometimes within 40 minutes. The notation Ver C (A standard) implies loading conditions from the limiting curve of SOLAS2009 requirements.

[4] This interpretation refers to the concept of the "mean recurrence interval", (1.0 / 0.25).

To further verify the above result, a systematic series of numerical simulations have been performed for loading case $W = DS$. The simulations were performed through a Monte Carlo scheme, with sampling from prior probabilities on damage and environment statistics given in [14], see e.g. Figure 10 and Figure 11. Based on up to 180 minutes time domain simulations of damaged ship response in waves using the PROTEUS3 code in this case, see Figure 12, it has been possible to construct the distribution of probability for the time it takes the vessel to capsize in either of the damages considered, and given loading DS occurred, $F_{T|W}(t|DS)$. In other words, equation (5) has been solved numerically. As is shown in Figure 13, e.g. some 250 capsizes have been observed among the 1000 simulation cases for 20minutes!

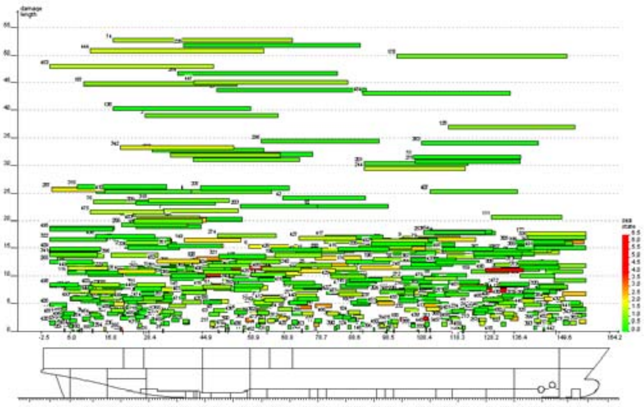

Figure 10 Sample of the first 500 out of 1000 MC simulations set-up, distribution of damage location, length and the sea state.

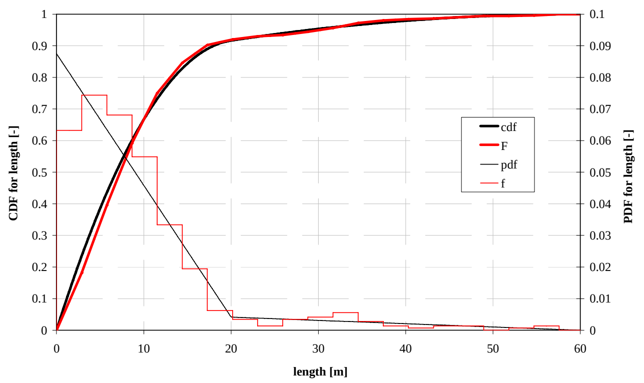

Figure 11 Set up of the first 500 MC simulations, probability distribution for damage length.

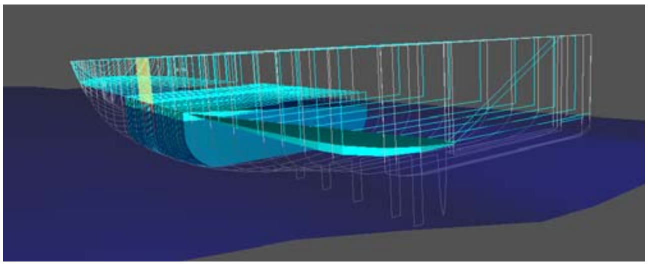

Figure 12 Sample of PROTEUS3, [15], simulated response in a damage case, water accumulation on the aft of the car deck.

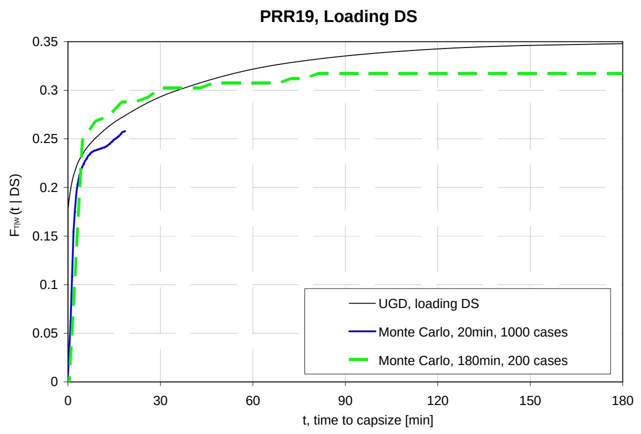

Figure 13 Cumulative distribution of probability for time to capsize <u>given</u> specific loading condition DS occurred, see equation (5).

On the basis of this verification, results of Figure 9 can now be considered representative of the “overall” vulnerability of the PRR19 ship, given the vessel was always operated in SOLAS2009 limiting loading conditions.

To reiterate, these results indicate that PRR19 is expected to capsize rapidly (say within <u>3 hours</u>) in approximately 29% (nearly 1 in 3!) of damage cases she might suffer[5].

Furthermore, as Figure 14 indicates, most, or ~95% of these critical damages would occur in loading with draft of 5.5m or more (DS and DP).

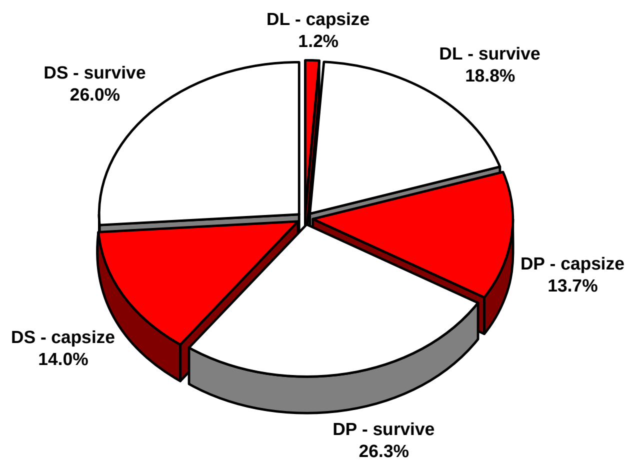

Figure 14 Distribution of **un**conditional[2] probability for time to capsize of 3 hours <u>and</u> loading conditions, see equation (4).

Furthermore, it can be seen from Figure 15 that many of these critical damage cases will be located in the aft part of the vessel without flooding of the LLH.

Obviously as many critical damages will also involve LLH, as shown in Figure 15.

[5] It seems that of any conceivable failure mode of any human made engineering system, ship capsizing is the only failure mode which is explicitly known to lead to catastrophic consequence and yet rate of its occurrence as high as 20-30% is accepted today.

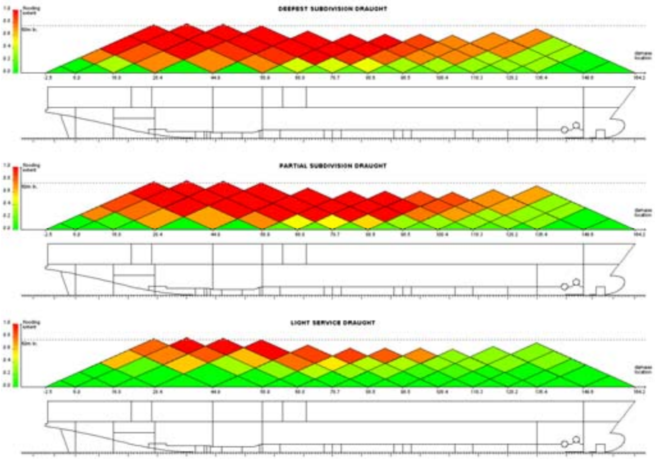

Figure 15 PRR19, distribution of conditional probability for time to capsize, given loading conditions and damage extent occurred, see equation (7).

Therefore, considering that PRR19 complies with IMO standards, it seems that currently it is accepted that such a ship is likely to capsize very rapidly in some one third of possible damage cases with potentially catastrophic consequences. If such catastrophic consequences are to be averted, then it is clear that <u>fundamental revision of the current regulatory requirements on ship stability standards must take place</u>.

Considering the above, a sensitivity study has been undertaken to establish the influence of various design/operational options to address this problem, and to highlight possible routes for provision of adequate [6] stability.

Design mitigation measures

Impact of subdivision

As mentioned above, the existence of a long lower hold on a ship gave rise to concern about the possibility of a rapid capsize. To investigate the impact of LLH and thus watertight subdivision on the "overall" vulnerability, a design is proposed whereby the lower hold of PRR19 is subdivided by transverse bulkheads, see Figure 17.

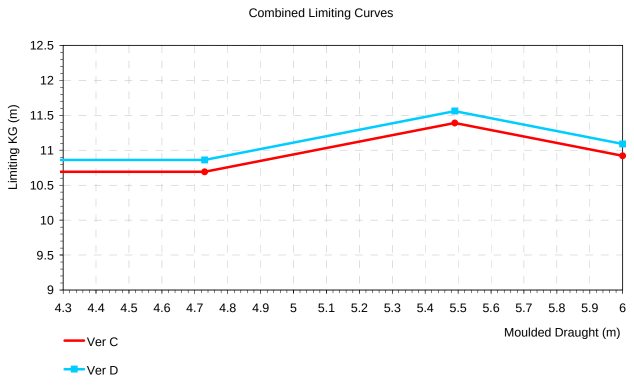

Figure 16 PRR19, "limiting curves" for compliance with SOLAS2009 regulations for Ver C and Ver D.

[6] obviously what is *"adequate"* is the key question yet to be answered

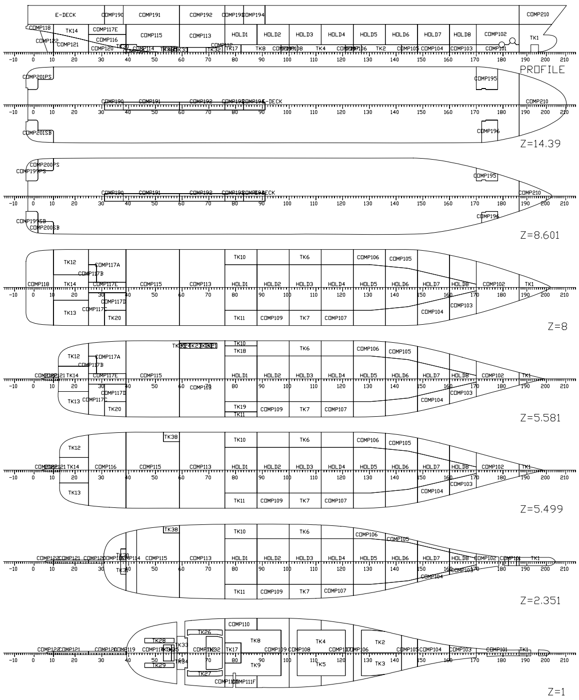

Figure 17 Arrangement of PRR19 after subdividing the long lower hold, Ver D.

The summary results from this study case are shown in Figure 18. It seems that a ship designed to a specific standard would display the same "overall" vulnerability irrespective of the specific subdivision solution.

This should not be too surprising, given that the new instrument for stability assessment SOLAS2009 has been derived with this principle in mind. Specifically, the required R is attained exactly in both cases, which is achieved by adjusting the loading conditions as shown in Figure 16.

This implies that a ship with a long lower hold is not inferior to a ship without a lower hold if both comply with the same standard.

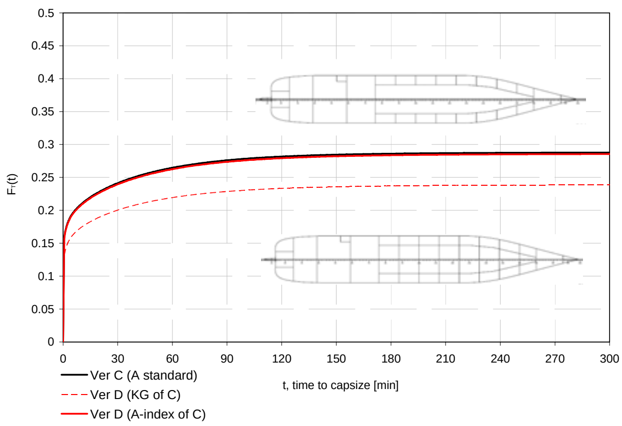

Figure 18 Distribution of **un**conditional[2] probability for t, see equation (3) or (16). Comparison between the basis ship, Ver C, and Ver D (KG of C) with loading from Ver C, see Figure 16, and Ver D (A-index of C) with loading to satisfy the requirements of SOLAS2009, $A = R = 0.682$. Despite different subdivision, compliance with the same standard implies a very similar "overall" vulnerability.

Impact of loading conditions

As discussed above, the manner of ship operation has quite an impact on the time to capsize, see for instance Figure 14.

The forthcoming SOLAS2009 instrument for standardizing how stability is assessed, [18], addresses this by stating that:

$$w_1 \cdot A_{DL} + w_2 \cdot A_{DP} + w_3 \cdot A_{DS} \geq R \quad \text{and} \quad A_i \geq 0.9 \cdot R$$

Or, more succinctly:

$$\sum_{i=1}^{3} w_i \cdot \left(\sum_{j}^{n_{flood}} p_j \cdot s_{ij} \right) = \sum_{i=1}^{3} w_i \cdot A_i \geq R \quad \& \quad A_i \geq 0.9 \cdot R \qquad (17)$$

However, such requirement leaves considerable flexibility to the designer in compensating poor stability in some, as found here, deepest, draft conditions with stability improvement in the lighter drafts. It is for this reason that a disproportional distribution of likelihood of rapid capsize between different drafts is obtained in Figure 14, despite the fact that the additional requirement of [18], namely that $A_i \geq 0.9 \cdot R$, is met.

There are two problems, both already mentioned above, such standard gives rise to. Namely:

(i) Firstly, as mentioned above and shown in Figure 14, it is clear that the ship is some **ten times as vulnerable** to rapid capsize at the deepest DS draught as it is at the lightest DL draught. This vulnerability is a result of two aspects:
   a. Design: lower level of survivability at DS than at DL (see equation (5)).
   b. Operation: greater frequency of operating at DS (40%) than at DL (20%).

(ii) Secondly, it might be that the ship is operated at DS even more frequently than the 40% of the time implied by the SOLAS2009 assumptions. This latter possibility implies higher likelihood to capsize rapidly than that assumed during design, i.e. higher than what is shown in Figure 9.

It is for this reason that it is proposed for a ship to be designed for either of the following design objectives.

(a) Constant conditional probabilities to capsize in either loading conditions[7].

It is proposed that it is required for the vessel to always be subject to the same likelihood of capsizing regardless of the draft conditions she operates at. It can be stated as follows (see also equation (5)[8]):

$$F_{T|W}\left(t|w\right) = const \qquad (18)$$

[7] Such requirement would ensure that survivability of the vessel is always exactly the same regardless of the draft conditions the vessel sails at. In such case the concept of factors w_i becomes redundant.

[8] Note that the term w is used here interchangeably as an event and probability

Assuming that $F_{T|W} \sim \left(1 - A_i\right)$ for time to capsize of some 3 hours, the above requirement (18) could be stated as $\left(1 - A_i\right) = const = \left(1 - R\right)$, that is as follows:

$$A_i \geq R \qquad (19)$$

To reiterate, equation (19) implies that $A_{DL} = A_{DP} = A_{DS} \geq R$. The practical consequence of this would be that the vessel would be subject to the same likelihood of capsizing after collision damage, regardless of the draft at which such collision would take place. Therefore, no possibility of compensating for poor stability in one draft with better stability in another draft conditions would exist.

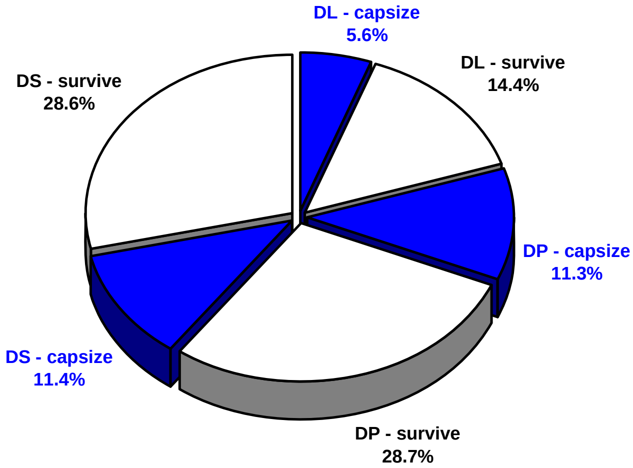

Figure 19 Distribution of **un**conditional[2] probability for time to capsize of 3 hours and loading conditions, see equation (4). The KG in each draft derived from the requirement that $F_{T|W}\left(t|w\right) = const$, enforced reasonably well through (19). Note that $F_{T|W}\left(t|w\right) = const$ implies that 5.6%/20% = 11.3% / 40% = 11.4% / 40%, see equation (5), with some round-off error allowance in the underlying calculations.

However, it would seem logical that the ship is provided with better stability standard at drafts at which she is operated more often. This gives rise to the following alternative design objective.

(b) The best stability at the draught at which the ship is operated more often.

This objective can be achieved by meeting the requirement (20), or after approximation, (21).

$$F_{T\&W}\left(t \cap w\right) = const \qquad (20)$$

Noting that $F_{T\&W}(t \cap w_i) \approx w_i \cdot (1 - A_i)$ for time to capsize of some 3 hours, the above can be expressed as follows:

$$w_i \cdot (1 - A_i) = const \quad \underline{\textbf{and}} \quad \sum_{i=1}^{3} w_i \cdot A_i \geq R \qquad (21)$$

When the assumptions of SOLAS2009 on loading distributions hold, i.e. the ship is operated at e.g. DS 40% of her operational time, i.e. $w_1 = 0.2, w_2 = w_3 = 0.4$, this requirement can be expressed as (22) for more straightforward application.

$$A_{DL} \geq \frac{R + 0.2}{0.6} - 1$$
$$A_{DP} = A_{DS} \geq \frac{R + 0.2}{1.2} \qquad (22)$$

After applying (22) to ship PRR19, the distribution $F_{T\&W}(3hrs \cap w_i)$ is obtained as shown in Figure 20.

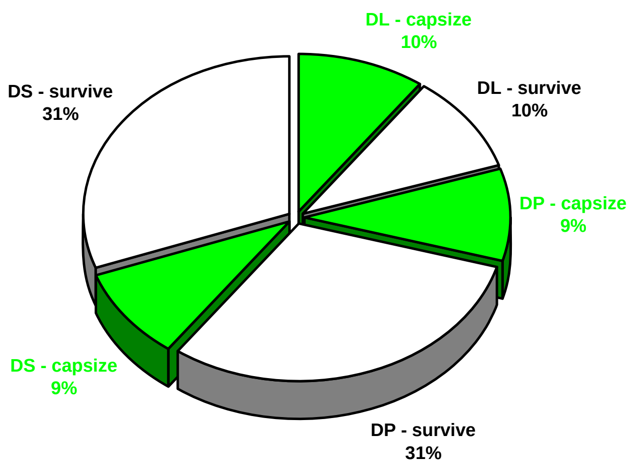

Figure 20 PRR19, distribution of **un**conditional[2] probability for time to capsize of 3 hours <u>and</u> loading conditions, see equation (4). The KG in each draft derived from requirement (22). Allow for some round-off errors in the underlying calculations.

To reiterate once again, practical implication of adopting standard (20) or its approximation (22), would be requiring proportionally higher stability in draft conditions at which the vessel operates more often. The "proportionality" is to reflect the frequency of operation at different draft conditions.

Figure 21 compares the limiting KG curves derived for the above discussed interpretations of the probabilistic method for assessing ship stability. It can be seen that the requirement (20) implies rather low KG value of below 10m at drafts of 6m, whilst it allows rather high KG at a draft of 4.73m. Note that the aggregate A index of 0.682 remains the same for each of these implementations and that the overall likelihood to capsize within given time remains always as shown in Figure 9.

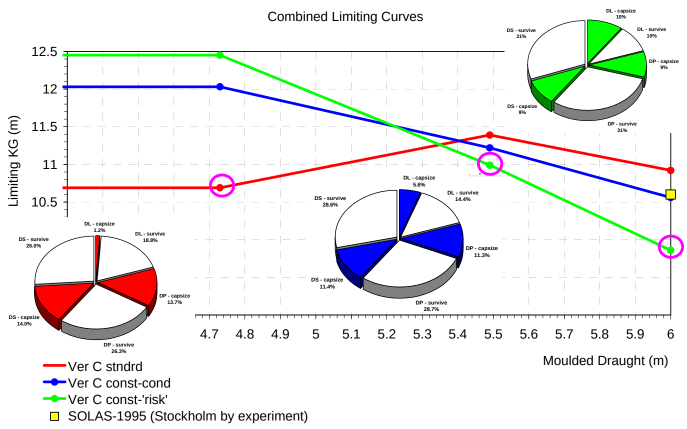

Figure 21 PRR19, "limiting curves" for compliance with SOLAS2009 regulations for Ver C for various interpretations of required index R. Note that the aggregate index A for each of the interpretation remains the same. The limitation on KG for draught of 6m, imposed by compliance with Stockholm Agreement by model testing could be achieved through requirement of $F_{T|W}(t|w) = const$ (curve Ver C const-cond), or even exceeded by requiring that $F_{T\&W}(t \cap w) = const$ (curve Ver C const-risk).

The true added value of considering standard (20) as an alternative to currently adopted standard (17) realises especially when requirements on intact stability are complied with in parallel. Thus, loading conditions shown in circles in Figure 21 would represent the limiting KG for the whole range of drafts. The net result of such a rather mild modification of the SOLAS2009 standard would be some **30%** reduction in the overall likelihood of ship capsizing when subjected to collision damage, as can be seen in Figure 22.

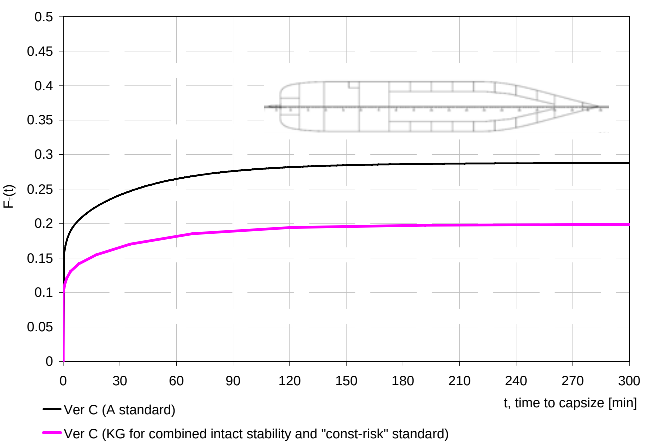

Figure 22 Distribution of **un**conditional[2] probability for the time to capsize t, see equation (3) or (16); impact of combining the stability standard for various drafts.

Historical trends

The last (but by no means least) point considered to be noteworthy in this paper is that of the context in which the results presented in the foregoing should be viewed, namely the risk to life.

Without going into details of this very intricate concept, it is proposed to modify equation (9) into the probability mass distribution (23), for the number of fatalities that are expected to result in an accident such as ship flooding.

$$pr_N\left(N|collision\right) = f_T\left(t_{fail}\left(N\right)\right)\cdot\left|\partial t_{fail}\left(N\right)\right| \qquad (23)$$

With the $t_{fail}\left(N\right)$ representing the inverse of the evacuation completion curve.

A typical result from (23) with (9) is a bimodal function shown in Figure 23. Although idealized, this result points to some important conclusion, namely that a collision accident results in either everybody rescued or everybody lost.

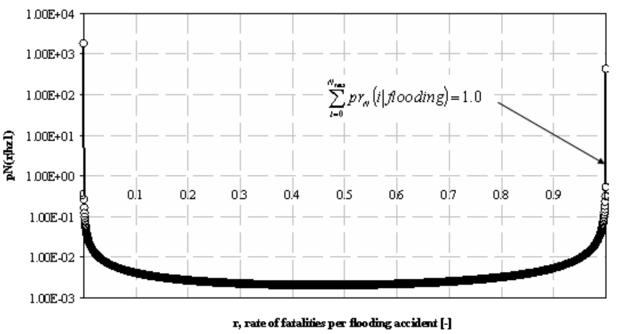

Figure 23 Probability distribution for the rate of fatalities in a collision accident, analytical solution, [28].

This trend finds its reflection in real data from the last 20 years as shown in Figure 24, with the difference between the two figures to be kept in mind relating to the number of casualty scenarios analysed, with some typically 50,000 cases analysed for derivation of Figure 23 and 31 cases building up Figure 24. It can be inferred from Figure 24 that the average mean recurrence interval for rapid capsize/sinking is some **3-4 years**!

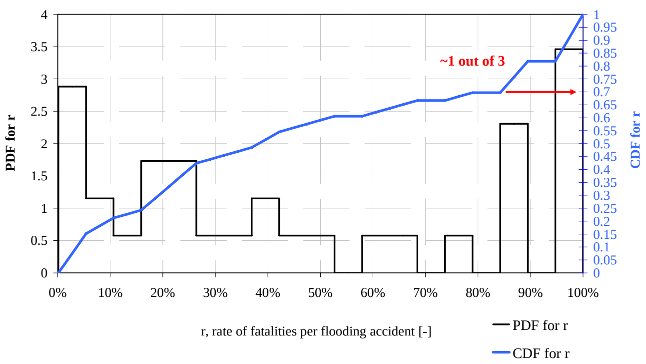

Figure 24 Probability distribution for the rate of fatalities in a flooding accident, LMIU, 1987-2007, 31 accidents. Some 85%-100% fatality rate has taken place in 1 in 3 casualties in the last 20 years!

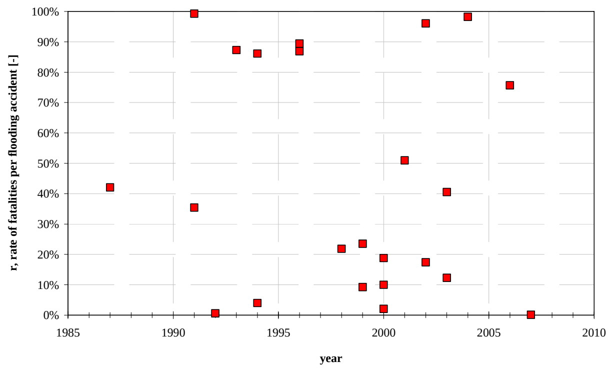

Figure 25 Historical record of the rate of fatalities in a collision accident, LMIU, 1985-2004, 31 accidents. The last two points indicate Al Salam and Sea Diamond.

Figure 23 to Figure 25 are presented here to strongly urge the profession to consider the findings of this paper on the vulnerability ships are exposed to by simple compliance with current standards on stability. The physics underlying the process of stability deterioration after flooding and expressed by model (16) or through numerical simulations, seem to find its reflection in the historical trends. On average, a catastrophic ship loss is expected every 3 to 4 years, this deriving from the mean recurrence interval of 3-4 years for catastrophic accident allowed by today's stability standards for at least some ships (as used in this study) on one hand, and from the fact that flooding accidents actually do take place on average at least once per year, on the other.

Therefore, it is imperative that measures are taken swiftly to break the trend seen in Figure 25 by addressing the standards on provision of stability, and thus reduce the expectation of large fatality rates implied by them, [26].

CONCLUSIONS

This paper presents with an approach to quantify ship vulnerability to flooding. A case study on a RoPax ship fitted with long lower hold showed how the proposed approach could be used for comprehensive and meaningful expression of ship stability.

Based on the case studies, the following set of specific conclusions can be drawn:

- A ship fitted with long lower hold does not appear to show inferior stability to other concepts designed to the same stability standards.
- These stability standards appear to be low, allowing some 20%-30% of ship collision accidents to lead to rapid capsize. Given that accidents involving passenger ships happen at least once per year, current standards imply a **3-4 years** mean recurrence interval for catastrophic loss (a catastrophic accident occurring every 3-4 years). Thus stability standards should be reviewed!
- A series of options for stability enhancements exist, ranging from subdivision modification, to external/internal buoyancy fitting and including change in operational profile or change of the basis in setting the stability standards.

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DISCLAIMER

Opinions presented in this paper are those of the authors, and should not be construed to represent the views of the affiliated institutions.

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