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Development of Strength Evaluation Methodology for Independent IMO TYPE C Tank with LH2 Carriers

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KEYWORDS: Type C tank, Cargo containment system, Liquefied hydrogen carrier, Thermal-structural analysis, Fatigue analysis, Crack propagation analysis

ABSTRACT: Given the inadequate regulatory framework for liquefied hydrogen gas storage tanks on ships and the limitations of the IGC Code, designed for liquefied natural gas, this study introduces a critical assessment procedure to ensure the safety and suitability of such tank designs. This study performed a heat transfer analysis for boil-off gas (BOG) calculations and established separate design load cases to evaluate the yielding and buckling strength. In addition, the study assessed methodologies for both high-cycle and low-cycle fatigue assessments, complemented by comprehensive structural integrity evaluations using finite element analysis. A comprehensive approach was developed to assess the structural integrity of Type C tanks by conducting crack propagation analysis and comparing these results with the IGC Code criteria. The practicality and efficacy of these methods were validated through their application on a 23K-class liquefied hydrogen carrier at the concept design stage. These findings may have important implications for enhancing safety standards and regulatory policies.

Nomenclature

<table>
<thead>
<tr>
<th>Symbol</th>
<th>Definition</th>
</tr>
</thead>
<tbody>
<tr>
<td>L</td>
<td>Rule length of the ship</td>
</tr>
<tr>
<td>V</td>
<td>Design speed</td>
</tr>
<tr>
<td>ρ</td>
<td>Density</td>
</tr>
<tr>
<td>σm</td>
<td>Membrane stress</td>
</tr>
<tr>
<td>σL</td>
<td>Local membrane stress</td>
</tr>
<tr>
<td>σs</td>
<td>The secondary stress</td>
</tr>
<tr>
<td>σg</td>
<td>The secondary stress</td>
</tr>
<tr>
<td>Qcrack</td>
<td>Total heat flux that penetrates from outside to inside of the LH2 tank</td>
</tr>
<tr>
<td>λ</td>
<td>Latent heat for vaporization</td>
</tr>
<tr>
<td>A</td>
<td>Heat transfer area</td>
</tr>
<tr>
<td>nσ</td>
<td>The number of stress cycles at each stress level</td>
</tr>
<tr>
<td>nσLoad</td>
<td>The number of loading and unloading cycles during the life of the tank is not to be less than 1,000</td>
</tr>
<tr>
<td>Cf</td>
<td>The maximum allowable cumulative fatigue damage ratio</td>
</tr>
<tr>
<td>B</td>
<td>Ship breadth</td>
</tr>
<tr>
<td>Cb</td>
<td>Block coefficient</td>
</tr>
<tr>
<td>ρr</td>
<td>Relative density of the cargo at the design temperature</td>
</tr>
<tr>
<td>C</td>
<td>Characteristics tank dimension</td>
</tr>
<tr>
<td>σ0</td>
<td>Bending stress</td>
</tr>
<tr>
<td>ρLH2</td>
<td>Density of liquefied hydrogen</td>
</tr>
<tr>
<td>V_LH2</td>
<td>Volume of LH2 in the cargo tank</td>
</tr>
<tr>
<td>U</td>
<td>Overall heat transfer coefficient</td>
</tr>
<tr>
<td>ΔT</td>
<td>Difference in temperatures between the outer tank and LH2</td>
</tr>
<tr>
<td>Ni</td>
<td>The number of cycles to fracture for the respective stress level</td>
</tr>
<tr>
<td>N_load</td>
<td>The number of cycles to fracture for the fatigue loads due to loading and unloading</td>
</tr>
<tr>
<td>C_f</td>
<td>The cumulative fatigue damage at loading condition, i means the load cases</td>
</tr>
</tbody>
</table>

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1. Introduction

Globally, the transition to various alternative energies to replace fossil fuels is accelerating to address the issue of global warming. Currently, liquefied natural gas is attracting attention as a means to achieve the emission reduction targets for nitrogen oxides and sulfur oxides. Furthermore, for the decarbonization transition, hydrogen (H2) energy, which does not emit carbon dioxide, will be needed as a future natural resource. Nevertheless, storage and transportation methods from overseas hydrogen production sites are required to implement a hydrogen economy domestically. The volume of liquid hydrogen is approximately 1/845 of gaseous hydrogen at room temperature and atmospheric pressure, and it reduces to approximately 1/2 of the volume compared to 700-bar high-pressure hydrogen. This makes it the most suitable form for storing the largest quantity and the most suitable method for substantial storage. Differentials exist strictly for tanks designed to store liquid hydrogen, and they must meet regulations to satisfy international standards. The independent tanks are categorized into Types A, B, and C as defined by the International Maritime Organization (IMO). As shown in Table 1, Type A tanks adhere to general liquid tank regulations. In the event of a presumed potential for liquid cargo leakage, a complete secondary barrier is needed for cases of significant leakage. This type of tank is primarily applied to LPG carriers. For Type B tanks, the structural safety of the tank must be verified through structural analysis. The leakage quantities are calculated using fatigue crack propagation analysis based on fracture mechanics, assuming crack occurrence. Partial secondary barriers are necessary for this type. Type C tanks are used for pressure vessels. They ensure the safety and integrity of the structure and pose no risk of leakage, obviating the need for a secondary barrier.

Type C tanks are typically cylindrical or spherical pressure vessels with a design pressure exceeding two bar. They are designed and manufactured to meet the requirements of recognized pressure vessel standards or codes, such as the American Society of Mechanical Engineers (ASME) Boiler and Pressure Vessel Code (BPVC), and are further supplemented by classification society requirements and legal regulations. Research on liquid hydrogen tanks has focused primarily on transportation, storage containers, fire hazards, sloshing effects, and double-wall vacuum piping (Ahn et al., 2017; Klebanoff et al., 2017). Kim and Islam (2021) and Kim et al. (2018) proposed a structural integrity assessment procedure for Type B liquefied natural gas (LNG) fuel tanks based on the International Code for the Construction and Equipment of Ships Carrying Liquefied Cases in Bulk (IGC Code). They conducted finite element analysis under various design loads to evaluate the structural safety. Choe et al. (2016) experimentally evaluated the cryogenic compressive strength of Divinycell of the NO96-type LNG insulation system. Nho et al. (2017) performed a numerical analysis on the sloshing impact response evaluation method of the LNG carrier insulation system, considering the elastic support effect of the hull structure. Recently, Song (2022) experimentally evaluated the cryogenic material of R-PUF used in the cargo containment system of LNG carriers.

Park (2019) developed fracture strength criteria for membrane-type cargo containment systems under sloshing loads. They evaluated the strength of cargo containment facilities using the finite element method and compared the results with the DNV guidelines. On the other hand, research on the design evaluation of Type C tanks used for liquid hydrogen storage is extremely rare. Lin et al. (2018) proposed an approach to evaluate the boil-off rate (BOR) of Type C tanks at various filling ratios. They estimated the BOR based on finite element analysis and compared the results with experimental data. Lee et al. (2022) numerically analyzed the impact of sloshing on heat transfer and boil-off gas (BOG). Yao et al. (2015) verified the design thickness of a Type-C independent tank using commercial software against conventional design codes. Liu et al. (2018) also performed an
optimization based on the design and application materials for Type C tanks for liquefied hydrogen, but they did not provide a reasonable procedure for the tank design itself.

The paper proposes a procedure to comprehensively evaluate the structural integrity of Type C tanks for storing and transporting liquid hydrogen. Currently, there are limitations in proposing accurate design evaluation procedures because of the absence of domestic and international regulations for the hydrogen environment. This study analyzed the design evaluation methodologies applied to ships carrying cryogenic cargo, and an evaluation procedure for liquefied hydrogen cargo holds with the IMO Type C tank was presented. This includes thermal analysis, structural and fatigue analysis, and, if necessary, crack propagation analysis. This study assessed the structural integrity assessment technique using the developed procedure and validated the suitability of the method by applying it to a finite element model.

2. Development of Structural Assessment Procedure for IMO TYPE C Tank

2.1 Introduction to Type C Tank

Type C tanks are considered leak-free because their design factors enhance the structural integrity, eliminating the need for a secondary barrier. As a result, they are commonly used in LNG fuel tanks for ships, and they have advantages that make them suitable for small-scale LNG, ammonia, or LPG carriers. Two types of tank support structures are used for cylindrical or spherical tanks: fixed and sliding. Fixed supports secure the independent tank, while sliding supports allow for tank expansion and contraction as needed.

The tanks are configured with either a single wall or a double wall, as shown in Fig. 1. In the case of a single wall tank, the exterior is covered with insulating material, and the insulating timber is placed between the saddle support and the tank structure. Double wall tanks feature vacuum insulation, and insulating material/plastic supports are placed between the internal and external tanks. They have the advantage of longer pressure retention time compared to single-wall tanks.

2.2 Procedure for Structural Strength Assessment

Fig. 2 presents the comprehensive strength assessment procedure for the Type C liquid hydrogen (LH2) tank. The developed methodology to check the integrity of the tank can be divided into several stages: heat transfer and BOR calculation, structural analysis, buckling analysis, fatigue, and crack-propagation analysis.

Heat transfer analysis is the critical step in designing a Type C LH2 tank because the temperature of LH2 in the liquid state is $-253^\circ\text{C}$. The analysis serves twofold purposes; the first is for hull steel grade selection, and the other is for thermal load transfer in structural strength analysis. The BOR calculation also involves heat transfer analysis. On the other hand, the environmental temperature conditions are different from those of the steel grade selection conditions. Steel grade selection is governed by the IMO and USCG thermal design

![Fig. 1 IMO Type C tank structure](image)
conditions, while the BOR is calculated under IMO warm thermal design conditions. The strength evaluation of a Type C LH2 tank requires complex thermal-structural analyses to resolve the thermal contacts among the tank shell, wood, and saddle structures. Sequentially coupled thermal-structural analysis was performed by applying thermal and mechanical loads. The design internal pressure is composed of vapor and liquid pressure. The liquid pressure results from the combined effects of gravity and acceleration, excluding sloshing loads. Structural analyses assessed each tank component under normal operating conditions with maximum acceleration, static conditions, 30° heeled conditions, collision conditions, and hydrostatic test conditions.

The buckling strength of the tank structure subjected to external pressures was calculated based on the IGC Code. The value of the design external pressure was determined, and the critical collapse pressure was calculated using finite element (FE) eigenvalue analysis. The design basis for a Type C independent tank is based on the pressure vessel criteria modified to include the fracture mechanics and crack propagation criteria. The fatigue strength was evaluated for the entire structure of the tank. In fatigue analysis, a high-cycle fatigue evaluation was performed because of repeated load histories during the design lifespan and low-cycle fatigue caused by thermal loads from cargo. The primary concern for high-cycle fatigue is the load history that acts repetitively during the design lifespan, and wave loads are a significant factor for both general vessels and independent self-support tanks. The stress distribution for long-term wave loads is directly

**Fig. 2** Flowchart of the comprehensive strength assessment procedure for the Type C LH2 tank
related to fatigue failure. Typically, in the design phase, the stress long-term distribution is estimated through direct load analysis (DLA) and stochastic analysis to account for the actual environmental loads. This study applied the method outlined in international regulations (IGC Code; IMO, 2016) that utilizes the simplified load histories based on Weibull distribution to simplify the process.

Furthermore, hydrogen cargo tanks experience thermal stresses inside and around the tanks due to temperature variations during loading and unloading. Compared to high-cycle fatigue, low-cycle fatigue should also be evaluated because the stress ranges can be very high even with a few cycles. Low-cycle fatigue is typically evaluated for areas subjected to high stresses under periodic static loads. A fracture mechanics-based analysis should be carried out for the critical locations of the tank structure with high dynamic stresses. A fracture mechanics approach assumes that an idealized crack propagates in relation to the stress intensity factor range. A fatigue crack propagation analysis was conducted for the tank skin plate and internal structure to verify the tank integrity. Fatigue crack propagation was assessed from the growth of an initial existing crack to a critical size. High-stress concentration areas or large fatigue damage locations were selected for the crack propagation analysis. Detailed descriptions of each developed procedure are described in the following chapters.

3. Numerical Model

3.1 Target Vessel

The target vessel is an ocean-going LH2 carrier. Table 2 lists the principal particulars of the target vessel. Where \( x \) is the longitudinal distance from amidships to the center of gravity (COG) of the tank; \( y \) is the transverse distance from the centerline to the COG of the tank; \( z \) is the vertical distance from the actual waterline to the COG of the tank, \( K = 1 \) in general. For a particular loading condition and hull forms, \( K \) is determined as \( 13 \frac{GM}{B} \), where \( GM \) is the metacentric height.

3.2 FE Model

The main components of the target tank included the inner tank, outer tank, inside supports (GRE G-10), and outside supports (saddle), as shown in Fig. 3. Among the components, the inner tank was the most critical because it subject to high internal pressures and cryogenic temperatures. An outer tank was designed to envelop the inner tank and safely protect it. The outer tank was glass bubble-filled vacuum insulated. The inner and outer tanks were connected through a glass-reinforced epoxy support. The entire tank was mounted on two saddles, and the outer tank and saddle were connected by welding. The cylinder diameter was approximately 18,000 mm, and the tank length was 31,500 mm. The main particulars of the target ship with a LH2 cargo tank were four (4) LH2 storage tanks with a capacity of 5,750 m\(^3\) × 4 ea.

The global FE model was constructed with four nodes of the shell element for the tank and saddle structures, eight nodes for the solid element for the GRE spacer, and a contact element for the interface among the tank, GRE spacer, and saddle support. The element size of the shell was set to approximately 100 mm × 100 mm, considering the convergence to the nominal stress calculated by the formula for hoop stress of the cylindrical shell. The inner and outer tanks were made of SUS304L steel. The saddle was made of mild steel, and the spacer was made of GRE G-10 material. Table 3 lists the thermal and mechanical properties of the materials.

<table>
<thead>
<tr>
<th>Table 2</th>
<th>Principal particulars of the target vessel</th>
</tr>
</thead>
<tbody>
<tr>
<td>( L ) (m)</td>
<td>( B ) (m)</td>
</tr>
<tr>
<td>184.6</td>
<td>28</td>
</tr>
</tbody>
</table>

Fig. 3 FE model of the target cargo tank
3.3 Boundary Conditions

Among all the components of the target tank, the inner tank is the most critical part because it is subject to high internal pressure and cryogenic low temperature. An outer tank was designed to envelop the inner tank to protect it safely. The outer tank was glass bubble-filled and vacuum-insulated. The inner and outer tanks were connected through glass-reinforced epoxy support. The entire tank was mounted on two saddles, and the outer tank and saddle were connected by welding. The ambient temperature was set to 5°C for heat transfer analysis based on the IMO recommendations. The temperature of liquid hydrogen is \(-253°C\), and the heat transfer coefficient, assuming natural convection, was set to 5 W/(m²·K). Fig. 4(a) shows the detail of the thermal boundary. All degrees of freedom of the nodes at the bottom of the saddle supports attached to the ship hull were fixed, as shown in Fig. 4(b). The contact condition was also considered by applying a coefficient of friction between the spacer and tanks to consider the effect of contact and sliding between them.

3.4 Design Loads

The design loads must be determined when designing a Type C LH2 tank. The self-weight of the tank was applied as an inertial load, considering the acceleration of gravity and the density of the materials involved. The low-temperature vacuum pressure of the vacuum insulation system was applied as an internal load to the inner tank and an external load to the outer jacket. The thermal loads were derived from steady-state heat transfer analysis to calculate the temperature distribution on overall tanks and support structures, assuming the design temperature of LH2 and ambient conditions were \(-253°C\) and 5°C, respectively. The temperature distribution results calculated from heat transfer analysis were mapped to the structural analysis model to evaluate the impact of mechanical and thermal loads on the tank integrity. The IGC Code adopts the fracture mechanics, crack propagation criteria, and the traditional pressure vessel design formulae to construct the Type C tanks. The minimum design vapor pressure is calculated using the following formulae.

\[
P_v = 0.2 + A C(p_g)^{1.5} \text{ (MPa)}
\]

\[
A = 0.00185 \left( \frac{\sigma_m}{\Delta \sigma_A} \right)
\]

where \(\Delta \sigma_A\) is the allowable dynamic membrane stress, which is 55 MPa for ferritic-pearlitic, martensitic, and austenitic steel and 25 MPa for aluminum alloys. The tank test condition is defined as 1.5 times the ambient condition.
vapor pressure and hydrostatic pressure caused by partially filled fresh water. The forward directional acceleration under the accidental collision condition was set to 0.5 times the acceleration of gravity (9.81 m/sec²).

The dynamic loads can be determined by measuring the accelerations using accelerometers on the full-scale ship, by performing a sea-keeping analysis of the ship, or during model testing. In this study, the acceleration components were derived based on the IGC Code recommendation, which corresponds to a probability level of $10^{-8}$ in the North Atlantic environment.

$$a_x = \pm a_x \sqrt{1 + \left( \frac{5.3 - 45}{L} \right)^2 \left( \frac{x}{L} + 0.05 \right)^2 + \left( \frac{0.6 A^4}{B} \right) + \left( \frac{0.69 R^4}{B} \right)^2}$$ (3)

$$a_y = \pm a_y \sqrt{0.6 + 2.5 \left( \frac{x}{L} + 0.05 \right)^2 + K \left( 1 + 0.6 A^2 - 0.25 A \right)}$$ (4)

$$a_z = \pm a_z \sqrt{0.6 A^2 - 0.25 A}$$ (5)

$$a_y = 0.2 \frac{V}{\sqrt{L}} + \frac{34 - \left( \frac{600}{L} \right)}{L}$$ (6)

$$A = \left( 0.7 \frac{L}{1200} + 5 \frac{z}{L} \right) \frac{0.6}{C_B}$$ (7)

where $a_x$, $a_y$, and $a_z$ are the maximum dimensionless accelerations (i.e., relative to the acceleration of gravity) in the respective directions. $a_x$ does not include the component due to the static weight; $a_y$ includes the component due to the static weight in the transverse direction caused by rolling, and $a_z$ includes the component due to the static weight in the longitudinal direction due to pitching.

3.5 Design Load Cases

The design loads must be determined when designing a Type C LH2 tank. The self-weight of the tank was applied as an inertial load, considering the acceleration due to gravity and the density of the materials involved, and the low-temperature vacuum pressure of the vacuum insulation system was applied as an internal load to the inner tank.

All load cases mentioned in the IGC Code are specified when calculating the structural integrity of the LH2 tank. Table 4 lists seven load cases considering the pressure due to the self-weight of the structure, thermal load due to the temperature gradient in the cargo tank, vapor pressure in the inner tank, cold vacuum pressure, heeling condition (30°), liquid hydrostatic pressure, and dynamic pressure due to accelerations.

3.6 Acceptance Criteria

The Type C independent LH2 carrier tank is a pressurized tank. The structural stress results were assessed to determine the yield strength according to the IGC Code and other internationally recognized pressure vessel codes. For the design of Type C LH2 tanks, the calculated stress shall not exceed the corresponding allowable stress, as listed in Table 5.
4. Thermal-Structural Analysis

4.1 Heat Transfer Analysis

Thermal-structural analysis was performed in two steps; the first step performed heat transfer analysis to calculate the member temperatures, and the last step accomplished structural analysis by applying the thermal loads from the previous step and all other mechanical loads. Fig. 5 shows the heat transfer analysis results for the LH2 tank. The maximum temperature of 3.3°C was estimated on saddle support, and the minimum temperature of −253°C was attained on the inner tank, which is in direct contact with the LH2. The steel foundation attached to the inner tank reached the same temperature as LH2 because of its high thermal conductivity. The temperature variation of the structure around the inner tank was minimal because the annular space between the inner and outer tank was vacuum-insulated with a glass bubble. On the other hand, the largest temperature gradient was obtained on the spacers.

<table>
<thead>
<tr>
<th>Material</th>
<th>$R_e$</th>
<th>$R_m$</th>
<th>$f$</th>
<th>1.5$f$</th>
<th>3.0$f$</th>
<th>0.9$R_e$</th>
</tr>
</thead>
<tbody>
<tr>
<td>SUS304L</td>
<td>175.0</td>
<td>480.0</td>
<td>116.7</td>
<td>175.0</td>
<td>350.0</td>
<td>157.5</td>
</tr>
<tr>
<td>Mild Steel</td>
<td>235.0</td>
<td>400.0</td>
<td>114.3</td>
<td>171.0</td>
<td>343.0</td>
<td>211.5</td>
</tr>
</tbody>
</table>

Table 7 Allowable stresses of the material

Fig. 5 Overview of the heat transfer analysis results

Fig. 6 Detail temperature results for different components of the tanks
because the two tanks are connected via GRE G-10 spacers with very low thermal conductivity, as shown in Fig. 5. Fig. 6 presents the detailed temperature distribution for each tank component.

4.2 Structural Analysis

Structural analysis was performed using LS-DYNA to check the movement of the tanks and associated stresses under the defined load cases. Seven load cases were analyzed, and the von Mises equivalent stresses were derived for each. The yielding check was done for each component, including the inner tank, outer tank, and two saddle supports. Figs. 7 and 8 show the stress contours under accident collision conditions, which were intended to be the highest stress among the load conditions. The internal pressure, consisting of the vapor pressure, cargo pressure, and cold vacuum pressure, has the greatest impact on all the loads acting on the inner tank. For example, the general primary membrane stress on the inner tank is 112 MPa, which is lower than the allowable stress of 116.7 MPa. The high-temperature gradient caused the inner tank to shrink, and subsequent stresses occurred at the junction between the inner tank and the steel foundation. Maximum stress of 272 MPa occurred in the inner tank where the steel foundation was attached. This stress was defined as localized stress because it was calculated at a critical location, such as a support, and was caused by the combined effects of thermal and other mechanical loads.

Table 8 lists the representative structural analysis results for the three most utilized load cases. The sum of the most critical terms (local, bending, and secondary stresses) and the corresponding allowable stresses are shown. All components of the stresses satisfied the criteria, and even for LC7, the ideal hydrostatic test conditions, the yield stress did not exceed 90% of the allowable value specified in the IGC Code. The current design of the cargo tank and its supports meet the yield strength criteria of the IGC Code.

![Fig. 7](image1.png) Maximum equivalent stress (left) and primary membrane stress (right) contours under collision conditions

![Fig. 8](image2.png) Stress contours for various components of the tanks under collision conditions
5. Buckling Analysis

The buckling requirements generally apply for cylindrical shells and torispherical or ellipsoidal ends exposed to external pressure and other loads causing compressive stresses. Different buckling modes can be obtained from eigenvalue FE analysis. In addition, the critical buckling pressure of the tank can be determined using two FE simulation methods, such as linear buckling (eigenvalue) analysis and post-buckling analysis with imposed imperfection. Linear buckling (eigenvalue) analysis requires the elastic material properties of the tank and a unit buckling load distribution applied on the tank to solve the eigenvalues for the corresponding buckling modes. Generally, the first buckling mode with the lowest eigenvalue represents the critical buckling load. On the other hand, post-buckling analysis should be carried out to determine the critical buckling load when the imperfection of a tank is significant. The elastic-plastic material properties are needed, and a load distribution is applied to the tank that contains the imperfection to be considered. The imperfection field can be obtained through actual measurements, manufacturing tolerance, or buckling mode shapes derived from eigenvalue analysis.

This study examined the buckling strength of the outer tank subjected to a vacuum pressure of 0.1 MPa through linear buckling analysis. Fig. 9 shows the first buckling mode from eigenvalue analysis. The FE results predicted a critical buckling pressure of 0.29 MPa and a safety factor of 2.9, which meets the IGC Code requirements.

6. BOR Calculation

The BOR is commonly used in the field of liquefied gas transportation and refers to the rate at which liquefied gases, such as LNG or liquefied hydrogen (LH2), vaporize and are lost from storage tanks or cargo holds. The BOR represents the quantity of gas vaporized over time, typically expressed in %/day. It plays a crucial role in the design and operation of ships or storage facilities, significantly impacting efficient gas management and economic viability. Liquefied gases evaporate at temperatures above their boiling point and generate BOG. Although there are numerous causes for BOG generation, heat ingress is the primary reason for generating BOG on ships. This study calculated BOR assuming the heat ingress into cargo tanks is the only source for generating BOG.

The BOR signifies the percentage of evaporated LH2 per day to the initial LH2 loaded amount and can be estimated using the following equation. Table 9 lists the design data for calculating the BOR of the target LH2 tank.

\[
BOR = \frac{\sum Q_{vap}}{V_{LH2} \times 3600 \times 24 \times 100\%} \\
(8)
\]

\[
Q_{vap} = U \cdot A \cdot \Delta T 
(9)
\]

Local FE steady-state heat transfer analysis was performed to estimate the heat leakage into the cargo tank. The IMO warm
environmental condition was applied as the thermal boundary condition where the air and seawater temperatures were assumed to be 45°C and 32°C, respectively. The convection heat transfer coefficient of 5 W/m²K was applied to transfer the heat from the environment to the outer tank. The vacuum insulation system with a conductivity of 0.0018 W/(m·K) and GRE spacer of 0.29 W/(m·K) was modeled with solid elements. Fig. 10 presents the results of heat transfer analysis, and Table 10 lists the results of the BOR calculation.

The specific standards for the BOR vary according to the type of cargo being transported, the mode of transportation, the design of the storage facility, and the type of vessel. For example, in the case of ships transporting LNG, the BOR is typically calculated daily and can vary in%/day depending on the design and operation of the vessel. For other types of cargo, such as LH2, the standards for BOR are not yet widely standardized, and regulations or standards continue to evolve. The BOR is particularly important for LH2 because the vaporization of LH2 greatly affects transportation efficiency. Therefore, minimizing the BOR is crucial when designing ships or storage facilities transporting LH2.

The total heat ingress can be calculated by expanding the heat ingress calculated from the spacer and inner tank to the total area. The BOR of this tank can be calculated using Eq. (8). The calculation procedure of the BOR presented in this study is an example case, and the design BOR of an actual Type C tank may be greater than the calculated value. This study only considered the heat leakage through insulation and GRE spacers, but under real operation conditions, there may be additional heat ingress through the liquid dome, pipe penetrations, and manholes. Therefore, more precise calculations should be performed in the actual design, considering all types of heat ingress into the LH2 tank.

### 7. Fatigue Analysis

According to the IGC Code, for a large Type C independent tank,
where the cargo at atmospheric pressure is below $-55^\circ C$, the administration or recognized organization acting on its behalf may require additional verification to check their compliance with static and dynamic stress. The fatigue assessment was performed on welded joints, and the allowable fatigue damage in high-stress areas of the tank should be less than 0.5 for regions detectable by in-service inspection and less than 0.1 for undetectable regions. This study calculated the high cycle fatigue damage calculation caused by ship motions and low cycle fatigue damage calculation caused by loading/unloading. In Eq. (10), the first term $\frac{n_i}{N_i}$ represents the high cycle fatigue, and the second term $\frac{n_{loading}}{N_{loading}}$ represents the low cycle fatigue.

$$\sum \frac{n_i}{N_i} + \frac{n_{loading}}{N_{loading}} \leq C_u$$ (10)

The loading and unloading cycles include a complete pressure and thermal cycle, typically corresponding to 20 years of operation. In this section, the fatigue damage was calculated for areas where structural analysis predicted the maximum stresses. The high cycle fatigue damage of structurally weak points was calculated using the simplified method. Fatigue analysis considers the appropriate combination of loads for the expected life of the LH2 tank because ship motions cause high cycle fatigue. The inertial force caused by ship motion was applied as a high-cycle fatigue load in this study, as shown in Table 8. Therefore, tank accelerations (longitudinal, transverse, and vertical direction) based on the IGC Code were applied to the structural model, and a simplified method was applied to calculate fatigue damage using the maximum principal stress. This method estimates the long-term...
stress distribution by applying the Weibull distribution function, and the calculation method is as follows:

\[ D_f = U_{Kf} \frac{\gamma^m}{K} \left( 1 + \frac{m_f}{q} S_f \right) + \frac{\gamma^n}{K} \left( 1 + \frac{n_f}{h} S_f \right) \]  \hspace{1cm} (11)

The critical location for the fatigue assessment was chosen based on areas of high stress in the structural analysis results. The maximum stress occurred near the tank support. Fig. 11 shows the results of the maximum vertical acceleration case.

The thermal loads based on the temperature distribution were considered for low-cycle fatigue analysis. The thermal stress range was obtained by the difference in stress between loading and unloading (Fig. 12). Fig. 13 presents the results when the temperature in the tank is \(-253^\circ\text{C}\).

Table 11 lists the values of the design S-N curve, and Table 12 summarizes the fatigue damage results. Even in the structural discontinuity region, the fatigue damage was calculated to be very small, and fatigue damage results satisfy the allowable criteria regardless of combining the fatigue damage by applying a conservative method.

### 8. Crack Propagation Analysis

The structural integrity can be evaluated by performing crack propagation analysis in areas where crack detection is difficult or in areas of structural weakness. This is known as ‘Engineering critical assessment (ECA) or Fracture mechanics analysis’ and is an analysis technique applied to evaluate the structural integrity and sustainability of a service. This design concept has been applied mainly to aircraft and nuclear reactors, and the concepts of damage tolerance design and leakage before failure theory are reflected in the design of cryogenic cargo tanks. ECA assumes that there is a crack inside the weld area and considers the length and depth of the crack, internal and external loads, strength and toughness of the material, and residual stress caused by the welds. In addition, the fractured joint was evaluated using a failure assessment diagram (FAD), which considers the loading characteristics and toughness characteristics, with the crack tip opening displacement (CTOD) used widely for toughness characteristics. Fig. 14 shows the flowchart for engineering a critical assessment.

The welded part of the independent tank was exposed to the internal cargo motion and the residual stress due to welding. The crack opening stress due to internal and external pressures was calculated using finite element analysis. A simplified stress history was used because the actual load and stress history cannot usually be considered accurately during the design stage, as shown in Fig. 15.

This can be determined as a long-term stress corresponding to the design life of the ship (i.e., probability level of \((10^8)\)). The simplified long-term stress distribution in the calculations was determined using the modified Weibull distribution in equation (12).

\[ \log_{10} N_i = 8 \times \left[ \frac{\Delta \sigma_{\text{eq}}}{\Delta \sigma_{\text{eq}}^0} \right] \]  \hspace{1cm} (12)

The total stress spectrum was divided into 20 groups to eliminate the effect of the stress sequence on the crack propagation life. The fatigue crack propagation path was assumed to be perpendicular to the principal stress direction. The stress intensity factor range was calculated from the stress range, crack shape and size, and geometry. International Institute of Welding (IIW) or equivalent standard was used to assess the stress intensity factor for a surface crack. The stress range was based on the maximum principal stress from the structural analysis results in Fig. 7:

\[ \frac{dK}{dN} \bigg|_{\Delta \sigma_{\text{eq}}^0} = 3.78 \times 10^{-9} (\Delta K_{ij})^{3.07} \]  \hspace{1cm} (13)

\[ \Delta K_{ij} = \begin{cases} \frac{5.5}{2.88 - R} & \text{for } R < 0 \\ 6.52 R & \text{for } 0 \leq R < 1.0 \end{cases} \]  \hspace{1cm} (14)

\[ \Delta K_{ij}^{\text{eff}} = \frac{K_{\text{eff}}^{\max}}{1 - 0.3472 R} (K_{\text{eff}}^{\max} - K_{\text{eff}}^{\min}) \]  \hspace{1cm} (15)

\[ K_{\text{eff}}^{\max} = \max(K_f, K_f^{\text{eff}}, K_f^{\text{eff}}) \]  \hspace{1cm} (16)

The initial crack length and depth must be assumed when performing the surface crack propagation analysis. In this study, a

### Table 11: Details of the basic design S-N curve

<table>
<thead>
<tr>
<th>Class</th>
<th>Environment</th>
<th>A (MPa)</th>
<th>m</th>
<th>C (MPa)</th>
<th>r</th>
<th>S_{ij} (MPa)</th>
<th>No. cycles</th>
<th>\text{f}_{\text{res}}</th>
</tr>
</thead>
<tbody>
<tr>
<td>Weld joints</td>
<td>Air/LH2</td>
<td>3.02 × 10^{12}</td>
<td>3.0</td>
<td>1.35 × 10^{16}</td>
<td>5.0</td>
<td>67.1</td>
<td>10^7</td>
<td>16</td>
</tr>
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</table>

### Table 12: Summary of fatigue damage

<table>
<thead>
<tr>
<th>Fatigue load case</th>
<th>Stress range (MPa)</th>
<th>N_{\text{loading}}</th>
<th>n_{\text{loading}}</th>
<th>Damage</th>
</tr>
</thead>
<tbody>
<tr>
<td>High cycle</td>
<td>Max. Acc. Longi.</td>
<td>7.89</td>
<td>-</td>
<td>1.41 × 10^{7}</td>
</tr>
<tr>
<td></td>
<td>Max. Acc. Trass.</td>
<td>21.25</td>
<td>-</td>
<td>4.51 × 10^{6}</td>
</tr>
<tr>
<td></td>
<td>Max. Acc. Vertical</td>
<td>25.15</td>
<td>-</td>
<td>8.15 × 10^{6}</td>
</tr>
<tr>
<td>Low cycle</td>
<td>-</td>
<td>74.76</td>
<td>9.99 × 10^{8}</td>
<td>1.00 × 10^{4}</td>
</tr>
</tbody>
</table>
crack length of 5.0 mm and a crack depth of 1.0 mm were applied for the fillet welds according to the guidelines of the Korean Register. Fig. 16 shows the crack lengths generated by applying the ASMR BPVC code over the design life of the ship. The results were calculated as non-penetrating in the thickness direction for 15 days and evaluated to satisfy the allowable criteria of the IGC Code.

9. Conclusion

This study developed a comprehensive structural integrity assessment procedure for Type C tanks that can store liquid hydrogen. Design load cases based on the IGC Code, which is essential for cryogenic cargo carriers, were defined. Thermal-structural analysis was conducted to calculate thermal loads, evaluate yield and buckling strengths, and propose a methodology for calculating the BOR. Furthermore, high-cycle and low-cycle fatigue analysis based on the simplified method was proposed. In addition, a procedure for crack propagation analysis based on fracture mechanics, which can be applied as needed, was presented. According to the developed procedure, the evaluation was performed on the conceptually designed 23K hydrogen carrier, and the suitability of the procedure was verified. Based on the study results, the following conclusions are derived.

(1) Heat transfer analysis was performed to calculate and transfer the thermal loads caused by the LH2 operating temperature. A similar
approach can be applied for steel grade selection by changing only the thermal design conditions (i.e., IMO and USCG conditions). The temperature change in the structure around the inner tank was minimal because the annular space between the inner and outer tank was vacuum-insulated, and the largest temperature gradient was obtained at the spacer. The calculated heat loads were further exported to account for the thermal effects of LH2 in the structural analysis phase.

(2) Structural strength evaluation based on the IGC requirements was performed by analyzing seven load cases. Each satisfied the minimum criteria, and the sum of the most critical terms (local stress, bending stress, and secondary stress) was compared with the corresponding allowable stresses. The maximum stress was identified at a critical location, was a local stress, and was caused by the combined effects of thermal and other mechanical loads.

(3) After predicting the buckling strength through a linear eigenvalue analysis, a safety factor of 2.93 was obtained, indicating that the outer tank has sufficient buckling capacity against the cold vacuum pressure. Assuming that heat leakage is the only cause of BOG generation, the procedure applied to the BOR calculation was presented with an application case. Further refinement in calculation could be applied in the actual design phase, considering all types of heat ingress into the LH2 tank, including liquid dome, pipe penetrations, and manholes.

(4) An analysis procedure for high-cycle fatigue caused by inertia load from tank acceleration and low-cycle fatigue caused by temperature differences from loading/unloading was developed, and the analysis results were reviewed. In addition, crack propagation analysis based on fracture mechanics was accomplished to perform an integrated structural design evaluation of the LH2 tank.

This study analyzes the methodology applied to tank design evaluation based on the IGC Code and presents an integrated procedure applicable to LH2 tanks. In addition, the evaluation methodology was applied to the IMO Type C tank, and the results were analyzed to examine applicability. This study provides initial research data because there is a lack of special regulations or approval procedures to apply to hydrogen tanks mounted on ships. Therefore, a special evaluation procedure that reflects the characteristics of liquefied hydrogen will be necessary.

Conflict of Interest

No potential conflict of interest relevant to this article was reported.

Acknowledgments

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<table>
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1. Introduction

The rising intensity and frequency of typhoons caused by climate change, such as global warming, have led to a rise in the frequency and scale of coastal disasters on the Korean Peninsula. These disasters, which affect major national infrastructure and coastal areas of large cities, are primarily caused by storm surges, inundation, coastal erosion, and swells (KIOST, 2022). Long-term observations and analysis of waves, which are the primary external force behind coastal disasters, are essential for researching the prediction of these disasters and mitigating their resulting damage (Shim and Min, 2007). Therefore, the long-term weather and ocean observation data from the Ieodo Ocean Research Station can be used for typhoon prediction. The station's location in the open sea, where it remains unaffected by the land and lies on the paths of most typhoons passing through the Korean Peninsula, makes its data particularly valuable (Mun et al., 2007). Therefore, the wave observation data from the Ieodo Ocean Research Station are highly significant for calculating the design conditions of coastal and offshore structures and for predicting wave behavior during extreme weather conditions (Lee et al., 2007). Established in 2003, the Ieodo Ocean Research Station (IORS) is Korea's first ocean research facility. The station is located 149 km southwest of Jeju Marado Island, the southernmost point of Korea, and is dedicated to observing oceanic, weather, and environmental factors (Shim and Chun, 2004).

In Korea, wave data are currently gathered through direct observations using buoys or pressure-type wave gauges, as well as through remote observations using radar (Jeong et al., 2018). Direct observation involves low installation costs and provides high accuracy, but it has the risk of damage or loss during severe weather conditions, such as typhoons, and poses maintenance challenges, making it unsuitable for installation in open-sea areas. In contrast, remote observations allow for easy maintenance and stable long-term monitoring even in severe weather conditions, but they provide less
accurate results than direct observations. Accordingly, the IORS, a jacket-type structure installed in the open sea 149 km from land, observes waves using the Wave and Current Radar (SM-050, hereafter “MWR”) from MIROS, considering the observation conditions rather than direct observation methods.

The MWR, as previously noted, necessitates minimal maintenance and enables wave observation in severe weather, but also presents a limitation where it may yield exaggerated data in low-wave conditions because of the reduced wind due to its utilization of the backscattering microwaves during wave observation (Min et al., 2018). Studies have been conducted on the MWR at the Socheongcho Ocean Research Station in 2018 and at Dokdo Island in 2020 to overcome such drawbacks (Min et al., 2018; Jun et al., 2020). Min et al. (2018) reanalyzed the wave data observed at the Socheongcho Ocean Research Station for approximately 2.5 months from January 2015 using MWR System Software (SW-002) v4.00. On the other hand, the Socheongcho Ocean Research Station's location, located further from typhoon trajectories, lacks the observation of high waves with a significant height of 3 m or more. Consequently, studies on the reliability of wave observations during extreme weather events could not be conducted. Jun et al. (2020) reassessed wave data spanning two years from October 2017, using an advanced version filter, SW-002 v4.10, on the raw observations from the MWR at Dokdo Island. The reliability of the data was enhanced by integrating the spike test algorithm (OOI, 2012) from the Ocean Observatories Initiative (OOI) and the H-Ts quality control method, a novel approach for wave data quality control. After applying three quality control methods, the wave observation data from the MWR exhibit a certain level of reliability for significant wave heights. Nevertheless, there were still errors, particularly those occurring for high wave events exceeding 3 m.

Highly reliable wave observation data from the MWR at the IORS, which holds significant value for typhoon research, were regenerated by collecting the raw observation data of the MWR from the station spanning May 19, 2013, to September 8, 2021. In addition, the reliability of the raw MWR data was analyzed by comparing it with data from the Jeju Southern Ocean buoy, operated by the Korea Hydrographic and Oceanographic Agency and located near the IORS. The errors remaining in the raw data were addressed by reanalyzing the wave data, primarily using the MWR System Software (SW-002) filter, which has been used in previous studies. As reported elsewhere, applying the SW-002 filter alone, commonly used for processing MWR wave observation data, does not ensure reliability for high wave events and significant wave periods. Therefore, this study proposes using an artificial neural network (ANN) technique, which is particularly effective for identifying and reproducing the nonlinear and potential correlations between the input and output values of MWR wave observation data applied with the filter. The correlation between 40 parameters of MWR and wind speed data of the IORS anemometer for buoy data was analyzed to select the proper input values for effectively training ANNs. Stratified sampling was then applied to the segmented training and test data to enhance the applicability and generalization performance of ANNs. The optimization process for hyperparameters, including the learning rate, batch size, and ANN architecture, along with the analysis of the results, were explained.

2. Wave Radar Observation and Verification

2.1 Wave Radar Observation

The MWR at the IORS is installed on the southeast side of the roof deck, positioned approximately 34.8 m above the sea level, and has
been conducting remote wave observations since 2003 (Fig. 1). The MWR is remote observation equipment (MIROS, 2011) for observing the gravity surface wave spectrum; Table 1 lists its specifications. The MWR consists of six antennas that sequentially take observations every 30°, covering a half-circle with a range of 210 m from the antenna. On the other hand, the observation is omnidirectional (360°) because it captures the incoming and outgoing waves. Each antenna sequentially irradiates microwaves corresponding to the C-Band (5.8 GHz) toward the sea surface at a 10° angle from the horizontal. The irradiated microwaves are then backscattered from the sea surface. The radar echo travels at the speed of water particles and is modulated in amplitude and phase according to the speed of the ocean current (Doppler effect). A strong radar echo is formed when the radar wavelength becomes twice the period of the reflected signal; since the radar wavelength is 5.17 cm (5.8 GHz), the actual scattered signals of the radar are from capillary waves caused by wind, having a wavelength close to 2.6 cm. Therefore, observations should be taken in an environment where the wind speed is at least 3 m/s because capillary waves are not generated without wind and are difficult to observe. The backscattered radar echoes were then collected for 128 seconds (2 Hz, total of 256 samples) and saved as a Doppler time series. The process takes between 12 and 15 minutes to complete one full observation of 180°. Subsequently, once the observation was complete, one wave spectrum was generated using the pulse Doppler method based on the data collected from the six antennas. After generating one wave spectrum, the observation data from the five previous antennas is combined once the observation from each antenna is completed, generating a wave spectrum every 2.5 minutes. The generated spectrum was saved as raw data in a DF025 file and sent to SW-002, the processing software of MIROS, to be calculated as a 2D spectrum (DF038) and wave parameter (DF037) in real time. During the calculation process, the quality of the observation data can be enhanced using the filter embedded in the software. The highly dependable wave data from past observations were reconstructed by manually conducting parameterization and applying filters using the SW-002 software to the raw MWR data.

### 2.2 Wave Radar Accuracy Verification

This study verified the accuracy of the MWR wave observation data collected at the IORS over nine years (2013-2021) by comparison with the data from the Jeju Southern Ocean Buoy provided by the Korea Hydrographic and Oceanographic Agency (KOOFS, 2023). The Jeju

![Fig. 2](image-url) Comparison of the ERA5 significant wave height data at Ieodo and Jeju Southern Ocean Buoy location. ERA5 significant wave height data: (a) Time series of significant wave height from Ieodo (red) and Buoy (blue) location; (b) Scatter plots of the significant wave height from Ieodo and Buoy location with statistics.
Southern Ocean Buoy is a large-scale marine observation buoy mooring approximately 168 km from the IORS (Fig. 1), and its specifications are provided in Table 1. The wave data collected by a wave sensor (MOSE-G100) installed on the buoy were processed every 30 minutes by a program, saved in a data logger, and transmitted to a management system in real time (Datawell, 2009). For a more accurate comparison, data from a buoy located closer to the MWR would be ideal. On the other hand, a comparative analysis was conducted using these MWR data because the Jeju Southern Ocean Buoy 1 data are the only long-term buoy observation data available for comparison with the IORS and are the nearest.

The ERA5 reanalysis data provided by the European Centre for Medium-Range Weather Forecasts (ECMWF) were used to verify the suitability of using the Jeju Southern Ocean Buoy data for comparison. These data were used to derive the comparison results for significant wave heights and statistical measures between the Jeju Southern Ocean Buoy 1 data and the IORS data, as shown in Fig. 2. The statistical measures included the Pearson correlation coefficient \( R \), root mean squared error (RMSE), and bias. Each statistical measure was derived using the following equations:

\[
R = \frac{\sum (x_i - \bar{x})(y_i - \bar{y})}{\sqrt{\sum (x_i - \bar{x})^2 \sum (y_i - \bar{y})^2}}
\]

\[
RMSE = \sqrt{\frac{\sum (x_i - y_i)^2}{n}}
\]

\[
Bias = \frac{\sum (x_i - y_i)}{n}
\]

where \( x \) is the actual value (buoy), \( y \) is the comparison value (MWR), \( \bar{x} \) and \( \bar{y} \) refer to the mean of each value, and \( n \) refers to the total amount of data.

The comparison showed that the RMSE and bias for the significant wave height of the ERA5 reanalysis data for the two stations were 0.56 m and 0.26 m, respectively, indicating the presence of some errors. These errors can be attributed to regional differences, such as the 168 km distance between the two stations and the 90 m difference in water depth (127 m for the Jeju Southern Ocean Buoy and 40 m for the IORS), highlighting the limitations of this study. The correlation coefficient of the significant wave height was 0.87, indicating a strong correlation between the two stations. The wave characteristics between the two stations are not considerably different, as shown in Fig. 2. Considering that the Jeju Southern Ocean Buoy is the only long-term observation station available for comparing the nine years of MWR raw data collected near the IORS, this study used the Jeju Southern Ocean Buoy data to enhance the reliability of the MWR regeneration data from the IORS.

The Jeju Southern Ocean Buoy wave data have been available since 2014, but the MWR raw data cannot be obtained from January to March 2014. Therefore, the comparison was conducted using wave data from March 13, 2014, to September 8, 2021. Both sets required identical time intervals to compare the observation data with the buoy data. The MWR wave data, initially recorded at two to three-minute intervals, had to be converted to match the 30-minute intervals of the buoy data. On the other hand, converting nine years' worth of data during preprocessing is extremely time-consuming. Therefore, the comparison was conducted using one-hour intervals for both data sets. Because the MWR data are not precisely divided into one-hour intervals, they were averaged into one-hour intervals to compare the significant wave height and period with the Jeju Southern Ocean Buoy data, which are recorded at one-hour intervals (Fig. 3).

As previously mentioned, the accuracy of MWR observation data diminishes in low-wave environments with little wind because of backscattering microwaves. The accuracy of MWR wave data was assessed using wind speed data from an IORS anemometer provided by the Korea Institute of Ocean Science and Technology ocean research station (Fig. 3(c)). The comparison showed that observation data when the wind speed was 3 m/s or below, tended to be overestimated compared to the buoy data, a trend also noted in

![Fig. 3 Comparison of the MWR and Jeju Southern Ocean Buoy data: (a) Time series of significant wave height from MWR (red) buoy (blue); (b) Time series of significant wave period from MWR (red) buoy (blue); (c) Time series of wind speed from IORS anemometer.](image-url)
previous studies. In addition, more spikes were evident in the significant wave period data than in the significant wave height data. On the other hand, the number of spikes is relatively smaller than the MWR data from the Socheongcho or Dokdo Ocean Research Stations in previous studies. This suggests that the data quality is better, likely due to the more suitable height at which the wave and current radar are installed above the sea level. MIROS, the manufacturer of the MWR, recommends installing the wave and current radar 25 to 80 m above mean sea level, at a 10° angle from the sea surface, with an observation radius of 170 to 450 m from the radar (MIROS, 2011). The suggested installation ranges are based on the radar scanning range of the sea surface, with a maximum footprint length of 75 m. Therefore, if the MWR is installed at 80 m or higher and the observations are made at the recommended 10° angle, the distance to the footprint becomes excessively long. This results in a lower reception rate of radar signals reflected off the sea, particularly when the wind speed is 3 m/s or below. The correlation between installation height and observation accuracy was examined by comparing the significant wave height and installation height of the MWRs analyzed in previous studies with those of MWRs installed at the IORS (Table 2). As the installation height increased, the accuracy of the significant wave height decreased due to the elongation of the observation range (distance to the footprint). Therefore, The MWR at the IORS was installed at a relatively lower height compared to the Socheongcho or Dokdo, allowing observations to be conducted within an appropriate range, resulting in fewer spikes in the data because of a higher reception rate of reflected radar signals.

### 2.3 Application of Optimal Filter

The raw data obtained from the MWR (DF025) exhibit numerous spikes and errors because of the scarcity of backscattered microwaves from radar signals in low-wind environments. In a previous study (Jun et al., 2020), the reliability of the generated wave data was enhanced using a filter from MIROS software, SW-002 (v4.10), to eliminate spikes. This study implemented the identical SW-002 filter used in a previous study on MWR observation data, the procedure of which is outlined as follows.

#### 2.3.1 Energy level check

The spikes observed in the wave height time series in low-wind conditions were eliminated by comparing the wave spectrum with the power of a moving averaged wave spectrum. The most notable achievement was observed in enhancing the accuracy of significant wave height and the quality of wave period.

#### 2.3.2 Long period noise removal

The spikes of low frequency that emerge in the wave spectrum during low wind and wave conditions were identified and removed before the wave spectrum calculation. The improvement in the significant wave height and period was minimal compared to that achieved by other filters.

#### 2.3.3 Reduce noise frequency

Empirical algorithms were used to remove the low-frequency wave energy that leads to overestimating the wave height from the wave spectrum calculated under low wave energy conditions. Although the overestimated wave heights and periods are improved, there is an overall tendency to underestimate the observed wave heights.

#### 2.3.4 Phillips check

The observed wave spectrum was compared with a theoretical wave spectrum, and if the measured spectrum density surpasses the threshold, it is flagged, and the results are recorded as status. Applying the filter had a limited impact on the wave data generated from the IORS MWR.

Individually, the aforementioned filters have a restricted impact on enhancing quality. In SW-002, the users can apply each filter multiple filters in combination, considering the specific characteristics of the respective sea (Jun et al., 2020). Therefore, this study aimed to enhance the quality of the MWR observation data by identifying an optimal filter for the Ieodo sea area. This was achieved by combining

### Table 3 SW-002 Filter Combinations to Improve Wave Data Quality

<table>
<thead>
<tr>
<th>Combinations of filters</th>
<th>Reduce noise frequency</th>
<th>Phillips check</th>
<th>Long period noise removal</th>
<th>Energy level check</th>
</tr>
</thead>
<tbody>
<tr>
<td>No filter</td>
<td>Off</td>
<td>Off</td>
<td>Off</td>
<td>Off</td>
</tr>
<tr>
<td>Case 1</td>
<td>On</td>
<td>On</td>
<td>On</td>
<td>On</td>
</tr>
<tr>
<td>Case 2</td>
<td>On</td>
<td>On</td>
<td>Off</td>
<td>On</td>
</tr>
<tr>
<td>Case 3</td>
<td>Off</td>
<td>On</td>
<td>On</td>
<td>On</td>
</tr>
</tbody>
</table>
different filters applied to the same period of MWR raw data used in the comparative analysis against the buoy data, as shown in Table 3. Case 1 assessed the quality improvement effect by applying all four filters, while Case 2 evaluated the effect of the remaining filters, excluding the “long-period noise removal” filter, which has minimal impact on controlling the period.

Case 3 examined the effect of excluding the “reduce noise frequency” filter, which tends to underestimate the wave heights. Table 4 presents the analysis of statistical measures (correlation coefficient, RMSE, and bias) after applying relevant filters in each case. In case 1, where all four filters were applied, both significant wave height and significant wave periods improved. On the other hand, the improvement in significant wave height was minimal compared to the significant wave period, and the decreasing bias indicated a tendency to underestimate the wave height. Comparing case 1, where all filters were applied, with case 2, which excluded the “long-period noise removal” filter, showed that using multiple filters together still allows each filter to exert its effect.

In addition, the “long-period noise removal” filter effectively removes low-frequency spikes in low-wave environments. Compared to case 1, case 3 had slightly lower quality in the wave period. On the other hand, excluding the “reduce noise frequency” filter, which tends to underestimate the wave height, improves the wave height quality.

Hence, this study chose case 3 as the optimal filter because it exhibited the lowest RMSE among the filter combinations, with an RMSE for a significant wave height of 0.48. This selection aimed to rectify the errors in observation data occurring in 3 m or higher wave environments. The significant wave height and period were compared before and after implementing the optimal filter on the buoy data, and the outcomes were depicted in both a time series plot and a scatter plot (Fig. 4). Overall, the wave height and period showed improvement after applying the optimal filter. On the other hand, the enhanced wave

<table>
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<th>Wave height</th>
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<td>-0.21</td>
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<tr>
<td>Case 2</td>
<td>0.88</td>
<td>0.52</td>
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<td>0.68</td>
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<tr>
<td>Case 3</td>
<td>0.88</td>
<td>0.48</td>
<td>-0.12</td>
<td>0.62</td>
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</table>

**Table 4** Statistic values of significant wave height and significant wave period with SW-002 Filter Combination.

![Fig. 4](image_url) Comparison of optimal filtered MWR data (case3, red) and Buoy data (blue) with no filtered MWR data (white): (a) Time series of significant wave height; (b) Time series of significant wave period; (c) Scatter plots of significant wave height; (d) Scatter plots of significant wave period.
height was marginal, while the wave period still exhibited numerous spikes. As described in section 2.2, the impact of applying the optimal filter is not conspicuous for the wave height, mainly because the installation height of the MWR is appropriate compared to other data, and observations for the displacement of sea level have been adequately addressed, resulting in a less pronounced improvement effect. Nevertheless, the accuracy declined considerably as the waves were underestimated in five-meter or higher wave conditions, typically during extreme weather events such as typhoons. It has been reported that only applying filters is limited in improving the quality of MWR wave observation data.

3. Quality Enhancement of Wave Radar Data with Artificial Neural Network

3.1 Artificial Neural Network

Different combinations of filters from the MWR software were tested to identify the optimal filter for the Ieodo sea area and applied to the MWR. On the other hand, despite these efforts, numerous errors persist with the wave period, and the reliability of observation data sharply diminishes during 3 m or higher wave conditions. An artificial neural network (ANN) is commonly used across diverse fields when a clear correlation between two physical quantities is not evident because of its ability to discern and replicate nonlinear and potential correlations between two values (Kim et al., 2021; Park et al., 2021; Wei, 2021; Yun et al., 2022). Thus, this study used the filtered MWR observation data as the input value and the Jeju Southern Ocean Buoy 1 data as the actual value. An ANN model was then developed and trained to improve the data by learning the nonlinear relationship between these two data sets, enhancing the overall quality of the IORS observation data. The ANNs are designed to mimic human brain activities and are implemented as a network of nodes replicating the electrical signal transmission process of neurons or brain nerve cells.

The architecture of the ANN primarily comprises input, hidden, and output layers, each containing numerous nodes. The input and output layers are single layers in which the number of nodes corresponds to the number of inputs and outputs accordingly. A hidden layer is situated between the input and output layers and is considered a hyperparameter that must be determined through trial and error by the user, as the optimal architecture varies depending on the characteristics of the input and output data. In each node, a weighted sum was calculated by incorporating a bias, which serves as a threshold for deciding whether to output a signal, and a weight, indicating the importance of the signal from the previous node. This value is then applied to an activation function, and the resulting output is passed to the next node as an input. This computation process occurs sequentially across all connected nodes as the input passes through each layer of the ANN. The final output, derived from a series of computations from the input layer to the output layer, is used to calculate loss from the target value via a loss function. This loss is then used to update the weights and biases of each node through backpropagation. Neural network training involves optimizing values by allowing the network to learn the relationship autonomously between the input and target values while fine-tuning parameters, such as the weights and biases, to achieve highly accurate results. Training is repeated either for a user-specified number of iterations or until the model achieves the accuracy level set by the user.

3.2 ANN Data

This study designed an ANN model to learn potential nonlinear correlations between data and generate highly dependable wave data based on significant wave height and period information from MWR observation data. Initially, the node count of the output layer was configured as two to enable the simultaneous output of the significant wave height and significant wave period, enhancing the practicality of the wave data regeneration model. Subsequently, the correlation between the target values (significant wave height and significant wave period acquired from Jeju Southern Ocean Buoy 1) and approximately 40 observation parameters gathered by the MWR were examined to identify a significant input variable. The analysis revealed strong correlations in significant wave height and significant wave period of the MWR, as well as in wave skewness and wave steepness. This is because the dependence of the MWR on wind-induced sea level fluctuations for continuous observation is a significant indicator of the wave height, while the wave skewness and steepness serve as significant indicators of wave period. Considering that the reliability of MWR observation data decreases during periods of low wind speed, the correlation was analyzed with the buoy observation data using the wind direction and wind speed data of the IORS. Both were utilized as input variables when developing the ANN model owing to the strong correlation between wind speed and wave height. Furthermore, the input parameters with different scales used in this study were normalized to enhance reliability and ensure they were equally weighted in the ANN model. In this study, the input data for the ANN model consisted of significant wave height and period from the MWR, as well as wave steepness, wave skewness, and wind speed from the IORS. In contrast, the output data were set to show the significant wave height and period from the Jeju Southern Ocean Buoy. The range for each data type matched that of the data used in the comparative analysis with the buoy data. The data were separated into training and testing sets at an 8:2 ratio, comprising 40,011 data points for training the ANN model and 10,002 data points for testing it (Table 5). Stratified sampling was used to enhance the generalization performance of the model, recognizing that the performance of observation equipment is greatly affected by the environment and that the distribution rate of high wave events is extremely low, considering the actual nature of observed waves. Stratified sampling involves data-splitting to maintain the user-specified ratio (strata) when the data composition is unbalanced. Accordingly, training and test datasets maintain the original data's characteristics, promoting model stability and applicability by effectively learning from a diverse range of data. The input values were divided into training and test.
datasets using stratified sampling based on the significant wave height and period of the buoy, representing actual values, and wind speed data, which significantly impacts MWR observations. Fig. 5 shows the composition of each dataset strata to determine the optimal reference value, ensuring that the data distribution of statistical measures (RMSE, correlation coefficient) is suitable for the buoy data.

### 3.3 ANN Optimization

PyTorch, an open-source Python machine learning library, was used to develop a deep neural network to enhance the quality of MWR wave data. The optimization and performance assessment of the ANN model during training was conducted in a workstation environment featuring an RTX 2080Ti GPU and an Intel Xeon 4210 CPU. Optimization involves finding the best combination of hyperparameters to enable a model to reach its target accuracy through iterative learning. These hyperparameters, such as hidden layer architecture, activation function, batch size, and learning rate, were determined empirically by a programmer.

A hidden layer is where various computations involving weighted sums and activation functions are performed sequentially upon receiving a signal from nodes in the input layer. Typically, as the number of layers deepens and the nodes increase, the performance of

---

**Table 5** Data used in the ANN model

<table>
<thead>
<tr>
<th>Value</th>
<th>Data period</th>
<th>Input data</th>
<th>Output data</th>
<th>Label data</th>
<th>Training and test data</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>2014.03.14 – 2021.09.08</td>
<td>Case 3 $H_s$, $T_s$, Sk, St</td>
<td>$H_s$, $T_s$</td>
<td>Jeju Southern Ocean buoy</td>
<td>40011 (8): 10002 (2)</td>
</tr>
</tbody>
</table>

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**Fig. 5** Composition of the training data (outer circle) and test data (inner circle) by stratified sampling: (a) Composition sampled by stratified the buoy significant wave height; (b) Composition sampled by stratified buoy significant wave period; (c) Composition sampled by stratified IORS wind speed.

**Fig. 6** Optimal ANN model structure (five inputs, five hidden layers, and two outputs)
Quality Enhancement of MIROS Wave Radar Data at Ieodo Ocean Research Station Using ANN

The model improves, enabling more accurate identification of data features. On the other hand, the model complexity escalates with the depth of hidden layers and the number of nodes, potentially leading to overfitting, where the model parameters become excessively tailored to the training data, resulting in diminished performance on the test data. In addition, the heightened computational workload prolongs the time required for a neural network to converge. Therefore, this study prefers a neural network demonstrating a convergence speed that maintains accuracy and practicality. To determine the appropriate architecture, 300 different neural network models with varying complexities were designed and evaluated. These models sequentially incorporated two to seven hidden layers and varied the number of nodes in each layer from eight to 128. Therefore, a model with five hidden layers, consisting of 128, 64, 64, 64, and 8 nodes, was the most suitable for representing the dataset used in this study (Fig. 6).

Subsequently, the optimal combination of hyperparameters for the best learning outcomes was determined for six parameters: activation function, learning rate, batch size, loss function, epoch, and optimizer function, as shown in Table 6. The optimization process of hyperparameters is as follows. First, nonlinearity is introduced into the neural network through activation functions, a critical factor in determining the network performance. Furthermore, a learning rate was chosen to adjust the extent of modification for each weight associated with the activation functions. In this study, Learning rates of 0.001 and 0.0005 were applied to the sigmoid function and hyperbolic tangent function, proposed in the early stages of neural network research, the ReLU (Rectified Linear Unit), which is widely used in many studies by effectively solving the gradient vanishing problem frequently encountered with the sigmoid function and hyperbolic tangent function, and the Leaky ReLU function, which resolves the issue of dying neurons in ReLU, respectively, to assess the performance of each function (Table 6). The same activation function was used for the nodes in all hidden layers. On the other hand, a linear function was applied as the activation function of the final hidden layer to ensure the output layer produces an arbitrary value because the neural network in this study was designed for regression analysis to estimate the correlations between variables. As a result, $R^2$ was identified as the optimal hyperparameter combination in terms of accuracy and practicality, based on the number of epochs required for convergence and the loss observed in the test data (Fig. 7).

Furthermore, a mean squared error function was used as the loss function to measure the discrepancies between the output and target values of the model. The Adam optimizer was chosen to minimize the...
loss through gradient descent, where it seeks the minimum value using the gradient (a derivative of the loss function). Adam is widely acknowledged in various studies for its adaptive learning rates, which adjust according to the fluctuations in curvature to find the minimum value while also providing momentum to the learning speed. After numerous trials and errors, the batch size, representing a data unit used for a neural network to update weights once during learning, was determined to be 64. The number of epochs representing a full cycle through the training dataset was unrestricted. On the other hand, the risk of overfitting was mitigated using an early stopping mechanism, which automatically terminates learning if improved performance is not achieved within 20 epochs.

3.4 Result of ANN application

The neural network was optimized by optimal hyperparameters for the provided data. The optimal hyperparameters for the provided data. The performance of the optimized neural network on the MWR observation data was assessed by comparing it before and after applying the neural network. The results are depicted in a scatter plot and statistical measures in Fig. 8 and Table 7, respectively. When the results after applying the ANN were compared with the data before applying the ANN (case 3), there was a 0.02 increase in the correlation coefficient, a 0.06 decrease in RMSE, and a 0.1 decrease in bias for the significant wave height. Similarly, for the significant wave period, there was a 0.19 increase in the correlation coefficient, a 0.61 decrease in RMS, and a 0.387 decrease in bias. As a result, while there is little discernible enhancement in significant wave height after applying the ANN, the issue of overestimation in significant wave period has been notably addressed through the ANN. Such performance improvement is also demonstrated in the scatter plot in Fig. 8. The significant wave height remains largely unchanged, as a strong correlation was already apparent before implementing the ANN. On the other hand, the distribution of significant wave period data has become increasingly concentrated along the diagonal line. Overall, for high wave conditions with a 3 m or higher wave height, there was a 0.07 increase in the correlation coefficient, a 0.07 decrease in RMSE, and a 0.04 decrease in bias for the significant wave height, while there was a 0.09 increase in the correlation coefficient, a 0.19 decrease in RMSE, and a 0.14 increase in bias for the significant wave period (Table 8). The reliability of the MWR observation data was improved using the ANN.

Table 7 Statistic values of significant wave height and wave period with ANN and Optimal Filter (Case 3)

<table>
<thead>
<tr>
<th></th>
<th>Wave height</th>
<th>Wave period</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>Correlation</td>
<td>RMSE</td>
</tr>
<tr>
<td>Case 3</td>
<td>0.88</td>
<td>0.47</td>
</tr>
<tr>
<td>ANN</td>
<td>0.90</td>
<td>0.41</td>
</tr>
</tbody>
</table>

Table 8 Statistical values of significant wave height and wave period under high wave conditions with ANN and Optimal Filter (Case 3)

<table>
<thead>
<tr>
<th></th>
<th>Wave height</th>
<th>Wave period</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>Correlation</td>
<td>RMSE</td>
</tr>
<tr>
<td>Case 3 ( $H_s \geq 3$ m)</td>
<td>0.66</td>
<td>0.94</td>
</tr>
<tr>
<td>ANN ( $H_s \geq 3$ m)</td>
<td>0.73</td>
<td>0.87</td>
</tr>
</tbody>
</table>
even under high wave conditions.

On the other hand, the regenerated wave data still contain some errors when a wave, 3 m or higher, occurs. This is because the dataset used for ANN training contained only 7% of data with wave heights above 3 meters, leading the model to perform better in low wave environments compared to high wave conditions. Therefore, to enhance the model performance under high wave conditions by addressing the imbalance in data distribution, it is essential to use various data augmentation methods, such as the synthetic minority over-sampling technique with Gaussian noise (SMOGN) capable of amplifying data in minority sections, such as high waves, and reducing the normal data in majority sections, all while preserving the characteristics of the existing data. Nevertheless, there are few cases of applying these data augmentation techniques to marine data, so further research is needed to optimize them for this specific data type.

4. Conclusions

This study collected raw MWR data (DF025) and conducted a comparative analysis against the observation data from the Jeju Southern Ocean Buoy, which is currently operated by the Korea Hydrographic and Oceanographic Agency, to enhance the quality of wave observation data from the IORS. Four types of filters included in the MIROS software (SW-002) were applied to eliminate spikes caused by backscattering microwaves under wind speeds of 3 m/s or lower. The optimal filter was to use three filters in combination, excluding the “reduce noise frequency” filter, which tends to underestimate wave height, because applying filters individually has limited effectiveness in improving quality. After applying the most optimal filter combination, most spikes were eliminated, but the improvement in wave height was minimal, and numerous errors in wave height persisted. In addition, errors continued to occur frequently under 5 m or more wave conditions, particularly during extreme weather conditions. This study applied an ANN designed to identify the generalization of neural networks. The hyperparameters that exhibited a strong correlation with the buoy data in the MWR observation data were chosen as input variables to facilitate efficient learning. A neural network architecture that achieves the best learning outcomes in accuracy and practicality was designed through iterative learning by adjusting the hyperparameters until the optimal learning outcomes were achieved. Ultimately, the optimal learning outcomes were achieved with a neural network featuring five hidden layers, with node counts of 64, 64, 32, 16, and eight, respectively, a learning rate of 0.0005, and a batch size of 64. Comparing the learning outcomes with the initial data, there was a notable enhancement in the reliability of wave period (correlation coefficient increased by 0.19 and RMSE decreased by 0.61), as well as for waves 3 m or higher (correlation: 0.07 increase for wave height and 0.09 increase for wave period; RMSE: 0.07 decrease for wave height, and 0.19 for wave period). These findings suggest that the proposed ANN model significantly enhanced the reliability of MWR wave data observed at the IORS. Given the regional characteristics of the IORS, located along typhoon pathways, these results are anticipated to offer valuable insights into designing waves for safeguarding coastal and offshore structures from high waves during extreme weather conditions. In addition, they are expected to serve as foundational data for improving wave prediction models. Moreover, implementing the proposed method with other MWR wave observation data beyond the IORS will help enhance the reliability of remote wave observation data. On the other hand, errors persist due to overfitting during ANN training, attributed to inadequate data distribution for waves of 3 m or higher. Therefore, various data augmentation techniques, such as SMOGN, are recommended to address the data imbalance issue. Further research is needed to identify suitable data augmentation techniques for wave data, such as that from the MWR because no established precedent has demonstrated its efficacy with marine data. In addition, applying ANNs to wave direction data is worthwhile, considering their proven effectiveness in enhancing the significant wave height and period of remote wave observation data.

Conflict of Interest

Kideok Do serves as a member of the journal publication committee of the Journal of Ocean Engineering and Technology, but he had no role in the decision to publish this article. No potential conflict of interest relevant to this article was reported.

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Voronoi Diagram-based USBL Outlier Rejection for AUV Localization

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KEYWORDS: USBL, Outlier detection, Voronoi diagram, Acoustic sensors

ABSTRACT: USBL systems are essential for providing accurate positions of autonomous underwater vehicles (AUVs). On the other hand, the accuracy can be degraded by outliers because of the environmental conditions. A failure to address these outliers can significantly impact the reliability of underwater localization and navigation systems. This paper proposes a novel outlier rejection algorithm for AUV localization using Voronoi diagrams and query point calculation. The Voronoi diagram divides data space into Voronoi cells that center on ultra-short baseline (USBL) data, and the calculated query point determines if the corresponding USBL data is an inlier. This study conducted experiments acquiring GPS and USBL data simultaneously and optimized the algorithm empirically based on the acquired data. In addition, the proposed method was applied to a sensor fusion algorithm to verify its effectiveness, resulting in improved pose estimations. The proposed method can be applied to various sensor fusion algorithms as a preprocess and could be used for outlier rejection for other 2D-based location sensors.

1. Introduction

Autonomous underwater vehicles (AUVs) have been deployed for various missions in underwater environments, making the accurate localization of AUVs crucial for successful mission execution. The ultra-short baseline (USBL) and Doppler velocity log (DVL) are the most commonly used sensors for tracking AUV positions. DVL, as one of the primary sensors for AUVs, emits acoustic waves from four transducers towards the seabed to measure velocity, and the measured velocity was integrated to estimate the AUV position. On the other hand, the integration constant accumulates with time, resulting in drift error, which can decrease accuracy when applied to the AUV navigation system. USBL, however, analyzes acoustic signals transmitted from an array to obtain the relative position of the AUV. Although USBL can secure stable position data without Drift Error over long operational periods, the data acquisition cycle may be extended depending on the distance between the transducer and transponder. In addition, outliers can occur due to multipath effects under varying environmental conditions. Missing data may occur in severe cases (Fig. 1). Sensor fusion-based approaches have been studied widely to overcome these drawbacks while integrating the advantages of different sensor types. More accurate and reliable localization data can be provided by combining information obtained from various sensors, enhancing the navigation and operational performance of AUVs.

Sensor fusion technology to improve AUV localization performance has been investigated in various ways. Studies include sensor fusion between the DVL and USBL based on particle filters (Rigby et al., 2006) and underwater navigation systems combining DVL and inertial measurement units (IMU) based on the unscented Kalman filter (UKF) (Krishnamurthy and Khorrami, 2013). Nevertheless, there is a high occurrence of outliers due to the USBL characteristics. Using data

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containing outliers as input values for the sensor fusion filter can cause critical flaws in the accuracy and reliability of underwater navigation systems. Therefore, improving the technology for removing outliers from USBL data significantly enhances the AUV navigation performance.

Previous studies on removing USBL outliers include using a median filter to calculate the median, measuring the distance between the median and current data, and considering it an outlier if it exceeds a set threshold (Morgada et al., 2015). Another study modeled the measurement errors and removed outliers based on the Mahalanobis distance (Lee et al., 2022). Furthermore, a moving average filter was proposed to consider the last N data points of a time series in a sliding window for support vector regression (SVR) training to remove the USBL outliers (Liu et al., 2019). Although these methods can be compatible with various systems, they involve many parameters for optimization and lack a structured method for selecting global parameter values, making parameter selection for the proposed method challenging.

Currently, commercially available underwater acoustic sensors often include outliers in their output because of their inherent characteristics. These outliers can distort the correlation between sensor data, making the estimated position or state inaccurate, degrading system performance, and causing unexpected errors and instability. Sensor data outliers must be identified and filtered out to ensure that sensor fusion algorithms receive accurate and reliable inputs because accurate localization is crucial in underwater environments.

This paper proposes a novel algorithm that utilizes Voronoi diagrams to detect and remove outliers from USBL data. The structure of this paper is as follows. Section 2 explains the AUV navigation system modeling, the proposed outlier removal approach, and the localization algorithm. Section 3 validates the proposed algorithm using Sea trial data. In addition, it outlines the design and application of a sensor fusion model to analyze the impact of outlier removal on sensor fusion results and presents comparative analyses.

2. Methodology

2.1 System Modeling

AUVs are generally described by six degrees of freedom motion, and the sensors primarily used for AUV localization considered in this study are the USBL, DVL, and IMU. The USBL measures the position in the global frame, while the DVL measures the velocity in the body-fixed frame. The motion of an AUV can be modeled based on the position, velocity, and attitude values obtained from sensors attached to the AUV (Lee et al., 2017). Considering a box-type hovering AUV used for precise surveys, the sway and heave motions are assumed to be negligible. The motion of the AUV can be expressed using the velocity obtained from the DVL and the attitude values from the IMU, as expressed in Eq. (1).

\[
x_{k+1} = [x] \\
y \\
z \\
u \\
v \\
\phi_k = \begin{bmatrix} x + (u_k \cdot \cos \phi_k - v_k \cdot \sin \phi_k) \cdot dt \\
y + (u_k \cdot \sin \phi_k + v_k \cdot \cos \phi_k) \cdot dt \\
z + w_k \cdot dt \\
u_k \\
v_k \\
\phi_k \end{bmatrix}
\]

where \(x, y, z\), and \(\phi\) represent the position and azimuth of the AUV in the global frame, while \(u, v\), and \(w\) represent the velocity of the AUV in the body-fixed frame. Using the velocity values in the body-fixed frame and \(\phi\), the velocity components \(\dot{x}, \dot{y}\), and \(\dot{z}\) of the AUV in the global frame can be expressed as Eq. (2) (Caccia and Veruggio, 2000), and the motion in the global frame can be represented over time.

\[
\dot{x} = u_k \cdot \cos \phi_k - v_k \cdot \sin \phi_k \\
\dot{y} = u_k \cdot \sin \phi_k + v_k \cdot \cos \phi_k \\
\dot{z} = w_k \\
\]

A hovering-type AUV can be described with three degrees of freedom, assuming it moves at a constant depth underwater. The system model can be simplified to include the velocity values in the global frame using Eq. (3).

\[
x = \begin{bmatrix} x \cdot dt \\
y + \dot{y} \cdot dt \\
u \cdot \cos \phi_k - v_k \cdot \sin \phi_k \\
u_k \\
v_k \cdot \sin \phi_k + v_k \cdot \cos \phi_k \\
\phi_k \end{bmatrix}
\]

The observation matrix measures all values in the state variables, and the characteristics of each sensor can be modeled through the measurement noise \(v\) introduced from the sensors using Eq. (4).

\[
y_k = H_k x_k + v_k \\
H_k = [I]_{1 \times 7}
\]

In addition, the system model noise is modeled by adding \(\omega\) using Eq. (5).

\[
x_{k+1} = f(x_k) + \omega_k
\]

2.2 Voronoi Diagram

The proposed algorithm consists of three main steps. First, USBL data is divided into regions using Voronoi diagrams, defining an area centered around each data point and structuring the data spatially. Second, a query point is calculated using a moving average filter. Finally, the presence of the query point in the divided regions is examined. The data in that area are treated as an inlier if a query point
Voronoi Diagram-based USBL Outlier Rejection for AUV Localization

Fig. 2 Construction of the Voronoi diagram

exists in the structured area. The data are classified as an outlier if the query point is not found.

The process of producing Voronoi diagrams involves connecting the closest feature points to a single feature point with line segments using Euclidean distance and then drawing perpendicular bisectors at the center of these segments to separate the areas of the feature points. Performing this process for all feature points results in the production of polygonal regions (Voronoi cells) (Fig. 2) (Sack and Urrutia, 1999).

A Voronoi cell represents an area on a plane centered around each feature point \( S_i \), with each feature point forming a cell. The shape and size of the cells vary according to the positions of the feature points. The cell size decreases if the feature points are in close proximity and vice versa. A Voronoi edge is a line segment that forms the boundary of each cell, located at the midpoint between the feature point of the cell and the closest feature point of the neighboring cell. A Voronoi vertex is the point where the Voronoi edges meet, formed at the intersection of three or more cells, determining the structure of the diagram (Fig. 3).

When a query point \( \hat{x}_q \) is given, the closest Voronoi cell location \( \emptyset_S \) is found based on the distance between \( \hat{x}_q \) and the feature points of the Voronoi cells (Eq. 6) (Edelsbrunner and Seidel, 1985).

\[
\emptyset_S = \arg\min_{S_i \in S} \| x_q - S_i \|
\]  

Fig. 3 Example of generated Voronoi cells

Fig. 4 Stepwise update process of the Voronoi diagram algorithm
Eq. 6 is used to find the Voronoi cell that minimizes the distance between the query point and the feature points with \( \emptyset \) indicating the location of the Voronoi cell containing the query point. Query points belonging to the same Voronoi cell can be expressed as follows (Eq. 7).

\[
R_i = \{ \hat{x}_k \in \mathbb{R}^d | \emptyset(\hat{x}_k) = S_i \}
\]

This characteristic of Voronoi diagrams effectively distinguishes the regions for all feature points and identifies to which Voronoi cell the query point belongs. Therefore, they are often used in algorithms for classification or clustering. On the other hand, this property can be used to determine the outliers using query points that effectively represent data trends. Query points are calculated by averaging observations from time-series data and moving forward to calculate the average at the next time point (Eq. 8).

\[
\hat{x}_k = \frac{S_{k-n+1} + S_{k-n+2} + \ldots + S_k}{n}
\]

Using these features, USBL data is segmented into polygonal regions with feature points, and the moving average filter is used as the query point to detect USBL outliers based on the constraint conditions of Eq. (9). \( \hat{x}_k \) is the moving average value as the query point, and \( R_i \) is the Voronoi cell generated by the feature point \( x_k \). The feature point is determined to be an inlier if the query point is located in the Voronoi cell of it. Fig. 4 shows the step-by-step process of the Voronoi diagram-based USBL outlier removal.

\[
\begin{align*}
S_i &= \text{Inlier} \quad \text{if} \quad \hat{x}_k \in R_i \\
S_i &= \text{Outlier} \quad \text{if} \quad \hat{x}_k \notin R_i
\end{align*}
\]

### 2.3 Advanced Query Point Calculation

The moving average filter calculates the output by averaging the previous input data. As the window size increases, the output is computed as the average of the earlier values of the input data, which can introduce a delay. When concurrency is required, the time delay issue can be addressed by proposing an alternative calculation for the query point.

For example, the time delay of the query point can be mitigated using an exponentially weighted moving average (EWMA) filter (Eq. 10).

\[
\hat{\dot{x}}_k = \alpha \dot{S}_k + (1 - \alpha) \cdot \hat{\dot{x}}_{k-1}
\]

The weight \( \alpha \) can be adaptively applied by considering the difference between the current USBL measurement and the previous measurements (Eq. 11).

\[
\alpha = 1 - \frac{\| S_k - \dot{S}_{k-1} \|}{\dot{K}_u}
\]

where, \( \dot{K}_u \) is a value adaptively calculated by considering the UUV speed \( u \), the USBL acquisition period \( T \), and the expected error range \( \epsilon \) of the USBL, as expressed in Eq. (12).

\[
\dot{K}_u = u \cdot T + \epsilon
\]

### 2.4 Sensor Fusion

A sensor fusion structure was designed to compare the results and verify the effectiveness of the proposed algorithm. The localization algorithm is based on the Kalman filter, which fuses sensor data to estimate the position of the AUV. A Federated Kalman filter structure was adopted and divided into a master filter and a local filter to ensure the convenience of sensor data fusion and the robustness of the DVL and IMU position data (Fig. 5) (Carlson, 1988). The local filter uses the values from the DVL and IMU to estimate the velocity in the global frame, which is then used by the master filter to estimate the final position. The proposed algorithm is applied for the USBL data filtering (Eq. 13).

\[
\begin{bmatrix}
\dot{x} \\
\dot{y} \\
\dot{u} \\
\dot{v} \\
\phi
\end{bmatrix} =
\begin{bmatrix}
\dot{x}_u \cdot \cos \phi - \dot{v}_u \cdot \sin \phi \\
\dot{y}_u \cdot \sin \phi + \dot{v}_u \cdot \cos \phi \\
\dot{x}_u \\
\dot{y}_u \\
\phi
\end{bmatrix}
\]

The measurements in the local filter include the velocities \( u, v \) obtained from the DVL and the yaw angle \( \phi \) obtained from the IMU. The state variables estimate the velocities \( \dot{x}, \dot{y} \) in the global frame, the velocities \( \dot{u}, \dot{v} \) in the body-fixed frame, and the yaw angle \( \phi \). The observation variables are the velocities \( u, v \) from the DVL and the yaw angle from the IMU (Eq. 14).

\[
\begin{align*}
\mathbf{x}_{L: DVL}^T &= [\dot{x}, \dot{y}, u, v, \phi]^T \\
\mathbf{z}_{L: DVL}^T &= [u, v, \phi]^T
\end{align*}
\]

The system model is expressed as Eq. (15), where \( w_K^L \) and \( v_K^L \) are assumed to follow a Gaussian distribution for the system noise and measurement noise, respectively (Eq. (15)).

\[
\begin{align*}
x_k^L = g(x_{k-1}^L) + w_K^L, \quad w_K^L \sim \mathcal{N}(0, Q^L_k) \\
\mathbf{z}_k^L = h(x_k^L) + v_K^L, \quad v_K^L \sim \mathcal{N}(0, R^L_k)
\end{align*}
\]

Fig. 5 Sensor fusion structure for validation of the proposed method.
The Jacobians of \( g(x_{k-1}) \) and \( h(x_k) \) are expressed in Eq. (16) because the system model of the filter is nonlinear.

\[
G = \frac{\partial g}{\partial x}|_{x_{k-1}} \quad H^M_k = \frac{\partial h}{\partial x}|_{x_k}
\]

\[
G = \begin{bmatrix}
\cos \phi_k & -\sin \phi_k & -u_k \sin \phi_k + v_k \cos \phi_k \\
\sin \phi_k & \cos \phi_k & \quad u_k \cos \phi_k - v_k \sin \phi_k \\
0 & 0 & \quad I_{2 \times 2}
\end{bmatrix}
\]

\[
H^M_k = [0_{2 \times 2} \quad I_{2 \times 2}]^T \sim N(0, R^M_k)
\]

The state prediction and update stages are defined by Eq. (17).

Prediction step
\[
x_{k-1} = g(x_{k-1})
\]

\[
P^M_{k|k-1} = G_k P^M_{k-1|k-1} G_k^T + Q^M_k
\]

Correction step
\[
K^M_k = P^M_{k|k-1} H_k^T [H_k^T P^M_{k|k-1} H_k^T + R^M_k]^{-1}
\]

\[
x^M_k = x_{k-1} + K^M_k (z_k - h(x_k))
\]

\[
P^M_{k|k} = (I - K^M_k H_k) P^M_{k|k-1}
\]

In the master filter, the USBL data and the results from the local filter are used as input to estimate the AUV position. The USBL data is preprocessed using the proposed algorithm to remove outliers before being input into the master filter, and the system model is expressed linearly. The state variables \( x, y, \dot{x}, \dot{y} \) were obtained from the USBL and the local filter, respectively, and the system model of the master filter is given by Eq. (18).

\[
\begin{align*}
x^M_k &= [x, y, \dot{x}, \dot{y}]^T \\
P^M_k &= \begin{bmatrix} I_{2 \times 2} & I_{2 \times 2} \end{bmatrix}^T \\
&= \begin{bmatrix} I_{2 \times 2} & I_{2 \times 2} \end{bmatrix}
\end{align*}
\]

System equation
\[
x^M_k = F^M_k x^M_k + w^M_k, \quad w^M_k \sim N(0, Q^M_k)
\]

Measurement equation
\[
z^M_k = H^M_k x^M_k + v^M_k, \quad v^M_k \sim N(0, R^M_k)
\]

3. Experimental Result

3.1 Parameter Analysis

Experiments were conducted to verify the performance of the proposed algorithm and analyze the characteristics of the user parameters. The experimental data were obtained from the coastal waters of Jangil-ri in Pohang, with a depth of approximately three meters. A transceiver was installed on a coastal pontoon structure. A transponder was mounted on a vessel to acquire underwater sensor data along the planned route of the vessel (Fig. 6). The USBL used in...
the experiment was the S2C R 18/34 from Evologics, with main specifications including a frequency of 18–34 kHz, a distance error of 0.01m, and an azimuth error of 0.1 degrees.

Fig. 7 presents the USBL data obtained from the experiment. The blue, red, and green points represent the USBL data identified as inliers, outliers, and GPS data for comparison, respectively. The GPS and USBL data were compared using root mean square error (RMSE). The USBL data were upsampled and interpolated based on the GPS data to calculate the RMSE using GPS and USBL signals with different periods.

The algorithm was applied using the moving average to the query points of the Voronoi diagram, and the results were compared to find the optimal window size, which is a user parameter. The RMSE was smallest when the window size was 15 (Table 1). The number of outliers increased as the window size increased, and a delay in measurement values occurred. In contrast, the delay in measurement values decreased when the window size decreased, but the robustness against large errors, such as spike noise, tended to decline.

Table 1  Comparison with the change in window size

<table>
<thead>
<tr>
<th>Window size</th>
<th>Inlier</th>
<th>Outlier</th>
<th>Outlier proportion</th>
<th>RMSE (m)</th>
</tr>
</thead>
<tbody>
<tr>
<td>5</td>
<td>43</td>
<td>16</td>
<td>27%</td>
<td>3.2655</td>
</tr>
<tr>
<td>10</td>
<td>35</td>
<td>24</td>
<td>40%</td>
<td>2.8592</td>
</tr>
<tr>
<td>15</td>
<td>36</td>
<td>23</td>
<td>39%</td>
<td>2.3236</td>
</tr>
<tr>
<td>20</td>
<td>34</td>
<td>25</td>
<td>42%</td>
<td>4.4192</td>
</tr>
<tr>
<td>25</td>
<td>29</td>
<td>30</td>
<td>51%</td>
<td>3.8119</td>
</tr>
</tbody>
</table>

In addition, the results were compared when the algorithm was applied using the weighted moving average for the query points of the Voronoi diagram (Table 2). The weights used in the weighted moving average were calculated using Eq. (11), and the gain $K_\epsilon$ in Eq. (11) was determined based on the expected error range $\epsilon$ of the USBL, which is a user parameter. The performance change of the algorithm according to variations in $\epsilon$ was analyzed, as shown in Table 2.

Table 2  Comparison with change of parameter $\epsilon$

<table>
<thead>
<tr>
<th>$\epsilon$</th>
<th>Inlier</th>
<th>Outlier</th>
<th>Outlier proportion</th>
<th>RMSE (m)</th>
</tr>
</thead>
<tbody>
<tr>
<td>0.1</td>
<td>43</td>
<td>16</td>
<td>27%</td>
<td>3.0784</td>
</tr>
<tr>
<td>0.2</td>
<td>43</td>
<td>16</td>
<td>27%</td>
<td>2.5408</td>
</tr>
<tr>
<td>0.3</td>
<td>42</td>
<td>17</td>
<td>29%</td>
<td>2.7521</td>
</tr>
<tr>
<td>0.4</td>
<td>44</td>
<td>15</td>
<td>25%</td>
<td>3.4191</td>
</tr>
<tr>
<td>0.5</td>
<td>41</td>
<td>18</td>
<td>30%</td>
<td>3.3720</td>
</tr>
<tr>
<td>0.6</td>
<td>44</td>
<td>15</td>
<td>25%</td>
<td>3.6757</td>
</tr>
</tbody>
</table>
hand, smaller weights were assigned to large outliers as $\epsilon$ decreased, resulting in more robust results. In contrast, data that fell within the normal range were sometimes classified as outliers when $\epsilon$ was too small. When $\epsilon=0.2$, the number of normal values improved compared to the case with a window size of 15 lists in Table 1.

The performance of the proposed algorithm was evaluated by comparing it with other outlier removal and noise reduction algorithms. The comparison groups included raw USBL data, Mahalanobis distance, moving average, and minimum covariance determinant (MCD) (Hubert and Debruyne, 2010). For meaningful outlier detection, the threshold for the Mahalanobis distance and MCD was set to apply the three-sigma rule, defining values more than three times the standard deviation as outliers.

The comparison results with the data obtained from the experiments showed that the RMSE improved by 4.05% for the Mahalanobis distance, 10.5%, 26.57%, and 29.84% for the sliding window, MCD, and the proposed method, respectively, compared to the raw USBL data (Table 3).

### 3.2 Performance Evaluation of Outlier Removal

This study used real-world data from the AUV Cyclops, developed by Pohang University of Science and Technology (POSTECH), to verify the effectiveness of the proposed algorithm on underwater navigation systems (Fig. 9). The AUV for data acquisition is a hovering type AUV, with its main specifications listed in Table 4 (Joe et al., 2021; Pyo et al., 2015). The data acquisition site was the coastal area of Jangil-ri in Guryongpo, Pohang.

The position acquisition sensors were an S2C/R 18/34 USBL from Evologics and a Workhorse Navigator DVL from Teledyne. The attitude data were obtained using the HG1700 from Honeywell.

### Table 3 RMSE according to the outlier removal method

<table>
<thead>
<tr>
<th>Method</th>
<th>RMSE (m)</th>
<th>Improvement rate (%)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Raw data</td>
<td>3.6217</td>
<td>-</td>
</tr>
<tr>
<td>Mahalanobis</td>
<td>3.4806</td>
<td>4.05</td>
</tr>
<tr>
<td>Moving Avg.</td>
<td>3.2775</td>
<td>10.5</td>
</tr>
<tr>
<td>MCD</td>
<td>2.6594</td>
<td>26.57</td>
</tr>
<tr>
<td>Ours</td>
<td>2.5408</td>
<td>29.84</td>
</tr>
</tbody>
</table>

### Table 4 Specifications of AUV Cyclops

<table>
<thead>
<tr>
<th>Type</th>
<th>Hovering</th>
</tr>
</thead>
<tbody>
<tr>
<td>Weight (kg)</td>
<td>210 (air)</td>
</tr>
<tr>
<td>Dimension (m)</td>
<td>0.9 $\times$ 1.5 $\times$ 0.9</td>
</tr>
<tr>
<td>Depth rating (m)</td>
<td>100</td>
</tr>
<tr>
<td>Propulsion (W)</td>
<td>475 (8 thrusters)</td>
</tr>
<tr>
<td>Max. speed (m/s)</td>
<td>1.029</td>
</tr>
<tr>
<td>Sensors</td>
<td>Doppler Velocity log</td>
</tr>
<tr>
<td></td>
<td>Dual Frequency Imaging Sonar</td>
</tr>
<tr>
<td></td>
<td>Ultra-short baseline</td>
</tr>
<tr>
<td></td>
<td>Digital pressure transducer</td>
</tr>
<tr>
<td></td>
<td>Video camera</td>
</tr>
<tr>
<td></td>
<td>Fiber-optic gyro</td>
</tr>
</tbody>
</table>

![Fig. 9 AUV Cyclops](image)
The AUV Cyclops was operated in an area of 20 m × 15 m to acquire real-world data. The acquired data was plotted over the time axis in an offline environment using MATLAB (Fig. 10). The DR Nav of the DVL and IMU showed deviations from the USBL over time due to drift errors. Although the USBL data was free of drift errors, outliers were included in the sensor output.

The proposed method was used to remove the outliers from the USBL data. The results were compared with the raw USBL data and the DVL results (Fig. 11). The blue dotted line represents the raw USBL data, the black dashed line represents the dead reckoning (DR) results using DVL and IMU, and the red dotted line represents the results processed according to the proposed algorithm. Significant outliers, around 6m and 12m, occurred at points a and b. The proposed algorithm effectively detected and removed these outliers, resulting in improved USBL data.

Figs. 12 and 13 compare the performance of sensor fusion with and without the USBL outlier removal algorithm, respectively. The blue line represents the raw USBL data, and the black dashed line represents the dead reckoning results using DVL and IMU. The pink dots in Fig. 12 show the estimated position of the AUV when sensor fusion was performed without outlier removal. Fig. 13 shows the results using the USBL data after outlier removal using the proposed algorithm.

4. Conclusion

This study proposes a method for removing outliers from USBL data using a Voronoi diagram-based approach. The USBL data was segmented into regions based on the Voronoi diagram, and the outliers were identified by analyzing whether the weighted moving average query points, using adaptive weights, were within the Voronoi cell. Real-world data were collected using GPS and USBL to validate the
performance of the proposed method, and the results were compared based on the adaptive weight parameters. In addition, the effectiveness of the proposed algorithm was verified by applying it to field test data obtained from a real AUV.

An AUV localization algorithm was designed with a federated Kalman filter structure to verify the improvement of results using the proposed method as a preprocessing step for sensor data fusion in AUV localization. This algorithm performed sensor fusion of USBL, DVL, and IMU data. The application to real-world data showed that removing outliers from USBL data using the proposed algorithm enabled more robust AUV localization. This approach enhances the reliability of USBL data and emphasizes the importance of raw data processing techniques in underwater navigation systems. Future research will focus on refining USBL-based AUV localization by analyzing and correcting the error caused by the oscillating transceiver on the ship and on applying the findings to develop cooperative systems between unmanned surface vehicles and AUVs.

Conflict of Interest

The authors declare that they have no conflicts of interest.

Funding

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References


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Collision-Damage Analysis of a Floating Offshore Wind Turbine Considering Ship-Collision Risk

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\textsuperscript{2}CEO, UlsanLab Inc., Ulsan, Korea

KEYWORDS: Floating offshore wind turbine, Collision-damage analysis, Accidental limit state, Ship collision, Fracture analysis

ABSTRACT: As the number of offshore wind-power installations increases, collision accidents with vessels occur more frequently. This study investigates the risk of collision damage with operating vessels that may occur during the operation of an offshore wind turbine. The floater used in the collision study is a 15 MW UMaine VolturnUS-S (semi-submersible type), and the colliding ships are selected as multi-purpose vessels, service operation vessels, or anchor-handling tug ships based on their operational purpose. Collision analysis is performed using ABAQUS and substantiation is performed via a drop impact test. The collision analyses are conducted by varying the ship velocity, displacement, collision angle, and ship shape. By applying this numerical model, the extent of damage and deformation of the collision area is confirmed. The analysis results show that a vessel with a bulbous bow can cause flooding, depending on the collision conditions. For damage caused by collision, various collision angles must be considered based on the internal stiffener arrangement. Additionally, the floater can be flooded with relatively small collision energy when the colliding vessel has a bulbous bow.

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1. Introduction

Offshore wind turbines offer advantages such as securing large installation space and abundant wind resources compared with onshore ones. However, complex risk factors arise from the sea. Among them, ultimate limit states (ULS), such as typhoons, are considered in the design phase of offshore wind turbines, whereas the accidental limit state (ALS), which may occur during the operating period, is not sufficiently considered. The ALS must be considered in the design phase because collision accidents with ships may occur during the operation of marine structures. Although the occurrence frequency is low, the risk of such accidents is high.

In 2020, the fixed offshore wind turbine structure of the Borkum Riffgrund wind farm in Germany collided with a 26-m-long high-speed vessel (Renews, 2020). In 2021, a jack-up vessel mobilized for the installation of a wind farm in the waters of Huizhou City, China collided with a substructure (BBN, 2021). In 2022, the monopile substructure of a fixed offshore wind farm in the Netherlands North Sea collided with the bulk carrier “Julietta D,” and the installed substructure was dismantled (Durakovik, 2022). In the Netherlands’ Hallandse Kust Zuid offshore wind farm, a cargo ship collided with a
marine substation, which damaged the “HKZ Beta” jacket substructure (Windfair, 2022). In 2023, a cargo ship with 1,500 tons of grains collided with a wind turbine in the Orsted’s offshore wind farm in Germany (Mandra, 2023). In addition to the cases mentioned above, several collision cases may be undisclosed, and such collision accidents are expected to increase in future if offshore wind farms are expanded. Since floating offshore wind turbines (FOWTs) are likely to be constructed in a large wind farm, the collision of one unit may cause a series of accidents. Therefore, these collision risk factors must be examined for safe wind-farm operation.

Research on ship collision was initiated by Minorsky (1959), whereas Cloughley (1978) and Donegan (1982) highlighted the necessity for research on the collision of marine structures. Many studies pertaining to the collision of FOWTs are currently being conducted. Recently, Do et al. (2020) performed a drop impact test and a verification via numerical analysis for cylinders stiffened with rings and stringers. They developed a collision-damage estimation formula based on the verified numerical analysis. Ren et al. (2023) investigated the collision of an FOWT using a 1:30 scale model and compared the experimental and numerical analysis results. Vandegar et al. (2023) investigated the collision of a cylindrical FOWT and performed an analysis using a simplified numerical operation method.

In this study, the results of drop impact tests and numerical analysis were compared for the verification of the collision numerical analysis employed. The numerical analysis technique that showed good agreement with the test results was further analyzed. Do et al. (2023) conducted a collision-damage analysis for a semi-submersible floater that applied a taut mooring system and developed a residual-strength estimation formula based on the damage incurred. The floater of a floating wind turbine can be flooded below the sea level when damaged by ship collision. If the floater is inclined beyond the allowable inclination angle due to flooding, power production will cease. Even within the allowable inclination angle range, power-generation efficiency can be degraded. To predict the economic loss and damage caused by a collision accident at sea, one must identify the ALS that may occur during operation. Therefore, in this study, a 15 MW semi-submersible FOWT was selected, and scenarios were established for collision accidents that may occur during its operating period. In addition, damage to the floater and its flooding caused by collision were examined.

2. Analysis Technique Validation

2.1 Impact Test Model

Prior to performing collision-damage analysis, a comparative study was conducted with the results of the impact test for a stiffened cylinder model conducted by Do et al. (2018) to verify the numerical analysis technique. The impact test was conducted at the Structural Research Laboratory of the Department of Naval Architecture and Ocean Engineering, University of Ulsan. Fig. 1 shows the test facility. The test was conducted on a cylindrical model with both longitudinal stiffeners and ring stiffeners. This stiffener arrangement is common to most cylindrical floats. The knife-edge geometry was adopted for to the striker, and the impact-test conditions are summarized in Table 1. Numerical analysis for the verification was conducted using Abaqus 6.22 software (2022). As for the constraints, fixed boundary conditions were imposed on the bottom of the cylinder end plate in the same manner as the impact test. In the study of Do et al. (2018), the element size of the finite-element model was determined via element-convergence verification, and the element size in the collision area was approximately 1.6 times the thickness. In this study, a finite-element model was constructed based on the above as shown in Fig. 2. The fine-element size (5 mm × 5 mm) was applied to the collision area, whereas the 20 mm × 20 mm size was applied to other areas.

2.2 Material Properties

The material properties obtained by conducting a tensile test on the specimen used in the experiment were secured from the literature (Do et al., 2018). The input data that exerted the most significant effect on the results of collision analysis were the material properties. However, the material properties may differ between the steel material used in the experimental model and that used in the floater design. Therefore, the results of the static and dynamic tensile tests conducted using tensile specimens from the steel material used in the actual structure are required to obtain accurate results. Normally, the yield strength can be known in the initial design phase of the actual structure. However, in this study, the ultimate strength and tensile strain were estimated from the yield strength of the material using Eqs. (1) and (2) proposed by Park (2017). Park derived the equations by the regression analyses of 7,737 mill sheets of mild and high-tensile steels. To calculate the

![Fig. 1 Drop-test facility in University of Ulsan](image)

<table>
<thead>
<tr>
<th>Table 1 Impact-test condition</th>
<th>SS-Striker-I</th>
</tr>
</thead>
<tbody>
<tr>
<td>Drop height, (H) (mm)</td>
<td>1,600.0</td>
</tr>
<tr>
<td>Collision velocity, (v) (m/s)</td>
<td>5.6</td>
</tr>
<tr>
<td>Striker mass, (M) (kg)</td>
<td>500.0</td>
</tr>
<tr>
<td>Kinetic energy, (E_k) (J)</td>
<td>7848.0</td>
</tr>
</tbody>
</table>
plastic deformation caused by the impact load via numerical analysis, a true stress-strain curve that can consider the strain-hardening effect of the material is required. Thus, a true stress-strain curve was calculated using Eqs. (3)–(5). In this instance, 0.0218 was applied from the estimation formula of Park for the strain at the start of the strain hardening. In the numerical analysis, both the actual material properties and the material properties calculated from the estimation formulas were applied. Fig. 3 shows the true stress-strain curve.

\[
\frac{\sigma}{\sigma_Y} = \left\{ 1 - 0.644 \times \frac{E}{1000\sigma_Y} \right\}^{2.4} \tag{1}
\]

\[
\frac{\varepsilon_{FL}}{\varepsilon_Y} = 336 \times \left( \frac{E}{1000\sigma_Y} \right)^{2.52} \tag{2}
\]

\[
\sigma_{tr} = E \varepsilon_{tr}(\varepsilon_{tr} - 1) \quad \text{when} \quad 0 < \varepsilon_{tr} < \varepsilon_{Y,tr} \tag{3}
\]

\[
\frac{\sigma_{tr}}{\varepsilon_{tr}} = \frac{\sigma_{Y,tr}}{\varepsilon_{Y,tr}} \quad \text{when} \quad \varepsilon_{Y,tr} < \varepsilon_{tr} < \varepsilon_{HS,tr} \tag{4}
\]

\[
\sigma_{tr} = \sigma_{HS,tr} + K(\varepsilon_{tr} - \varepsilon_{HS,tr})^n \quad \text{when} \quad \varepsilon_{HS,tr} < \varepsilon_{tr} \tag{5}
\]

where

\[
n = \frac{(\varepsilon_{Y,tr} - \varepsilon_{HS,tr})}{(\sigma_{Y,tr} - \sigma_{HS,tr})} \tag{6}
\]

\[
K = \frac{(\sigma_{Y,tr} - \sigma_{HS,tr})(\varepsilon_{HS,tr} - \varepsilon_{HS,tr})^n}{(\varepsilon_{Y,tr} - \varepsilon_{HS,tr})^n} \tag{7}
\]

In addition, the stress-strain curve based on the strain rate was estimated by applying Eqs. (6)–(9) proposed by Park (2017) to consider the strain-rate effect of the material with instantaneous deformation. In this instance, 0.001, 1, 10, 30, 50, 80, 100, 200, and 1000 s\(^{-1}\) were considered for the strain rate. Fig. 4 shows the stress-strain curve with consideration of the strain rate.

\[
\frac{\sigma_{YD}}{\sigma_Y} = 1 + 0.3 \times \left( \frac{E}{1000 \times \sigma_Y} \right)^{0.5} \times (\dot{\varepsilon})^{0.25} \tag{6}
\]

\[
\frac{\sigma_{FD}}{\sigma_Y} = 1 + 0.16 \times \left( \frac{E}{1000 \times \sigma_Y} \right)^{3.32} \times (\dot{\varepsilon})^{0.03} \tag{7}
\]

\[
\frac{\varepsilon_{RSD}}{\varepsilon_{RS}} = 1 + 0.3 \times \left( \frac{E}{1000 \times \sigma_Y} \right)^{1.73} \times (\dot{\varepsilon})^{0.43} \tag{8}
\]
2.3 Validation Results

Collision analysis was conducted by applying the material properties defined above, and the displacement values at the point with the maximum displacement were compared. Fig. 5 shows the deformation history at the collision point of the numerical-analysis results. The average value of the section from 0.03 to 0.045 s after the spring-back was regarded as the final permanent deformation. Subsequently, the test and numerical-analysis results were compared, as shown in Table 2. The largest error of 22% occurred when the Cowper-Symonds equation was applied, and an error of 19% was observed even when the actual material properties were applied. Meanwhile, applying Park’s dynamic equation resulted in an error of 8% with respect to the test results. In addition, the numerical-analysis results obtained by applying Park’s estimation formula and the numerical-analysis results obtained based on the actual material properties derived from the tensile test were almost identical, that is, the difference

![Stress-strain rate curves of cylinder shell](image)

\[ \frac{\varepsilon_{YD}}{\varepsilon_{YD}} = 1 - 0.117 \times \left( \frac{E}{1000 \times \sigma_T} \right)^{2.512} \times \left( \frac{\sigma_T}{\sigma_y} \right)^{0.588} \times (\varepsilon)^{0.2} \] (9)

![Displacement history at collision point](image)

![Schemes follow the same formatting. If there are multiple panels, they should be listed as](image)

**Table 2** Displacement results of impact test and numerical analysis

<table>
<thead>
<tr>
<th>Item</th>
<th>Impact test result</th>
<th>Numerical analysis</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td></td>
<td>Exp.(^1)-CS(^2)</td>
</tr>
<tr>
<td>Displacement (mm)</td>
<td>36.8</td>
<td>43.8</td>
</tr>
<tr>
<td>Difference (%)</td>
<td>-</td>
<td>19</td>
</tr>
</tbody>
</table>

\(^1\) Material properties form tensile test
\(^2\) Cowper-Symonds equation
\(^3\) Material properties obtained using empirical formula (Park, 2017)
\(^4\) Dynamic equation (Park, 2017)
in maximum deformation was only 0.2 mm. Fig. 6 shows the
dehomed model after the impact test and the numerical- analysis
results. As shown in the figure, the deformed shape was almost
identical. As for the collision analysis for the actual structure,
umerical analysis was conducted in this study by applying Park’s
material-property estimation formula and dynamic stress-strain
curve equations. This is expected to yield more conservative
results as compared with the damage to the actual structure.

3. Collision Analysis

3.1 Collision Scenarios

The appropriate collision scenarios must be selected for the
collision analysis of a floater. Dai et al. (2013) examined the
risk of ship collision that may occur during the life cycle of an
offshore wind turbine. They considered the ships that may
approach the floater during its operation. In this study,
installation waters were not selected, and thus, ships that operate
along the routes under ALS scenarios were excluded. Among the
ships that can be utilized during the life cycle, only working
vessels with relatively high potential for collision damage were
defined as targets. Table 3 summarizes the working vessels
mobilized based on the operational stage and purpose of FOWTs.
Vessels such as crew-transfer vessels (CTVs) were considered in
ULS design. However, they were excluded from collision analysis
owing to their insignificant impact on structures due to small
displacement. As for colliding ships, multipurpose vessels
(MPVs), service-operation vessels (SOVs), and anchor-handling
tug ships (AHTSs), which are ships with different bow shapes,
were considered (see Fig. 7). Additionally, 1,500 and 4,000 tons
of displacement were applied considering the cargo loads. The
hydrodynamic effect by fluid viscosity only considered the added
mass and 0.25 was applied as the added-mass coefficient in
accordance with DNV standards (2021). The DNV standards
recommend collision velocities of 2 m/s or higher for the ALS.
Therefore, collision velocities of 2 and 5 m/s were considered in
this study. Analysis cases that do not involve fracture at a
velocity of 2 m/s were excluded from the analysis cases for a
velocity of 5 m/s. At a velocity of 2 m/s, only the cases with
the highest collision energy were considered for each collision
ship type. In this study, the collision between the bow of the
ship and the outer column of the floater was considered, and
collision angles of 0°, 30°, 60°, and 90° examined. In particular,
angles of 0° and 90° represent collision cases with vertical girder
members in the outer column, whereas angles of 30° and 60°
represent collision cases with longitudinal stiffeners. Thus, 0°
and 60° were selected as the final collision angles. Table 4
summarizes the collision-analysis conditions. The energy of the
striker was calculated using Eq. (10).

\[
E_{\text{kin}} = \frac{1}{2} \cdot (1 + a) \cdot m \cdot v_{\text{impact}}^2
\]  

(10)

Table 3 Utilized vessels during FOWT life cycle

<table>
<thead>
<tr>
<th>Phase</th>
<th>Pre-Installation</th>
<th>Installation</th>
<th>Operation</th>
<th>Decommission</th>
</tr>
</thead>
<tbody>
<tr>
<td>Tasks</td>
<td>Geotechnical and metocean survey</td>
<td>FOWT Grid, Anchor Substation</td>
<td>Maintenance SOV, Grid, Substation</td>
<td>FOWT Anchor Substation</td>
</tr>
<tr>
<td>Vessel type</td>
<td>SOV</td>
<td>SOV, AHTS, MPV</td>
<td>SOV, CTV</td>
<td>SOV, AHTS, MPV</td>
</tr>
</tbody>
</table>
3.2 Material Properties

In the collision analysis, a yield strength of 355 MPa was assumed for high-tensile steels frequently used in the marine structural design. Material properties that consider the true stress-strain curve and the strain-rate effect were estimated using the estimation formula of Park (2019). The same material properties were applied to both the shell and stiffener. Fig. 8 shows the stress-strain curves of the material applied to the numerical-analysis model. In addition, a shear-fracture model was applied to consider fracture deformation. Meanwhile, the shear criterion was set based on the studies of Ringsberg et al. (2018) and Park et al. (2023), who specified 0.23 to 0.27 as the shear criterion based on a parametric analysis for validation. In this study, a value of 0.25 was applied as the criterion for the shear-fracture model.

3.3 Analysis Setup

A 15MW semi-submersible type of UMaine VolturnUS-S was
applied as the floater (Christopher et al., 2020). The internal stiffener of the floater was designed in accordance with DNV standards, and watertight bulkheads were installed at 5 m above the still water level (SWL) and 3 m below the SWL. Vertical displacement was restrained at the bottom of the floater. Except for the collision column, constrains for horizontal displacement were imposed on both sides of the column at the bottom. Collision ships were modeled as rigid bodies. A mesh size of 50 mm × 50 mm, which is 1.6 times the thickness of the column shell, was applied to the collision area, whereas 400 mm × 400 mm was applied to the other areas to reduce the analysis time. Fig. 9 shows the applied boundary conditions and the arrangements of the floater and collision ship. Fig. 10 shows the created finite-element model.
Collision-Damage Analysis of a Floating Offshore Wind Turbine Considering Ship-Collision Risk

Yoon and Choung (2023) demonstrated that collision occurs instantaneously. Thus, the effect of hydrodynamic force on damage is limited. In the collision analysis of this study, hydrodynamic force was considered only as the added mass of kinetic energy, whereas the damage range and the occurrence of fracture were examined based on the collision-area geometry.

4. Collision Results

4.1 Results of Displacement and Deformation

The shell deformation results for the collision column were examined. Fig. 11 shows the maximum deformation distribution in the collision area under a collision angle of 0°. The maximum deformation of the shell was approximately 694 mm, which occurred during the collision of an SOV with a bulbous bow. Fig. 12 shows the x-direction displacement by case for a collision angle of 0°. As shown, the displacement distribution of the collision ship is consistent with the bow shape. Fig. 13 shows the maximum displacement at a collision velocity of 5 m/s. As presented, the displacement increased with the increase in the displacement of collision ship. This appears to be a valid result considering kinetic energy, and displacement more than doubled.
Fig. 13 Displacement results of collision analysis for 5 m/s velocity

Fig. 14 Displacement and deformation results

Table 5 Collision-analysis results

<table>
<thead>
<tr>
<th>Case</th>
<th>Vessel</th>
<th>Kinetic energy (kJ)</th>
<th>Maximum displacement (mm)</th>
<th>Maximum deformation (mm)</th>
<th>Fracture</th>
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</thead>
<tbody>
<tr>
<td>1</td>
<td>MPV</td>
<td>23,438</td>
<td>135.9</td>
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<tr>
<td>2</td>
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<td>698.7</td>
<td>689.0</td>
<td>Not</td>
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<tr>
<td>3</td>
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<td>62,500</td>
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<td>4</td>
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<tr>
<td>6</td>
<td>SOV</td>
<td>23,438</td>
<td>833.0</td>
<td>824.5</td>
<td>Not</td>
</tr>
<tr>
<td>7</td>
<td>SOV</td>
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<td>1,142.0</td>
<td>232.6</td>
<td>Occur</td>
</tr>
<tr>
<td>8</td>
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<td>1,789.0</td>
<td>596.0</td>
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<tr>
<td>9</td>
<td>AHTS</td>
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<td>144.1</td>
<td>129.8</td>
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</tr>
<tr>
<td>10</td>
<td>AHTS</td>
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<td>825.4</td>
<td>824.5</td>
<td>Occur</td>
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<td>11</td>
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<tr>
<td>13</td>
<td>MPV</td>
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<td>388.8</td>
<td>53.1</td>
<td>Not</td>
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<tr>
<td>14</td>
<td>SOV</td>
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<td>484.6</td>
<td>156.0</td>
<td>Occur</td>
</tr>
<tr>
<td>15</td>
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<td>10,000</td>
<td>367.2</td>
<td>55.5</td>
<td>Not</td>
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</tbody>
</table>
at a collision angle of 60°. Fig. 14 shows the displacement of the collision column in the xy-plane for Cases 3 and 4. The results indicate that a large deformation occurred at a collision angle of 60° due to the displacement of the entire column arising from the displacement of the floater pontoon. Table 5 summarizes the damage and fracture results for all collision cases. For the MPV and AHTS, the risk of flooding was low even after collision because the collision area and maximum deformation positions were above the sea level. In the case of SOV, the collision area and maximum deformation position were near the sea level, and damage occurred above and below the watertight bulkhead positions, as shown in Fig. 15. Therefore, when collision occurs, it is necessary to examine the occurrence of fracture in the shell and the flooding of the column.

### 4.2 Occurrence of Fracture

The occurrence of fracture in the shell of the collision column was examined. If fracture occurs in the shell, then flooding occurs in the area below the SWL or in the splash zone, thus jeopardizing the floater’s stability. In accordance with the standards, the fracture occurrence was similarly examined at 2 m/s, which is the lowest collision velocity of the ALS. Fig. 16 shows the equivalent plastic strain results of SOV-collision analysis. For a collision angle of 0°, no fracture occurred even at a high displacement of 4,000 t. In the case of 60°, fracture occurred even at a low displacement of 1,500 t. Fine fracture occurred at a collision velocity of 2 m/s with the smallest kinetic energy. Furthermore, the occurrence of fracture differed depending on the collision angle, because the longitudinal girders...
attached at 90° intervals from 0° prevented the plastic deformation of the shell (Fig. 17). These results confirmed that the shell deformation caused by collision is proportional to the collision energy, whereas the shell can be fractured even by small collision energy, depending on the collision angle and internal stiffener arrangement. Fig. 18 shows the extent of damage. Because fracture occurs between the sea level and the watertight bulkhead at 3 m below the SWL, the flooding section caused by collision is the VOID section near the SWL. The results confirmed that collision with ships featuring a bulbous bow, including the SOV that was applied as a collision ship, may cause the flooding problem. Thus, the damage stability must be further examined to secure the safety of FOWT systems after collision occurs. If the damage stability cannot be secured, it can be made possible by designing the minimum flooding section of the VOID tank between watertight bulkheads or by applying foam-filling technology to prevent flooding of the damaged area (Shin et al., 2019).

5. Conclusions

In this study, collision-damage analysis was performed on a 15 MW FOWT with a semi-submersible floater. First, the numerical-analysis results were compared with the impact-test results to verify the material properties applied in the numerical simulation. The results showed that the error of the numerical calculation was approximately 8% comparing with the experimental value. Collision scenarios for the accidental limit state were investigated for multi-purpose vessels, service operation vessels, and anchor handling tug ships, which can operate during the life cycle of offshore wind turbines. The extent of damage to the floater was examined under different collision conditions, such as the bow shape, displacement, collision velocity, and collision angle.

Analysis results based on the shape and displacement of the colliding ship confirmed that the extent of damage caused by collision...
was proportional to the kinetic energy of the striker. The maximum deformation of the shell (approximately 694 mm) occurred during collision with a ship featuring a bulbous bow (Case 7). The local deformation of the collision area was significant at a collision angle of 0°, whereas the total displacement of the floater column was significant at 60°. The results based on a collision angle of 0° showed that the displacement increased, and the shell deformation increased by approximately three times for barge-type ships with a large collision area. However, the shell deformation increased less (approximately 1.5 times) despite an increase in displacement for ships with a bulbous bow and knife-edge geometry. This suggests that displacement contributes significantly to the extent of damage for damage resulting from collision with a ship featuring a large collision area.

At a collision angle of 60° where only the small stiffeners were involved, fracture with a maximum area of 0.25 m² occurred in the shell. Even at a collision velocity of 2 m/s with the lowest kinetic energy, fracture with an area of 0.01 m² occurred. This shows that the occurrence of fracture is more significantly affected by the internal stiffener than by the maximum shell deformation. This indicates that an appropriate collision angle must be selected by considering the internal stiffener of the floater when establishing collision-analysis scenarios. In addition, collision with a ship featuring a bulbous bow may cause fracture in the collision area below the SWL, resulting in flooding in the floater. When collision analysis is conducted for a ship with a bulbous bow, damage can be evaluated conservatively. In the future, design improvements should be conducted to satisfy damage-stability criteria under flooding conditions in some compartments of the floater columns.

**Conflict of Interest**

The authors declare that they have no conflicts of interest.

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**References**


Author ORCIDs

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1. Introduction

To address climate change resulting from rising global temperatures, there is a growing worldwide interest in achieving net-zero carbon emissions. In response, the European Union (EU) aims to establish a hydrogen economy through its 2020 Hydrogen Strategy, which secures renewable hydrogen-related technologies across various sectors, including industry and transportation. By 2030, the EU plans to develop a 40 GW electrolysis facility and produce 10 million tons of green hydrogen (European Commission, 2020). Renewable hydrogen is categorized into green, blue, and gray hydrogen based on the amount of greenhouse gases emitted during production. Green hydrogen, produced from renewable energy without greenhouse gas emissions, has a higher production cost compared to other hydrogen types, but is increasingly recognized as a clean energy solution with international environmental regulations becoming stricter (Martinez-Luengo et al., 2017; Yu et al., 2021). Offshore wind, characterized by long full load hours and stronger winds compared to land-based systems, is classified as a valuable resource for green hydrogen production.

Research is being conducted on infrastructure design and economic evaluation for hydrogen production (Pham et al., 2021). Offshore wind turbines can generally be categorized into fixed and floating types based on water depth. Offshore fixed wind turbines are typically used in water depths of less than 60 m, while floating structures are cost-effective in areas deeper than 100 m. Floating wind turbines, installed in areas with stronger offshore winds, can support large, heavy turbines without concerns about the seabed utilizing self-buoyancy. Eurek et al. (2017) indicate that 80% of global offshore wind resources are located in deep waters (over 60 m), suggesting the potential for large-scale floating offshore wind turbines to operate alongside green hydrogen production platforms. The National Renewable Energy Laboratory (NREL) in the United States, in collaboration with the University of Maine, has developed the Volturn US-S semi-submersible platform for a 15 MW wind turbine as part of the International Energy Agency (IEA) Wind Energy 37 initiative (Allen et al., 2020). Various studies utilizing this floating wind turbine platform have been conducted. For instance, Pillai et al. (2022) enhanced the mooring footprint to reduce loads by optimizing the...
anchoring of the IEA 15 MW turbine and VolturnUS-S platform through numerical simulations in shallow water. Niranjan and Ramisetti (2022) conducted a fully coupled dynamic analysis for the IEA 15 MW wind turbines and VolturnUS-S platforms based on aero-hydro-servo-elastic codes. Additionally, Balli and Zheng (2022) proposed a pseudo-coupling approach to simplify the fatigue damage evaluation procedures for semi-submersible offshore wind turbines. Further research on various forms of floating wind turbine platforms is also being conducted. Heo et al. (2023) reviewed the safety of the supporting structures of an 8-10 MW Tri-Star floating wind turbine under operating environmental conditions and extreme environmental conditions. Jin et al. (2023) applied the effective inertia coefficient technique to reduce the computational time for the OC4 semi-submersible platform.

Methods for integrating floating offshore wind turbines (FOWTs) with green hydrogen production facilities include transmitting electricity generated from the wind turbines via cables to onshore or offshore hydrogen production facilities, or equipping hydrogen production facilities on the floating wind turbine platform to produce green hydrogen using the generated electricity and then transmitting it via risers and pipes (Ibrahim et al., 2023). The first method requires producing high-voltage current to minimize energy losses and delivering the generated electrical energy to the hydrogen production facilities. In contrast, the second method has the advantage of using proven risers and pipes commonly used in marine engineering to produce and transport green hydrogen with minimal energy loss. To apply the second method, research into the design of green hydrogen risers for floating wind turbines is essential. However, studies on the coupled analysis between these floating wind turbines and risers are nearly nonexistent. Therefore, prior research related to the design of offshore risers for oil production and transportation should be referenced for configuring green hydrogen risers and pipes. Recent relevant studies include Li et al. (2016), who conducted a basic configuration design for a flexible riser in a turret-moored floating production storage offloading (FPSO) vessel in the South China Sea, comparing tension and curvature. Trapper (2020) also used a simplified structural analysis model to design the riser configuration so that the lazy wave flexible riser would have minimal potential energy. Additionally, Elsas et al. (2021) estimated the optimal configuration of the riser using Bayesian optimization methods.

The objective of this study is to determine the optimal configuration of a hydrogen riser for a green hydrogen transportation from a 15-MW floating wind turbine to onshore. Specifically, a validated floating wind turbine model was used to analyze the global motion of the floating platform under extreme loading conditions, and to assess the hydrodynamic performance and safety of the mooring lines and lazy wave riser. The location and length of the buoyancy module of the riser were estimated using a parametric study. In this context, the tension and bending moment of the riser were compared to determine the optimal riser configuration, and these were analyzed using response surface methods.

2. Dynamics of 15-MW FOWT with Green Hydrogen

For hydrodynamic analysis of the floating wind turbine platform, it is necessary to consider the coupling effects among the floating body, mooring system, tower, and turbine. This requires analyzing the hydrodynamics of the floating body, the elastodynamics of the mooring system and tower, and the aerodynamics of the turbine, and an integrated dynamic analysis model must be constructed. Additionally, in the dynamic analysis model, the energy efficiency of floating offshore wind turbines can be maximized through control of the blade pitch or yaw angle of wind turbines. In this study, since the focus is on evaluating the safety of the green hydrogen riser of floating wind turbines under extreme environmental conditions, the control strategy of the wind turbine blades were not considered. Therefore, the equations of motion for the floating body of wind turbines can be expressed as follows:

\[ (M + M_r(\infty))\ddot{\hat{\xi}}(t) + C\dot{\hat{\xi}}(t) + K\hat{\xi}(t) = F_{w1}(t) + F_{w2}(t) + F_{w3}(t) + F_{w4}(t) + F_{wave}(t) \]  

(1)

where \( M, M_r(\infty), C, \) and \( K \) denote the mass matrix of the floating body, added mass matrix at infinite frequency, additional damping matrix considering viscous effects, and the restoring coefficient matrix based on the wetted surface of the floating body, respectively. The additional damping matrix can be defined in the form of a quadratic damping force proportional to the square of the floating body’s velocity, \( F_{w1}, F_{w2}, F_{w3}, \) and \( F_{wave} \) denote the matrices for the wave-frequency wave force, second-order wave force, radiated damping force matrix owing to the floating body’s radiation force, mooring force matrix from the coupling effect due to the mooring lines and risers, and the load matrix owing to the tower’s elasticity, respectively. \( \xi, \dot{\xi}, \) and \( \ddot{\xi} \) denote the displacement, velocity, and acceleration matrices of the floating body’s motion, respectively, with the motion of the floating body having six degrees of freedom, comprising three translational motions (surge, sway, and heave) and three rotational motions (roll, pitch, and yaw). The wave-frequency wave force, second-order wave force, and radiated damping force can be expressed as Eqs. (2), (3), and (4), respectively:

\[ F_{w1}(t) = \Re\left\{ \sum_{j=1}^{\infty} A_j D_\omega e^{-i\omega t} \right\} \]  

(2)

\[ F_{w2}(t) = \Re\left\{ \sum_{j=1}^{\infty} \sum_{k=1}^{\infty} A_j A_k D_{\omega k} e^{-i(\omega_j - \omega_k) t} \right\} \]  

(3)

\[ F_{w3}(t) = -\int_0^t \Re\{\hat{\xi}(\tau)\dot{\tilde{\eta}}(\tau)\} d\tau \]  

(4)

where \( \omega \) and \( A_j \) represent the wave frequency and incident wave amplitude at the \( j \)-th frequency component, respectively, and \( N_\omega \) represents the wave number at frequency component \( \omega \).
denotes the number of frequency components of the incident wave. In this study, irregular wave conditions were described based on 300 incident wave frequency components. $L(w)$ and $D(\omega_i, \omega_j)$ represent the linear transfer function of the wave-frequency wave load and the quadratic transfer function of the low-frequency wave load, respectively. In this study, the Newman approximation was applied to represent the low-frequency wave load. $R(t)$ denotes the impulse response function of the radiation force, which is defined through the Fourier cosine transform of the radiation damping coefficient. The wave-frequency and second-order wave loads and radiation damping coefficient were obtained by solving diffraction and radiation problems.

Submerged line structures (such as mooring lines and risers), as well as offshore wind turbine towers and blades, can all be represented using a lumped-mass model. In this model, relatively slender objects are represented as a series of elements, with two nodes at each end of an element accounting for half of the element mass. These nodes are connected by axial stiffness and damping, bending stiffness and damping, and torsional stiffness and damping (see Fig. 1). The stiffness of each component is influenced by the material properties and the structural dimensions of the materials. Additionally, submerged line structures, along with the tower and turbine blades, are affected by fluid flow drag under environmental loads (Eq. 5), and the submerged line structures are subject to added mass proportional to the object's acceleration due to the surrounding water (Eq. 6). These are expressed as follows:

\[ F_D = \frac{1}{2} \rho D C_D v(t) \]  \hspace{1cm} (5)  
\[ F_I = C_m \Delta \dot{\alpha}_i - C_d \Delta \alpha_i \]  \hspace{1cm} (6)

where $\rho$, $D$, $l$, and $\Delta$ denote the fluid density, object diameter, element length, and element mass, respectively. $C_m$, $C_d$, and $C_D$ represent the matrices representing the inertia coefficient, added mass coefficient, and drag coefficient, respectively, and $v_i$, $\dot{\alpha}_i$, and $\alpha_i$ are the relative velocity between fluid and elements, fluid acceleration, and element acceleration, respectively. By applying these two environmental loads to a lumped-mass model shown in Fig. 1, the behavior and loads of the mooring lines, risers, tower, and turbine blades can be calculated.

### 3. Numerical Analysis Model

#### 3.1 15-MW FOWT Platform

This study utilized the IEA-15-240 RWT 15-MW turbine and the semi-submersible platform VolturnUS-S (Allen et al., 2020). The IEA-15-240 RWT 15-MW turbine is a conventional horizontal-axis turbine with three blades, a rotor diameter of 240 m, a hub height of 150 m, and an operating wind speed range of 3–25 m/s. Developed by the NREL and the University of Maine, the VolturnUS-S comprises four columns, as shown in Fig. 2, with the wind turbine connected to the central column. Detailed specifications are presented in Table 1. The numerical model of the floating platform is represented by a total of 3,508 rectangular elements.

The floating wind turbine uses three mooring lines to maintain its position, as shown in Fig. 3, with each mooring line being connected to an outer column and installed at 120° intervals. Each mooring line comprises an R3 studless chain with a nominal diameter of 185 mm. The total length of the mooring line is 850 m, with a horizontal distance from the floating platform to the anchor of 837.6 m. The minimum breaking load of the mooring line was 22,286 kN. The added mass coefficient and drag coefficient were referenced from Table 1.

<table>
<thead>
<tr>
<th>Table 1 Property specifications of the VolturnUS-S platform (Allen et al., 2020)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Parameter</td>
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<td>Hull steel mass (t)</td>
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<tr>
<td>Tower interface mass (t)</td>
</tr>
<tr>
<td>Ballast (fixed/fluid) (t)</td>
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<tr>
<td>Design draft (m)</td>
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<tr>
<td>Freeboard (m)</td>
</tr>
<tr>
<td>Vertical center of gravity from still water line (m)</td>
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<td>Vertical center of buoyancy from still water line (m)</td>
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<td>Pitch inertia about the center of gravity (kg$\cdot$m$^2$)</td>
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<td>Yaw inertia about the center of gravity (kg$\cdot$m$^2$)</td>
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</table>
Fig. 2 Configuration of 15-MW floating offshore wind turbine

Table 2 Mooring system properties of the VolturnUS-S platform (Allen et al., 2020)

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Value</th>
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<tr>
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<tr>
<td>Fairlead depth from M.W.L. (m)</td>
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<tr>
<td>Anchor radial spacing (m)</td>
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<td>Fairlead radial spacing (m)</td>
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<tr>
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<td>Line unstretched length (m)</td>
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<tr>
<td>Normal added mass coefficient</td>
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<td>Tangential added mass coefficient</td>
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<td>Normal drag coefficient</td>
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<tr>
<td>Tangential drag coefficient</td>
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</tr>
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</table>

DNVGL-RP-C205 (DNV GL, 2021a) and DNVGL-OS-301 (DNV GL, 2021b). Further details are presented in Table 2.

3.2 Green Hydrogen Riser

Risers and pipes for transporting green hydrogen can directly utilize those widely employed in ocean engineering field, which have already been validated for their safety and durability. A scenario was set where energy produced through wind power is converted into green hydrogen and transported via a riser. The numerical model for the green hydrogen riser used the lazy wave riser model provided as an example in OrcaFlex. This model applies a flexible joint at the connection between the floating structure and the riser, with a buoyancy module installed in the middle to represent the lazy wave riser shape (Fig. 4).

A parametric study was conducted by adjusting the length and position of each section of the riser. Table 3 presents the specifications of the flexible production riser from OrcaFlex examples (Orcina, 2023).

Table 3 Specifications of flexible production riser from OrcaFlex examples (Orcina, 2023)

<table>
<thead>
<tr>
<th>Item</th>
<th>Parameter</th>
<th>Value</th>
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<td>Outer diameter (m)</td>
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</tr>
<tr>
<td></td>
<td>Inner diameter (m)</td>
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</tr>
<tr>
<td></td>
<td>Young’s Modulus (kPa)</td>
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<tr>
<td></td>
<td>Poisson ratio</td>
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</tr>
<tr>
<td></td>
<td>Bending stiffness (kN·m²)</td>
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</tr>
<tr>
<td></td>
<td>Axial stiffness (kN)</td>
<td>4.727×10^6</td>
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<tr>
<td></td>
<td>Torsional stiffness (kN·m²)</td>
<td>35.7×10^3</td>
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<td></td>
<td>Added mass coefficient</td>
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<tr>
<td></td>
<td>Drag coefficient</td>
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</tr>
<tr>
<td>Buoyancy part</td>
<td>Outer diameter (m)</td>
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<tr>
<td></td>
<td>Inner diameter (m)</td>
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</tr>
<tr>
<td></td>
<td>Added mass coefficient</td>
<td>0.59827</td>
</tr>
<tr>
<td></td>
<td>Drag coefficient</td>
<td>0.10081</td>
</tr>
</tbody>
</table>

Fig. 3 Overview of the mooring system for 15-MW floating offshore wind turbine (Allen et al. 2020)
of the buoyancy module to determine the optimal riser configuration. Initial riser configuration conditions were referenced from Rentschler et al. (2019), where the total length of the riser ($l_{total}$) and the horizontal distance at both ends of the riser were set to 2.8 times and 2 times the water depth ($h$), respectively. The length ratio between the upper bare section and the buoyancy module section was set to 0.25 of the total riser arc length. In this study, the total arc length of the riser and the connection locations at both ends were fixed, and optimization was performed on the position and length of the buoyancy module. Detailed riser specifications are provided in Table 3, and the lazy wave riser is shown in Fig. 4. The red part in Fig. 4 indicates the buoyancy module. The representative element length of the mooring line and the riser was set to 10 m, with the element length gradually shortened near each boundary condition to minimize numerical errors.

### 3.3 Environmental Conditions

The design environmental conditions were established assuming the installation of a FOWT for green hydrogen production near Uldolmok in Uljin, South Korea. The conditions were based on the International Electrotechnical Commission’s (IEC) report on design requirements for FOWTs (IEC, 2019), applying the operating condition DLC 1.1 with maximum wind speed, and the extreme load condition DLC 6.1. The operating condition was based on a maximum wind speed of 25 m/s at the hub of this 15-MW wind turbine, and the significant wave height and peak period at this wind speed were calculated using wind speed–wave height distribution tables. Additionally, the current speed induced by wind was calculated and applied based on DNVGL-OS-C205 (DNV GL, 2021c) (Eq. (7)), as follows:

$$V_{c,sea} = 0.03 V_{a,1H,10m}$$

(7)

where $V_{c,sea}$ and $V_{a,1H,10m}$ denote the current speed at the sea surface and the hourly average wind speed at 10 m above the water surface, respectively. For the extreme load conditions, the wave, current, and wind conditions with a 50-year recurrence period were referenced from Lee et al. (2023), and both the operating and extreme load conditions are summarized in Table 4. In these conditions, the wind, waves, and currents all act in the same direction. Typically, the wind speed criterion used in the design of floating structures refers to the speed at a height of 10 m above sea level, so the extreme wind profile was used to calculate the wind speed at the hub of the floating wind turbine as in Eq. (8) (DNV GL, 2021c; IEC TS 61400-3-2:2019, 2019).

$$V_{a}(z) = V_{a,hub}(z) = V_{a,hub}(z)$$

(8)

where $V_{a}$ denotes the wind speed at a specific location ($z$), $V_{a,hub}$ denotes the wind speed at the hub location, and $z_{hub}$ denotes the height of the hub above the water surface. The values 0.14 and 0.11 were used for the operating and extreme conditions, respectively (DNV GL, 2021b; European Commission, 2015).

### 4. Numerical Analysis Results

#### 4.1 Validation Test

In this section, the hydrodynamic performance of the floating platform under wave conditions with a white noise spectrum is analyzed and compared with the open published results to validate the numerical modeling of the floating wind turbine. Comparing the results between them, the green hydrogen riser was not considered. Fig. 5 shows the incident wave spectrum and the response amplitude operator (RAO) of the floating platform. When waves are incident in the negative x-axis direction, without the influence of currents or wind, the time-domain motion equations (Eq. (1)) can be used to describe the global motion of the floating platform. In this study, the total analysis time was 3 hours. The time series data of the platform’s global motion were transformed into a response spectrum using Fourier transformation, and then the incident wave spectrum (Fig. 5(a)) was applied for backward estimation of the motion RAOs (Fig. 5(b)). The time step in numerical analysis was set to 0.025 s. The estimated results generally matched those of Allen et al. (2020).

Based on the validated numerical modeling, the global motion of the floating platform was analyzed under two environmental conditions (Table 4). The directions of the waves, currents, and wind were all aligned in the positive direction of the x-axis. Irregular waves were generated using the Joint North Sea Wave Project (JONSWAP) spectrum, with the analysis time set to a storm duration of 3 hours (Fig. 6). Fig. 6(b) shows the incident wave spectrum reconstructed from the

<table>
<thead>
<tr>
<th>Environmental conditions (wave, wind, and current)</th>
<th>Significant wave height, $H_s$ (m)</th>
<th>Wave peak period, $T_p$ (s)</th>
<th>Enhancement factor, $\gamma$</th>
<th>Wind mean speed at 10 m above water level, $V_{a}$ (m/s)</th>
<th>Wind speed at the hub, $V_{a,hub}$ (m/s)</th>
<th>Surface current speed, $V_{current}$ (m/s)</th>
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</thead>
<tbody>
<tr>
<td>DLC 1.1</td>
<td>6.20</td>
<td>12.40</td>
<td>1.0</td>
<td>17.11</td>
<td>25.00</td>
<td>0.46</td>
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<tr>
<td>DLC 6.1</td>
<td>8.34</td>
<td>13.1</td>
<td>1.7</td>
<td>30.68</td>
<td>41.33</td>
<td>1.69</td>
</tr>
</tbody>
</table>
time series of Fig. 6(a), compared with theoretical predictions.

Fig. 7 illustrates the global motion of the floating platform under two environmental conditions. As the platform is symmetrical along the x-axis and incidence angle of the environmental load is in the positive direction of the x-axis, only three degrees of freedom (surge, heave, and pitch) were calculated. The response characteristics were examined by transforming the motion response spectrum through Fourier transformation. Due to the larger significant wave height, wind
speed, and current speed, DLC 6.1 showed the average position of the floating platform moving further in the positive direction of the x-axis compared to DLC 1.1. Surge motion appeared more pronounced at lower frequencies rather than wave frequency motion. Heave motion primarily showed a significant response at the peak wave frequency, while pitch motion was predominantly observed at the natural frequency. Overall, under DLC 6.1 conditions, higher significant wave heights resulted in greater motion responses compared to DLC 1.1.

Along with the three degrees of freedom behavior of the floating platform, the nacelle accelerations (Fig. 8) and mooring line tensions (Fig. 9) under two loading conditions were compared. Nacelle acceleration is directly related to the power production of the floating wind device. According to Boo et al. (2018), the nacelle acceleration under operating conditions should be less than 0.4g (= 3.924 m/s²). In this analysis, under operating conditions (DLC 1.1), a maximum nacelle acceleration of 1.5 m/s² was observed, meeting the design requirements. For extreme conditions (DLC 6.1), a maximum of approximately 7 m/s² was evident. Furthermore, in the nacelle acceleration's Power Spectral Density (PSD) shown in Fig. 8(b), low-frequency components below 0.3 rad/s appear under DLC 6.1 conditions, whereas components around 1.5 rad/s and below 0.3 rad/s were evident under DLC 1.1. The mooring line tensions (Fig. 9) are shown separately for mooring lines 1, 2 and 3, considering the platform's x-axis symmetry. As shown in Fig. 7(a), the average position of the floating platform moved further in the positive direction of the x-axis under DLC 6.1 than under DLC 1.1. Consequently, mooring line 1 experienced a smaller static tension, while mooring lines 2 and 3 had greater static tension. Additionally, due to the relatively large motion responses of the floating platform under DLC 6.1 conditions, significant dynamic tensions also occurred. However, the maximum tension was approximately 4,500 kN, which is much smaller than the maximum allowable load of the mooring line (13,928 kN). The maximum allowable load is calculated based on a maximum allowable load of 22,286 kN considering a safety factor of 1.6 applied, based on the American Petroleum Institute standards (API, 2008). The PSD of the mooring line tension under extreme loading conditions reveals the behavior characteristics of the mooring line, with relatively large tensions evident at the natural frequencies of the surge, heave, and pitch motions. Importantly, the impact of the surge motion mode, characterized by low-frequency movement, was most significant on the mooring line tension. Table 5 indicates the natural frequencies of the mooring lines, showing that their impact was relatively minor.

### 4.2 Effect of Length of Upper Bare Section

In this section, the flexible riser for transporting green hydrogen associated with offshore wind power facilities is considered. Using previously verified numerical modeling of the floating offshore wind power facility, a lazy wave riser for hydrogen transportation was
applied, and a parametric study was conducted to determine the optimal riser profile. To achieve the desired lazy wave riser configuration, a buoyancy module was applied in the middle of the riser. This section estimates the impact of the length of the upper bare section of the riser, which refers to the portion from the floating platform to the start of the buoyancy module, without the module itself. Fig. 10 illustrates the tension and bending moment at the connection point between the riser and the floating platform. Here, the lengths of the buoyancy module and the upper bare sections corresponded to 0.25 of the total riser length, with environmental conditions set to DLC 6.1. The maximum tension and bending moment observed were approximately 148 kN and 230 kN·m, respectively. Analyzing the PSD of the tension and bending moment, it is found that the tension was significantly influenced by both the low-frequency motion and the wave frequency motion of the floating platform, while the bending moment was primarily affected by low-frequency motion. Consequently, to ensure the riser’s safety, minimizing low-frequency motion is crucial.

Figs. 11 and 12 show the maximum tension and the standard deviation of the bending moment across the riser, based on the length of the upper bare section. The maximum tension can be used to assess the safety of the riser concerning its maximum allowable tension, while the standard deviation of the bending moment can estimate short-term fatigue damage. The tension in the riser is greatest near the floating platform and decreases as one moves away from it. The tension increases due to the partially acting buoyant force, and it tends to decrease again once the buoyancy module ends where the buoyancy module begins. The overall increase in the maximum tension of the riser as the distance from the buoyancy module increases suggests that having a shorter upper bare section is advantageous for riser safety. Additionally, this demonstrates that the lazy wave riser is structurally safer compared to a catenary riser without the buoyancy module.

The standard deviation of the bending moment varies significantly depending on the presence of the buoyancy module. Notable changes in the standard deviation were observed at several critical points: the sag bending point before the start of the buoyancy module, the hog bending point within the buoyancy module, the inflection point after the buoyancy module, and the touchdown point of the riser. The maximum standard deviation occurred at the sag point within the upper bare section. When analyzing the effect of the length of the upper bare module, shorter lengths resulted in higher maximum values of the standard deviation of the bending moment. This is attributed to the sag bending point being located higher up, increasing the impact of waves near the water surface. While the buoyancy module can reduce fatigue damage at the touchdown point, an increase in fatigue damage at the upper part of the riser was observed. Additionally, small but somewhat discontinuous bending moments were noted at the connection points between the floating platform and the riser, as well as at the points where the buoyancy module begins and ends.

4.3 Effect of Length of Buoyancy Module Section

In the previous section, the impact of the length of the upper bare
section was considered. This section focuses on the impact of the length of the buoyancy module section. The buoyancy module is installed starting at the quarter point of the riser’s total length, and its length varies as 0.21, 0.23, 0.25, 0.27, and 0.29 times the total length of the riser. Figs. 13 and 14 show the maximum tension and the standard deviation of the bending moment across the riser, respectively, for different lengths of the buoyancy module section. As observed in Fig. 11, when there is no buoyancy module, the tension decreases as one moves away from the floating platform. When a module is present, the tension gradually increases as the distance from the platform increases. As the module length increases, the riser generally experiences more buoyant force, resulting in lower maximum tension. The standard deviation of the bending moment follows a similar trend as in Fig. 12. While the length of the buoyancy module section has little impact on the standard deviation of the bending moment in the upper part of the riser, longer lengths lead to a decrease in the standard deviation of the bending moment at the touchdown point. In terms of fatigue damage, increasing the length of the module is advantageous.

4.4 Response Surface Analysis

Next, based on the findings detailed in Sections 4.2 and 4.3, the effects of the lengths of the buoyancy module section and the upper bare section on the tension and bending moment were examined. A parametric study was conducted using five different lengths for the buoyancy module section (0.21, 0.23, 0.25, 0.27, and 0.29) and five different lengths for the upper bare section, presented through a 2D response surface analysis (Fig. 15). For each length condition, the maximum value of the tension (Fig. 15(a)) and the maximum value of the standard deviation of the bending moment (Fig. 15(b)) across the riser were compared. The study found that when the upper bare section is short and the buoyancy module section is long, the smallest maximum tension was observed, while the standard deviation of the bending moment was largest under the same conditions. Specifically, the maximum tension was lower with a longer buoyancy module section and higher with a longer upper bare section. However, the standard deviation of the bending moment was more significantly affected by the length of the buoyancy module section than by the length of the upper bare section, with shorter buoyancy module
sections showing lower standard deviation values. In conclusion, to ensure safety under extreme conditions and reduce fatigue damage, both the upper bare section and the buoyancy module section should be relatively short.

5. Conclusion

In this study, a parametric analysis was conducted on the lengths of the buoyancy module section and the upper bare section of a riser for a green hydrogen production base associated with a FOWT facility. A full coupled analysis of the FOWT, mooring lines, and riser was performed, and operating and extreme environmental conditions were assessed based on actual marine observation data from the Korean coast.

Initially, the validity of the numerical model was established by applying a white noise incident wave spectrum and calculating the motion response function for comparison. Motion characteristics were analyzed through the coupled analysis of the floating platform and mooring lines. Additionally, nacelle accelerations under operating conditions and mooring line tensions under extreme conditions were analyzed to confirm that this floating platform meets the design criteria. The verified numerical analysis model applied to the riser for transporting green hydrogen examined the effects of the lengths of the buoyancy module section and the upper bare section on the maximum tension and the standard deviation of the bending moment of the riser. The results indicate that as the length of the buoyancy module section increases, the maximum tension decreases, while the maximum tension increases with the length of the upper bare section. Also, shorter buoyancy modules are expected to cause less fatigue damage, with relatively little impact from the length of the upper bare section.

These findings provide useful information for the design and operation aimed at efficient integration between floating offshore wind turbines and green hydrogen production facilities and are expected to contribute to future research and industrial applications. This study is limited to the safety analysis of green hydrogen risers under extreme environmental conditions, performed under parked turbine conditions. For a thorough evaluation of riser fatigue damage, follow-up studies should consider dynamic turbine behavior, turbine control techniques, and turbulent wind flow.

Conflict of Interest

No potential conflict of interest relevant to this article was reported.

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\[
G_{GEV}(x; \mu, \sigma, \xi) = \begin{cases} 
\exp\left[ -\left(1 + \xi(x - \mu)/\sigma\right)^{-1/\xi} \right] & \xi \neq 0 \\
\exp\left[ -\exp\left(- (x - \mu)/\sigma \right) \right] & \xi = 0 
\end{cases}
\]

in which \(\mu\), \(\sigma\), and \(\xi\) represent the location (“Shift” in figures), scale, and shape parameters, respectively.
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<table>
<thead>
<tr>
<th>Item</th>
<th>Buoyancy riser</th>
</tr>
</thead>
<tbody>
<tr>
<td>Segment length(^1) (m)</td>
<td>370</td>
</tr>
<tr>
<td>Outer diameter (m)</td>
<td>1.137</td>
</tr>
<tr>
<td>Inner diameter (m)</td>
<td>0.406</td>
</tr>
<tr>
<td>Dry weight (kg/m)</td>
<td>697</td>
</tr>
<tr>
<td>Bending rigidity (N·m(^2))</td>
<td>1.66E8</td>
</tr>
<tr>
<td>Axial stiffness (N)</td>
<td>7.098E9</td>
</tr>
<tr>
<td>Inner flow density (kg·m(^3))</td>
<td>881</td>
</tr>
<tr>
<td>Seabed stiffness (N/m/m(^2))</td>
<td>6,000</td>
</tr>
</tbody>
</table>

\(^1\) Tables may have a footer.

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![Fig. 1 Schemes follow the same formatting.](image)

Fig. 1 Schemes follow the same formatting. If there are multiple panels, they should be listed as: (a) Description of what is contained in the first panel; (b) Description of what is contained in the second panel. Figures should be placed in the main text near to the first time they are cited.

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<thead>
<tr>
<th>Author name</th>
<th>ORCID</th>
</tr>
</thead>
<tbody>
<tr>
<td>So, Hee</td>
<td>0000-0000-000-00X</td>
</tr>
<tr>
<td>Park, Hye-Il</td>
<td>0000-0000-000-00X</td>
</tr>
<tr>
<td>Yoo, All</td>
<td>0000-0000-000-00X</td>
</tr>
<tr>
<td>Jung, Jewerly</td>
<td>0000-0000-000-00X</td>
</tr>
</tbody>
</table>
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