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“Journal of Ocean Engineering and Technology” is the official journal published by “The Korean Society of Ocean Engineers (KSOE)”. The ISO abbreviation is “J. Ocean Eng. Technol.” and acronym is “JOET”. It was launched in 1987. It is published bimonthly in February, April, June, August, October, and December each year. Supplement numbers are published at times.

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South Korea is highly dependent on energy imports because it has no domestic energy resources such as oil fields or coal mines. Typically, imported energy resources include coal, petroleum, and natural gas, but these fossil fuels generate pollutants such as SOx (sulfur oxides) and NOx (nitrogen oxides), which are harmful to the human body when burned. Nuclear energy can be an alternative. However, it has its safety risks. Because of environmental concerns, the International Maritime Organization has implemented a SOx regulation to reduce sulfur content to 0.5%, effective from January 1, 2020, as shown in Fig. 1.

Fig. 1 2020 sulfur oxide emission control graph

1. Introduction

Since liquefied natural gas (LNG) is imported in a liquid state of about -162℃ to increase transportation efficiency in Korea, it must be vaporized in a gaseous state to supply it to consumers. Among them, ambient air vaporizer (AAV) has caught attention due to eco-friendly and low costs characteristics. However, there is a disadvantage that the performance of the heat exchanger is deteriorated due to frost due to mist and icing when used for a long time. In this paper, frost generation model in AAV vaporizer was investigated with numerically to examine utilizing the vaporizer performance with the frost generation behavior. The frost generation behavior of AAV vaporizers was examined with humidity, fin characteristic, and temperature effects. As for the LNG discharge temperature, the 12 fin vaporizer showed the highest discharge temperature when the atmospheric temperature was 25℃, and the 8 fin vaporizer had the lowest LNG discharge temperature when the atmospheric temperature was 0℃. In the case of frost formation, in the case of the 12 fin vaporizer, it was formed the most at the atmospheric temperature of 25℃, and the least was formed in the vaporizer at the 0℃ condition of the atmospheric temperature of 8 fins.
Emission regulation is a sensitive issue that has a significant impact on the health of residents in coastal areas. The willingness of major countries to participate in the regulations is so strong that emission control areas have been gradually expanding, as shown in Fig. 2. The use of natural gas has been steadily increasing because of its eco-friendliness, stability, and economic efficiency compared to current car fuels. Furthermore, sectors using natural gas are diversifying because it has ample reserves.

Natural gas is supplied to importers in a liquid state of approximately -162 °C to improve transportation efficiency, and it is vaporized when used as an energy source (Kim et al., 1994). Vaporizers are essential for this process, and typical types of vaporizers include open rack vaporizer, submerged combustion vaporizer, and ambient air vaporizer (AAV). AAV refers to a liquefied natural gas (LNG) vaporizer that exchanges heat with surrounding air as a heating medium. The AAV is environmentally friendly, has low operating costs, and its LNG vaporization can be performed during winter. However, over the long-term operation, the performance of its heat exchangers declines because of frost (Kim et al., 2008).

AAVs, which are drawing attention due to global environmental issues, have fog and frost issues in long-term operation, as shown in Fig. 3, and these issues must be resolved urgently. The large generation of fog and frost acts as thermal resistance for the vaporizer, reducing the thermal efficiency by about 80–85% (Park and Oh, 2006). Many studies have been conducted to reduce the frost generation caused by the condensation of water vapor in the AAV, but there is limited research on solutions that can prevent frost growth (Hermes, 2009; Park et al., 2018; Park and Song, 2019; Kim et al., 2018). This study examined a numerical analysis method for the frost generation process during the operation of the AAV, which is essential for cryogenic LNG. We used the LNG properties to understand the frost generation behavior numerically using atmospheric humidity and temperature conditions, which are essential variables in AAV operation. The frost generation results were compared according to the number of fins in the vaporizer.

### 2. Numerical Analysis of Frost Generation

#### 2.1 Numerical Modeling Assumptions

1. We assumed that the fin tube's properties are constant regardless of temperature changes.
2. Since methane, ethane, and propane account for 99% of the LNG, it is treated as a mixture of these components.
3. Compulsory conditions are given for the air fan flow around the vaporizer.
4. For the airflow region outside the tube, turbulent flow conditions are given to the region of $Re \geq 10^5$.
5. Heat transfer by the radiation effect is ignored.

#### 2.2 Heat Transfer, Phase Change, and Mass Exchange Model

For the frost generation around the vaporizer, we used the mixture model of Eqs. (1) and (2) with the properties of air and frost. To simulate the mass exchange between the air and frost, we used a multi-phase mass exchange model using Eqs. (7) and (8). For the simulation of multi-phase mass exchange, we created subroutines by applying user-defined functions. Based on these functions, we used mass (Eq. (1)) and momentum conservation equations (Eq. (3)) as source terms. We determined the enthalpy using the energy generated with the source term, forming Eqs. (4)–(6) as the energy equations.

$$\frac{\partial \rho A}{\partial t} + \frac{\partial \rho A u}{\partial l} = 0$$

(1)

$$\rho = \frac{1}{x / \rho_{\text{air}} + (1-x) / \rho_{\text{frost}}}$$

(2)

$$\frac{\partial \rho A u}{\partial t} + \frac{\partial \rho A u}{\partial l} = - \frac{\partial \rho A}{\partial l} + \text{friccoeff} \cdot A$$

(3)

$$\frac{\partial \rho H A}{\partial t} + \frac{\partial \rho H A}{\partial l} = q$$

(4)

$$H = \frac{1}{x / H_{\text{air}} + (1-x) / H_{\text{frost}}}$$

(5)

$$S_{\text{energy}} = H \cdot S_{\text{mass}}$$

(6)

$$S_{\text{f}} = \frac{r_x \rho (T_r - T_f)}{T_r} \quad T_r \geq T_c$$

(7)

$$S_{\text{t}} = \frac{r_x \rho (T_d - T_t)}{T_t} \quad T_c \geq T_t$$

(8)

To apply the LNG properties, we created a mixture with the three main components of CH₄, C₂H₆, and C₃H₈ using the LNG components of Table 1 (QuerolE et al., 2010; Fernández et al., 2017; Rao et al.,...
Table 1 LNG Properties

<table>
<thead>
<tr>
<th>Component</th>
<th>Constituent of LNG</th>
<th>Typical-LNG</th>
</tr>
</thead>
<tbody>
<tr>
<td>N₂</td>
<td>0.04</td>
<td></td>
</tr>
<tr>
<td>CH₄</td>
<td>89.26</td>
<td></td>
</tr>
<tr>
<td>C₂H₆</td>
<td>8.64</td>
<td></td>
</tr>
<tr>
<td>C₃H₈</td>
<td>1.44</td>
<td></td>
</tr>
<tr>
<td>iC₄H₁₀</td>
<td>0.27</td>
<td></td>
</tr>
<tr>
<td>nC₅H₁₀</td>
<td>0.35</td>
<td></td>
</tr>
<tr>
<td>iC₅H₁₂</td>
<td>0</td>
<td></td>
</tr>
<tr>
<td>nC₅H₁₂</td>
<td>0</td>
<td></td>
</tr>
<tr>
<td>Total</td>
<td>100</td>
<td></td>
</tr>
</tbody>
</table>

Subroutines were created for the LNG material properties by applying user-defined functions, thereby modularizing every component.

### 2.3 Calculation Conditions

For the calculation of frost generation, we created a non-adaptive grid (hexahedral grid) with 200,000 nodes. To examine the convergence of the calculation, we checked whether the conditions of less than or equal to $10^{-4}$ and $10^{-6}$ were appropriate for the mass and energy residual balance, respectively. They were maintained to proceed with the numerical analysis.

### 3. Results

#### 3.1 Numerical Analysis and Validation

The frost generation and heat transfer model were validated through a comparison with experimental results for validation (Afrasiabian et al., 2018). The validation experiment had the following conditions: a duct below the plate having a size of 99.5 mm × 80.2 mm × 3.4 mm, with low-temperature fluid (liquid nitrogen and low-temperature fluid) flowing inside the duct. Air flowed at a constant flow rate above the plate, and the bottom of the plate had a wall temperature of -4 to -16°C. The results of the frost generation were summarized based on these conditions. To validate the numerical analysis, it was conducted under the experiment's conditions (the humidity was set to 85%, and the wind speed was set to 7 m/s), and its results were compared with the experimental results. Fig. 4 shows the frost generation results based on the wall temperature for the flow of low-temperature fluid. We found that the lower the wall temperature, the greater the frost generation, and Fig. 5 compares the obtained values. In Fig. 5, the error rate is large in the low-temperature region. The error is somewhat large in the low-temperature region because actual frost generation, condensation, and densification occurred, and the numerical analysis showed the frost generation through heat transfer based on the temperature change. A comparison of the numerical analysis and experimental results showed that the error rate between the results was approximately 5 to 10%.

#### 3.2 Numerical Analysis Conditions of AAV

For the numerical analysis of the AAV, we modeled the vaporizer at the Incheon Production Base of Korea Gas Corporation and set the operating time to 180 min (Lee et al., 2018a; Lee et al., 2018b). Furthermore, based on the Korea Meteorological Administration’s data for the Incheon Production Base, we set the external temperature conditions to 0 and 25°C.

Fig. 4 Frost generation value according to temperature: (a) -4°C wall temperature; (b) -8°C wall temperature; (c) -12°C wall temperature; (d) -16°C wall temperature

Fig. 5 Comparison between the experimental data and validation data

Fig. 6 Geometry model of the fin tube: Specification of vaporizer
Table 2 Summary of fin characteristics

<table>
<thead>
<tr>
<th>Variable</th>
<th>Items</th>
<th>Length</th>
</tr>
</thead>
<tbody>
<tr>
<td>L (mm)</td>
<td>Tube length</td>
<td>12,000</td>
</tr>
<tr>
<td>N</td>
<td>Number of fins</td>
<td>8, 12</td>
</tr>
<tr>
<td>H (mm)</td>
<td>Fin height</td>
<td>86</td>
</tr>
<tr>
<td>δ (mm)</td>
<td>Fin thickness</td>
<td>5</td>
</tr>
<tr>
<td>D_i (mm)</td>
<td>Inner diameter</td>
<td>10.5</td>
</tr>
<tr>
<td>D_o (mm)</td>
<td>Outer diameter</td>
<td>15.7</td>
</tr>
<tr>
<td>Total L (mm)</td>
<td>Tube total length</td>
<td>72,000 (12,000 mm × 6)</td>
</tr>
</tbody>
</table>

Similarly, humidity conditions of 60% were given based on the Korea Meteorological Administration’s data. Fig. 6 and Table 2 show the conditions and geometry of the fin applied to the numerical analysis. The number of fins used in the vaporizer was set to 8 and 12 to compare the frost generation process, which has heat transfer. For the AAV’s capacity, we set the LNG flow rate to 10 t/h based on the reference capacity of the Incheon Production Base and assigned a condition of 10 m/s for the fan based on the fan capacity of approximately 4,200 N·m³/min. A temperature condition of -163°C was given for the injected LNG temperature, and the LNG injection pressure in the flow was set to 7.55 MPa.

3.3 Numerical Analysis Results of AAV

Figs. 7–14 show the frost generation process for 8 and 12 fins. They show contours to summarize the temperature and frost thickness results obtained through the numerical analysis when the atmospheric temperature is 25 and 0°C, and the fan speed was 10 m/s. We summarized the temperature and frost thickness results at 1, 12, 24, 36, 48, and 70 m for the 72 m AAV.

Figs. 7–10 show contours to summarize the temperature and frost thickness results obtained from the numerical analysis for 300 s when the 8-fin fan speed is 10 m/s with an atmospheric temperature of 25 or 0°C. We summarized the temperature and frost thickness contours by setting the length of AAV to 1, 12, 24, 36, 48, and 70 m. When the fan speed was 10 m/s in the 8-fin, the LNG discharge temperature was higher when the atmospheric temperature was 25°C than when it was 0°C. Furthermore, more frost was formed when the atmospheric temperature was 25°C.

Fig. 7 Temperature contour of 8 fin with outer temperature of 25°C at 300 s

Fig. 8 Frost thickness contour of 8 fin with outer temperature of 25°C at 300 s

Fig. 9 Temperature contour of 8 fin with outer temperature of 0°C at 300 s

Fig. 10 Frost thickness contour of 8 fin with outer temperature of 0°C at 300 s

Fig. 11 Temperature contour of 12 fin with outer temperature of 25°C at 300 s
Figs. 11–14 show contours to summarize the temperature and frost thickness results obtained through the numerical analysis for 300 s when the 12-fin fan speed was 10 m/s with an atmospheric temperature of 25 or 0 ℃. We summarized the temperature and frost thickness contours by setting the length of the AAV to 1, 12, 24, 36, 48, and 70 m. When the 12-fin fan speed was 10 m/s, the LNG discharge temperature was higher when the atmospheric temperature was 25 ℃ compared to when it was 0 ℃. Furthermore, more frost was formed when the atmospheric temperature was 25 ℃.

Fig. 15 shows the LNG discharge temperature results based on a fan speed of 10 m/s for the 8-fin and 12-fin vaporizers. For the 8-fin vaporizer, the discharge temperature was about 8 ℃ higher when the atmospheric temperature was 25 ℃ compared to when it was 0 ℃. For the 12-fin vaporizer, the discharge temperature was about 15 ℃ higher when the atmospheric temperature was 25 ℃ compared to when it was 0 ℃. The 12-fin vaporizer showed the highest LNG discharge temperature at an atmospheric temperature of 25 ℃. On the other hand, the 8-fin vaporizer showed the lowest LNG discharge temperature at an atmospheric temperature of 0 ℃. According to the frost thickness results, both the 8-fin and 12-fin vaporizers generate more frost when the atmospheric temperature is 25 ℃, because when the atmospheric temperature rises, so does the convective heat transfer coefficient, resulting in a higher LNG discharge temperature, increasing frost formation.

Fig. 16 shows the thickness results of the generated frost according to the AAV length after one hour for the 8-fin and 12-fin vaporizers. The frost generation was concentrated on the 0–1 m part of the inlet, and frost generation decreased as the distance from the inlet increased. Based on the frost thickness derived according to the AAV length, the 12-fin vaporizer generated approximately 92 and 95% more frost on the inlet part compared to the 8-fin vaporizer when the atmospheric temperature was 25 and 0 ℃, respectively.

These results indicate that the vaporizer inlet requires a device that can remove frost. Furthermore, the operating conditions must be reflected according to the atmospheric temperature, and a method that
can configure as many fins as possible under conditions that guarantee vaporizer robustness is required.

4. Conclusion

This study examined a numerical analysis method for the frost generation process in the operation of an AAV, which is essential for cryogenic LNG. Using LNG properties, we numerically investigated frost generation under atmospheric humidity and temperature conditions, which are essential variables in AAV operation.

For the LNG discharge temperature, the 12-fin vaporizer showed the highest discharge temperature at an atmospheric temperature condition of 25°C, while the 8-fin vaporizer showed the lowest LNG discharge temperature at an atmospheric temperature condition of 0°C. Based on the generated frost thickness obtained according to the AAV length, after one hour, for the 8-fin and 12-fin vaporizers, the frost generation was concentrated on the 0–1 m part of the inlet, and as the distance from the inlet increased, the frost generation decreased. We also found that the 12-fin vaporizer generated approximately 92 and 95% more frost on the inlet than the 8-fin vaporizer when the atmospheric temperature was 25 and 0°C, respectively. In the future, we will conduct research on heat transfer and frost generation for each LNG component based on the results of this study.

Conflict of Interest

Sung-Woong Chio serves as an editor of the Journal of Ocean Engineering and Technology but has no role in the decision to publish this article. No potential conflict of interest relevant to this article was reported.

Funding

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Author ORCIDs

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<tr>
<td>Chio, Sung-Woong</td>
<td>0000-0001-7285-4257</td>
</tr>
</tbody>
</table>
Assessment of Cryogenic Material Properties of R-PUF Used in the CCS of an LNG Carrier

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1Professor, Department of Naval Architecture and Ocean Engineering, Mokpo National University, Jeonnam, Korea

KEY WORDS: LNG cargo containment system (LNG CCS), Insulation system, Cryogenic test, Reinforced polyurethane foam (R-PUF), LNG carrier, Secondary barrier

ABSTRACT: Reinforced polyurethane foam (R-PUF), a material for liquefied natural gas cargo containment systems, is expected to have different mechanical properties depending on its stacking position of foaming as the glass fiber reinforcement of R-PUF sinks inside R-PUF under the influence of gravity. In addition, since R-PUF is not a homogeneous material, it is also expected that the coordinate direction within this material has a great correlation with the mechanical properties. So, this study was conducted to confirm this correlation with the one between the mechanical properties and the stacking position. In particular, in this study, R-PUF of 3 different densities (130, 170, and 210 kg/m³) was used, and tensile, compression, and shear tests of this material were performed under 5 temperatures. As a result of the tests, it was confirmed that the strength and modulus of elasticity of the material increased as the temperature decreased. Specifically, the strength and modulus of elasticity in the Z direction, which was the lamination direction, tended to be lower than those in the other directions. Finally, the strength and elastic modulus of different specimens of the material found at the bottom of their lamination compared to the specimens with these properties found at positions other than their lamination bottom were evaluated. Further analysis confirmed that as the temperature decreased, hardening of R-PUF occurred, indicating that the strength and modulus of elasticity increased. On the other hand, as the density of R-PUF increased, a sharp increase in strength and elastic modulus of R-PUF was observed.

Table 1: A MARK III type LNG cargo insulation material specification

<table>
<thead>
<tr>
<th>Primary barrier</th>
<th>Corrugated SUS 304L 1.2 mm</th>
</tr>
</thead>
<tbody>
<tr>
<td>Primary insulation</td>
<td>Plywood covered reinforced PUF (100 mm)</td>
</tr>
<tr>
<td>Secondary barrier</td>
<td>Glued triplex membrane</td>
</tr>
<tr>
<td>Secondary insulation</td>
<td>Plywood covered reinforced PUF (170 mm)</td>
</tr>
</tbody>
</table>

1. Introduction

Recently, as part of the response from the international community to global warming, regulations on greenhouse emissions and the marine environment are being strengthened day by day through climate agreements and IMO (International Maritime Organization). As a measure of responding to these environmental regulations, the amount of liquefied natural gas (LNG) used, which can reduce the emission of SOx by 97%, NOx by 80%, and CO2 by 25%, is rapidly increasing worldwide. Notably, the LNG projects of the world usually consist of LNG carriers, LNG-fueled vessels, and LNG offshore structures. In addition, due to the nature of transporting or storing LNG, LNG cargo holds and fuel tanks must be at a cryogenic temperature of -163°C. So, as shown in Table 1, the interior of the LNG cargo hold consists of the primary barrier; and stainless steel (SUS type), polyurethane foam (PUF), plywood, and adhesives constituting the secondary barrier.

In addition, when the natural frequency of the cargo hold of an LNG carrier is the same as that of the ship, the sloshing impact load of LPG amplifies, which is dangerous. Therefore, to ensure the structural safety of the cargo hold, impact, compression, and tensile tests on reinforced polyurethane foam (R-PUF) should be performed under cryogenic conditions where R-PUF specimens manufactured according to the recommendations of GTT (Gaztransport & Technigaz) are placed in an environment similar to the one inside the cargo hold (Han et al., 2010).

In addition, the effect of repeated impact on the mechanical performance of glass fiber-reinforced PUF was analyzed as repeated compressive loads were generated in this PUF due to the effect of the sloshing impact load and their understanding was important (Kim et
al., 2019a). Consequently, it was confirmed that the glass fiber-reinforced PUF exhibited a sharp decrease in its impact resistance and compression according to the repeated impact conditions when impact energy above a critical level was continuously applied.

Next, an isotropic Frank-Brockman type elastic-isoplastic model was introduced to simulate the compression behavior of an LNG cargo hold, considering the effects of cryogenic temperatures and strain rate (Lee et al., 2015). This simulation was performed in Abaqus UMAT, one of the finite element analysis codes. In particular, the elastic deformation according to the temperature and strain rate of R-PUF subjected to a compressive load was simulated. Finally, the simulation results were compared with the results of a series of compression tests on the material to verify the model.

LNG cargo holds are subjected to various temperatures, from room to cryogenic temperature. So, an experimental basis for evaluating the mechanical performance of R-PUF at cryogenic temperatures was developed by closely analyzing the compression properties of R-PUF (Park et al., 2014). In particular, the compression characteristics at temperatures of 296, 223, 163, and 110 K were analyzed and evaluated so that the phenomenological temperature-dependent insulation properties of R-PUF could be identified. Finally, based on the analysis, the experimental basis necessary for the evaluation of the applicability of R-PUF as a cargo hold insulation material of an LNG carrier was formed.

The cryogenic heat transfer characteristics and thermoelastic behavior of R-PUF were examined by Jang et al. (2013) by performing a numerical analysis of this material using a general-purpose finite element analysis code. In particular, a numerical analysis based on forced convection theory was performed to understand the cryogenic heat transfer characteristics. Additionally, a comparative review of the cryogenic heat transfer and thermoelastic deformation characteristics according to the density change of R-PUF was evaluated by numerical and experimental methods, respectively, was performed.

Kim et al. (2019b) improved the performance of existing single-reinforced R-PUF by adding glass bubble and silica aerogel into the R-PUF and analyzed the thermal insulation performance and mechanical properties of the resulting R-PUF. Specifically, the physical properties of the resulting R-PUF according to the weight ratio of addition in this R-PUF were analyzed. In addition, the effect of ultrasonic dispersion of the added materials on the inner cell structure of R-PUF was investigated. Finally, mechanical strength, dynamic impact, and quasi-static compression tests on the R-PUF added with glass bubble and silica aerogel were performed at cryogenic and room temperatures.

Further, improvement in the mechanical and thermal performances of R-PUF used in existing LNG cargo hold insulation systems was achieved by Ahn et al. (2018) with the addition of glass bubbles into R-PUF in various weight ratios and the mechanical and thermal properties of the resulting R-PUF were evaluated under various temperatures. Consequently, a performance change of R-PUF was observed according to the amount of glass bubble added. In particular, the mechanical strength of the R-PUF added with glass bubbles found by conducting compression tests on this material under various temperatures (-163, -100, -40, and 20°C) resulted in this observation. Finally, for comparing the thermal insulation performances of R-PUFs with different glass bubble additions, the thermal conductivity of glass bubble-added R-PUFs was measured.

In a different approach, instead of glass fiber, Kevlar aramid fiber with excellent mechanical strength and thermal performance was added to PUF, and the effect of this addition on the cell structure of PUF was analyzed by Oh et al. (2018). And as a material test, a compression test at different temperatures was performed on the Kevlar aramid fiber-added PUF. Notably, aramid fibers are stronger than steel and have high heat resistance, high elasticity, and excellent flame retardancy. Meanwhile, changes in the properties of the cell structure were observed depending on the amount of fiber added.

The extensive material behavior and failure characteristics of PUF and R-PUF at low and cryogenic temperatures and under static compressive loads were studied experimentally by Park et al. (2016). In addition, the micro-structural cellular variation of three types of PUF materials according to temperature changes was identified, and a micromechanical approach was introduced to describe the failure characteristics of PUF.

Next, the behavior of R-PUF under repeated compression and creep loads was analyzed under room to cryogenic temperatures by Denay et al. (2013). The thermal and mechanical damages of R-PUF were also investigated as part of the analysis. Finally, creep and cyclic tests aiming at finding how far viscoelasticity and/or damage contribute(s) to the deformation of R-PUF were conducted.

However, although various studies have been conducted on R-PUF, there is no study on the correlation of mechanical properties according to the stacking location and coordinate direction of R-PUF. So, research on this correlation is necessary.

Therefore, in this study, R-PUF with three different densities (130, 170, and 210 kg/m³) was used, and specific stacking positions (bottom, middle, and upper) and coordinate directions (X, Y, and Z in Fig. 1) were chosen to study the R-PUF at room temperature. In particular, tensile, compression, and shear tests were performed on the R-PUF. Further, in a low-temperature environment (-20, -70, -120, and -170 °C), specimens of R-PUF with middle stacking position and three different coordinate directions (X, Y, and Z) were considered, and the mechanical properties of the samples were studied by performing tensile, compression, and shear tests. Notably, a cryogenic chamber was manufactured, and liquid nitrogen (LN2) was used as its refrigerant for this study. Also, considering the thermal conductivity of R-PUF, each test was performed after maintaining the R-PUF at the target temperature for 3 h or more.

2. Test Specimen

2.1 R-PUF

R-PUF is a composite material made by laminating high-density
Assessment of Cryogenic Material Properties of R-PUF Used in the CCS of an LNG Carrier

2.1 Tensile Test Specimen

The specimen for the tensile test of R-PUF was prepared according to the ASTM D1623-03 standard. The dimensions of the specimen are given in Fig. 2.

2.2 Compression Test Specimen

The specimen for the compression test of R-PUF was prepared according to the ISO 844 standard. The dimensions of the specimen are given in Fig. 3.

2.3 Shear Test Specimen

The specimen for the shear test of R-PUF was prepared according to the ISO 1922 standard. The dimensions of the specimen are given in Fig. 4.

Table 2 gives the density from the inspection.

<table>
<thead>
<tr>
<th>Specified density (kg/m³)</th>
<th>Stacking position</th>
<th>Measured density in X direction¹ (kg/m³)</th>
<th>Measured density in Y direction¹ (kg/m³)</th>
<th>Measured density in Z direction¹ (kg/m³)</th>
</tr>
</thead>
<tbody>
<tr>
<td>130</td>
<td>Upper</td>
<td>123.89 (0.45)</td>
<td>127.40 (1.10)</td>
<td>128.83 (1.20)</td>
</tr>
<tr>
<td></td>
<td>Middle</td>
<td>119.89 (0.54)</td>
<td>121.00 (0.38)</td>
<td>122.09 (3.08)</td>
</tr>
<tr>
<td></td>
<td>Bottom</td>
<td>128.36 (0.35)</td>
<td>128.67 (0.36)</td>
<td>130.71 (0.69)</td>
</tr>
<tr>
<td>170</td>
<td>Middle</td>
<td>180.91 (1.13)</td>
<td>180.74 (1.65)</td>
<td>175.85 (1.31)</td>
</tr>
<tr>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>210</td>
<td>Middle</td>
<td>220.85 (1.11)</td>
<td>219.58 (1.90)</td>
<td>216.62 (1.74)</td>
</tr>
</tbody>
</table>

¹ Values within the brackets denote the standard deviation of the corresponding density.

Table 2: Specified and average measured densities of R-PUF specimens

Before the tests, the density of all specimens was thoroughly inspected, and it was found that the density had a variation of 10% or less with respect to the specified density of the specimen material.
3. Experiments

3.1 Test Facility

The cryogenic tests of the study were performed using a cryogenic chamber and 50 and 250 kN MTS universal testing machines. In addition, PT-100 temperature sensors were used inside the chamber to measure the temperature immediately next to the specimen and in the upper and lower parts of the chamber to ensure a stable temperature inside the chamber. R-PUF is a special material that minimizes heat transfer as an insulator, so pre-cooling is required for more than 3 hours. Fig. 5 shows the universal testing machines used in this study.

3.2 Cooling Time and Methodology

For the cryogenic test, LN2 was injected into the cryogenic chamber to maintain a cryogenic state inside the chamber. Temperature sensors were then attached to the upper/lower parts of the chamber to check the temperature in these positions in real time. Additionally, for a uniform temperature distribution of the specimen, a temperature sensor was attached to the specimen to check the target temperature (for maintaining this temperature for 3 h before conducting the test). So, the temperature history curves at various points are given in Fig. 6. Since the specimen and the plate jig used for holding the specimen during the test usually contract while reaching the target temperature, zero-offset of the linear variable differential transformer of the test setup (used for linear distance measurements) should be corrected according to the contraction before starting the test. The related schematic specimen shrinkage control is shown in Fig. 7.

3.3 Tensile Test Methodology

The tensile test of R-PUF was conducted at cryogenic temperature, and the ultimate tensile strength and tensile Young’s modulus of R-PUF were found from the force-deformation curve obtained from the test. The tensile test setup is shown in Fig. 8.
In addition, the tensile Young’s modulus of R-PUF (E) was expressed as,

\[ E = \frac{\sigma(\varepsilon)}{\varepsilon} = \frac{F/A}{\Delta L/L_0} = \frac{F_{\text{max}}}{A\Delta L} \]  

where, \( E, F, A, \Delta L, \) and \( L_0 \) were the force applied on the specimen, area of cross-section of the specimen perpendicular to the direction of the applied force, an increase in the distance between the marks on the specimen corresponding to a specified force increment, and the original distance between these marks, respectively.

### 3.4 Compression Test Methodology

The compression test on R-PUF was performed at room/cryogenic temperature, and the compressive ultimate strength and compressive Young’s modulus of R-PUF were found from the stress-strain curve obtained from the test. For the test, first, an R-PUF sample was placed between the plates of the compression tester (one of the variants in the universal testing machine), and the sample center was aligned with the plates. Subsequently, the moving plate of the tester was moved at a constant speed, compressing the specimen. Generally, if possible, the test speed should be 10% of the measured specimen thickness before compression/min. So, under this condition on the test speed, the sample was compressed, and the ultimate compressive strength and ultimate compressive strength at 10% relative strain of the sample were measured. The experimental setup for the compression test is shown in Fig. 9.

Mathematically, the elongation (\( \varepsilon \)) of a specimen during the tensile test was expressed as a percentage of the original sample length as,

\[ \varepsilon = \frac{\Delta L}{L_0} \times 100 \]  

where, \( L_0 \) and \( \Delta L \) were the original distance between the marks on the specimen and the increase in the distance between these marks corresponding to a specified force increment, respectively.

Further, the ultimate stress (\( \sigma_{\text{max}} \)) inside the specimen was expressed as,

\[ \sigma_{\text{max}} = \frac{F_{\text{max}}}{b \times h} \]  

where, \( F_{\text{max}}, \) \( b, \) and \( h \) were the maximum force applied to the specimen during the test, the original width of the narrow part of the specimen, and the original thickness of the narrow part of the specimen, respectively.

The ultimate compressive strength (\( \sigma_m \)) inside the sample was obtained by dividing the maximum compressive load on the sample (\( F_m \)) by the initial cross-sectional area of the sample (\( A \)) when the elongation of the sample was 10% or more as,

\[ \sigma_m = \frac{F_m}{A} \]
3.5 Shear Test Methodology
The R-PUF shear test was performed on the XY, YZ, and ZX planes and certain specified directions. In particular, the test was performed on an R-PUF specimen with a density of 130 kg/m³ at room temperature for each stacking position (upper, middle, and bottom). In addition, the test temperatures were -20, -70, -120, and -170°C, and a displacement control methodology was used for the test. The experimental setup for the shear test of R-PUF is shown in Fig. 10.

![Experimental setup for the shear test of R-PUF](image)

Meanwhile, the shear stress ($\tau$) within the sample was expressed as,

$$\tau = \frac{1.000 \times F}{b \times l_{oc}}$$  (5)

where, $G$, $l$, and $b$ were the maximum force applied onto the specimen during the test (N), the original length of the specimen, and the original width of the specimen, respectively.

Likewise, the shear modulus of R-PUF ($G$) was expressed as,

$$G = \frac{1.000 \times \delta \times \theta}{b \times l}$$  (6)

where, $\delta$ and $\theta$ were the specimen thickness and the slope of the linear portion of the force-strain curve.

3.6 Test Condition
As mentioned before, it was expected that differences in strength and Young's modulus would occur for each stacking position of R-PUF, so the tests were performed under each stacking position at room temperature. However, when the stacking position was not specifically stated, it meant the default middle stacking position. Further, reliable results of each test were obtained by conducting the test under the same conditions 5 times to obtain valid/reliable test data.

Importantly, in the shear test of R-PUF, when the R-PUF density was 170 kg/m³ or more, a fracture occurred at the bonding surface of the R-PUF specimen and the plate jig, so the test was performed with a density of only 130 kg/m³. Finally, Table 3 shows the number and types of tests conducted.

<table>
<thead>
<tr>
<th>Material (Specified density)</th>
<th>Temperature (°C)</th>
<th>Tensile test</th>
<th>Compression test</th>
<th>Shear test</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>X</td>
<td>Y</td>
<td>Z</td>
<td>X</td>
</tr>
<tr>
<td>R-PUF (130 kg/m³)</td>
<td>20</td>
<td>5</td>
<td>5</td>
<td>5</td>
</tr>
<tr>
<td></td>
<td>-20</td>
<td>5</td>
<td>5</td>
<td>5</td>
</tr>
<tr>
<td></td>
<td>-70</td>
<td>5</td>
<td>5</td>
<td>5</td>
</tr>
<tr>
<td></td>
<td>-120</td>
<td>5</td>
<td>5</td>
<td>5</td>
</tr>
<tr>
<td></td>
<td>-170</td>
<td>5</td>
<td>5</td>
<td>5</td>
</tr>
<tr>
<td></td>
<td>20</td>
<td>5</td>
<td>5</td>
<td>5</td>
</tr>
<tr>
<td></td>
<td>-20</td>
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<td>5</td>
<td>5</td>
</tr>
<tr>
<td></td>
<td>-70</td>
<td>5</td>
<td>5</td>
<td>5</td>
</tr>
<tr>
<td></td>
<td>-120</td>
<td>5</td>
<td>5</td>
<td>5</td>
</tr>
<tr>
<td></td>
<td>-170</td>
<td>5</td>
<td>5</td>
<td>5</td>
</tr>
<tr>
<td>R-PUF (170 kg/m³)</td>
<td>20</td>
<td>5</td>
<td>5</td>
<td>5</td>
</tr>
<tr>
<td></td>
<td>-20</td>
<td>5</td>
<td>5</td>
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</tr>
<tr>
<td></td>
<td>-70</td>
<td>5</td>
<td>5</td>
<td>5</td>
</tr>
<tr>
<td></td>
<td>-120</td>
<td>5</td>
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<td>5</td>
</tr>
<tr>
<td></td>
<td>-170</td>
<td>5</td>
<td>5</td>
<td>5</td>
</tr>
<tr>
<td>R-PUF (210 kg/m³)</td>
<td>20</td>
<td>5</td>
<td>5</td>
<td>5</td>
</tr>
<tr>
<td></td>
<td>-20</td>
<td>5</td>
<td>5</td>
<td>5</td>
</tr>
<tr>
<td></td>
<td>-70</td>
<td>5</td>
<td>5</td>
<td>5</td>
</tr>
<tr>
<td></td>
<td>-120</td>
<td>5</td>
<td>5</td>
<td>5</td>
</tr>
<tr>
<td></td>
<td>-170</td>
<td>5</td>
<td>5</td>
<td>5</td>
</tr>
</tbody>
</table>

Table 3 The number and types of tests conducted in this study (X, Y, Z, XY, YZ, ZX, U, M, and B in the table denote the coordinate directions, coordinate planes, and stacking positions, respectively, in/on/at which the measurements of this test were made)
4. Test Results

4.1 Tensile Test

Since the ultimate tensile strength and tensile modulus are expected to be different depending on the PUF stacking direction, the test was conducted in a total of 9 cases: upper, middle, and bottom with a density of 130 kg/m³ in each direction. In addition, since ultimate tensile strength and tensile modulus are expected to be different for each temperature, each density (130, 170, and 210 kg/m³) is tested at 20, -20, -70, -120, and -170°C. A total of 15 cases were performed.

As shown in Fig. 11 and Fig. 12, differences in ultimate tensile strength and tensile modulus occurred depending on the foaming location and foaming direction.

In the case of the X-direction specimen, the ultimate tensile strength and tensile modulus of the bottom tended to be high. Ultimate tensile strength increased by about 21% compared to the upper and 14.7% compared to the middle. The young’s modulus increased by about 15.8% compared to the upper, and increased by about 15.6% compared to the middle.

In the case of the Y-direction specimen, as in the X-direction, the ultimate tensile strength and tensile modulus of the bottom tended to be high. Ultimate tensile strength increased by about 26.2% compared to the upper and 18.1% compared to the middle. The tensile modulus increased by about 20.7% compared to the upper, and increased by about 18.6% compared to the middle.

In the case of the Z-direction specimen, the ultimate tensile strength and tensile modulus of the middle part were high, unlike the specimens in the X and Y directions. Ultimate tensile strength increased by about 37.9% compared to the upper and by 38.6% compared to the bottom. The tensile modulus increased by 152.9% compared to the upper and 133.6% compared to the bottom.

Also, the difference in ultimate tensile strength and tensile modulus occurred according to the direction of foaming.

In the case of the upper part, the ultimate tensile strength and tensile modulus in the Z-direction tended to be low. The ultimate tensile strength decreased by 54.7% compared to the X direction and...
decreased by 52.7% compared to the Y direction. The tensile modulus decreased by 43.8% compared to the X direction and 41.3% compared to the Y direction.

In the case of the middle part, the ultimate tensile strength in the Z direction showed a tendency to decrease, and the tensile modulus showed a tendency to increase. The ultimate tensile strength decreased by 40.8% compared to the X direction and decreased by 38.9% compared to the Y direction. The tensile modulus increased by 42% compared to the X direction and 45.9% compared to the Y direction.

In the case of the bottom part, the ultimate tensile strength and tensile modulus in the Z direction tended to be low. The ultimate tensile strength decreased by 62.7% compared to the X direction and 62.7% compared to the Y direction. The tensile modulus decreased by 47.4% compared to the X direction and 47.4% compared to the Y direction.

### Table 4 R-PUF tensile test results

<table>
<thead>
<tr>
<th>Material (Specified density)</th>
<th>Temperature (°C)</th>
<th>Ultimate tensile strength (MPa)</th>
<th>Tensile Young’s modulus (MPa)</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>In X direction</td>
<td>In Y direction</td>
<td>In Z direction</td>
</tr>
<tr>
<td></td>
<td>In X direction</td>
<td>In Y direction</td>
<td>In Z direction</td>
</tr>
<tr>
<td>R-PUF (130 kg/m³)</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>20</td>
<td>3.141 (0.067)</td>
<td>3.048 (0.151)</td>
<td>1.861 (0.384)</td>
</tr>
<tr>
<td>-20</td>
<td>3.508 (0.252)</td>
<td>3.477 (0.358)</td>
<td>3.213 (0.496)</td>
</tr>
<tr>
<td>-70</td>
<td>3.643 (0.229)</td>
<td>3.752 (0.168)</td>
<td>2.543 (0.300)</td>
</tr>
<tr>
<td>-120</td>
<td>3.653 (0.484)</td>
<td>3.760 (0.449)</td>
<td>2.394 (0.147)</td>
</tr>
<tr>
<td>-170</td>
<td>3.299 (0.213)</td>
<td>3.123 (0.404)</td>
<td>1.970 (0.315)</td>
</tr>
<tr>
<td>R-PUF (170 kg/m³)</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>20</td>
<td>5.469 (0.472)</td>
<td>5.384 (0.350)</td>
<td>1.888 (0.051)</td>
</tr>
<tr>
<td>-20</td>
<td>5.670 (0.277)</td>
<td>5.595 (0.479)</td>
<td>2.657 (0.059)</td>
</tr>
<tr>
<td>-70</td>
<td>5.928 (0.623)</td>
<td>6.007 (0.689)</td>
<td>2.900 (0.117)</td>
</tr>
<tr>
<td>-120</td>
<td>5.938 (0.429)</td>
<td>5.897 (0.478)</td>
<td>2.667 (0.157)</td>
</tr>
<tr>
<td>-170</td>
<td>5.663 (0.560)</td>
<td>5.286 (0.952)</td>
<td>2.284 (0.106)</td>
</tr>
<tr>
<td>R-PUF (210 kg/m³)</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>20</td>
<td>7.363 (0.460)</td>
<td>6.451 (0.692)</td>
<td>2.485 (0.147)</td>
</tr>
<tr>
<td>-20</td>
<td>7.552 (0.726)</td>
<td>7.076 (0.593)</td>
<td>3.281 (0.138)</td>
</tr>
<tr>
<td>-70</td>
<td>8.094 (0.832)</td>
<td>7.585 (1.103)</td>
<td>3.644 (0.117)</td>
</tr>
<tr>
<td>-120</td>
<td>8.370 (0.893)</td>
<td>7.492 (0.432)</td>
<td>3.409 (0.177)</td>
</tr>
<tr>
<td>-170</td>
<td>7.806 (0.530)</td>
<td>7.639 (0.480)</td>
<td>3.236 (0.256)</td>
</tr>
</tbody>
</table>

1) Values within the brackets denote the standard deviation of the corresponding ultimate tensile strength/tensile Young’s modulus.

---

**Fig. 13** Density 130 kg/m³ tensile test comparison X, Y and Z

**Fig. 14** Density 170 kg/m³ tensile test comparison X, Y and Z

**Fig. 15** Density 210 kg/m³ tensile test comparison X, Y and Z
As shown in Table 4 and Figs. 13–15, it can be seen that the mechanical properties change according to the temperature. As the temperature decreases, the ultimate tensile strength and tensile modulus increase, but show a tendency to decrease again when a certain temperature critical point is exceeded. The standard deviation of all data can be found in Table 4.

4.2 Compression Test

Since the ultimate stress and compressive modulus are expected to be different depending on the PUF stacking direction, the test was conducted in a total of 9 cases: upper, middle, and bottom with a density of 130 kg/m$^3$ in each direction. In addition, since ultimate stress and compressive modulus are expected to be different for each temperature, each density (130, 170, and 210 kg/m$^3$) is tested at 20, -20, -70, -120, and -170°C. A total of 15 cases were performed.

As shown in Fig. 16 and Fig. 17, differences in compressive ultimate stress and compressive modulus occurred depending on the foaming location and foaming direction.

In the case of the X-direction specimen, the compressive ultimate stress and compressive modulus of the bottom tended to be high. Compressive ultimate stress increased by about 17.1% compared to the upper and 17.8% compared to the middle. The compressive modulus increased by about 32.4% compared to the upper, and increased by about 31.9% compared to the middle.

In the case of the Y-direction specimen, as in the X-direction, the compressive ultimate stress and compressive modulus of the bottom tended to be high. Compressive ultimate stress increased by about 5.5% compared to the upper and 13.7% compared to the middle. The compressive modulus increased by about 3.3% compared to the upper, and increased by about 13.6% compared to the middle.

In the case of the Z-direction specimen, unlike the specimens in the X and Y directions, there was no significant difference in compressive ultimate stress, and the compressive modulus increased slightly. The compressive ultimate stress decreased by about 0.1% compared to the upper and increased by about 10.1% compared to the middle. The compressive modulus increased by 6.1% compared to the upper and

![Fig. 16](image1)

**Fig. 16** Ultimate compressive strength of R-PUF by stacking position and coordinate direction for an R-PUF density of 130 kg/m$^3$ at room temperature

![Fig. 17](image2)

**Fig. 17** Compressive Young’s modulus of R-PUF by stacking position and coordinate direction for an R-PUF density of 130 kg/m$^3$ at room temperature
8.3% compared to the middle.

Also, the difference in compressive ultimate stress and compressive modulus occurred according to the direction of foaming. In the case of the upper part specimen, the compressive ultimate stress of the Z-direction specimen showed a tendency to be low. The compressive modulus of the specimen in the X direction showed a tendency to be low. The compressive ultimate stress decreased by 14.3% compared to the X direction and decreased by 19.3% compared to the Y direction. The compressive modulus decreased by 45.8% compared to the Y direction and 37.7% compared to the Z direction.

In the case of the middle part specimen, the compressive ultimate stress of the Z-direction specimen showed a tendency to be low as in the upper part. As for the compressive modulus, the specimen in the X direction showed a low tendency. The compressive ultimate stress decreased by 21.8% compared to the X direction and decreased by 21.1% compared to the Y direction. The compressive modulus decreased by 40.2% compared to the Y direction and 36.2% compared to the Z direction.

In the case of the bottom part specimen, the compressive ultimate stress of the Z-direction specimen showed a tendency to be low. As for the compressive modulus, the specimen in the X direction showed a low tendency. The compressive ultimate stress decreased by 26.9% compared to the X direction and decreased by 23.6% compared to the Y direction. The compressive modulus decreased by 30.5% compared to the Y direction and 22.2% compared to the Z direction.

As shown in Table 5 and Figs. 18–20, it can be seen that the mechanical properties change according to the temperature. As the temperature decreased, the compressive ultimate stress and compressive modulus increased, and unlike the tensile test results, there was no tendency to decrease as the temperature exceeded a

<table>
<thead>
<tr>
<th>Material (Specified density)</th>
<th>Temperature (°C)</th>
<th>Ultimate compressive strength (MPa)</th>
<th>Compressive Young’s modulus (MPa)</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>In X direction</td>
<td>In Y direction</td>
<td>In Z direction</td>
</tr>
<tr>
<td>R-PUF (130 kg/m³)</td>
<td>20</td>
<td>1.702 (0.022)</td>
<td>1.687 (0.015)</td>
</tr>
<tr>
<td></td>
<td>-20</td>
<td>2.096 (0.071)</td>
<td>2.324 (0.170)</td>
</tr>
<tr>
<td></td>
<td>-70</td>
<td>2.983 (0.034)</td>
<td>2.845 (0.196)</td>
</tr>
<tr>
<td></td>
<td>-120</td>
<td>2.924 (0.193)</td>
<td>3.165 (0.115)</td>
</tr>
<tr>
<td></td>
<td>-170</td>
<td>3.177 (0.064)</td>
<td>3.155 (0.230)</td>
</tr>
<tr>
<td>R-PUF (170 kg/m³)</td>
<td>20</td>
<td>3.532 (0.092)</td>
<td>3.419 (0.015)</td>
</tr>
<tr>
<td></td>
<td>-20</td>
<td>4.697 (0.068)</td>
<td>4.439 (0.125)</td>
</tr>
<tr>
<td></td>
<td>-70</td>
<td>6.162 (0.104)</td>
<td>6.095 (0.173)</td>
</tr>
<tr>
<td></td>
<td>-120</td>
<td>7.112 (0.074)</td>
<td>7.457 (0.022)</td>
</tr>
<tr>
<td></td>
<td>-170</td>
<td>8.487 (0.084)</td>
<td>7.587 (0.148)</td>
</tr>
<tr>
<td>R-PUF (210 kg/m³)</td>
<td>20</td>
<td>5.503 (0.026)</td>
<td>5.590 (0.080)</td>
</tr>
<tr>
<td></td>
<td>-20</td>
<td>6.926 (0.057)</td>
<td>7.065 (0.083)</td>
</tr>
<tr>
<td></td>
<td>-70</td>
<td>8.945 (0.115)</td>
<td>6.110 (0.165)</td>
</tr>
<tr>
<td></td>
<td>-120</td>
<td>11.196 (0.147)</td>
<td>11.325 (0.098)</td>
</tr>
<tr>
<td></td>
<td>-170</td>
<td>12.374 (0.137)</td>
<td>12.711 (0.180)</td>
</tr>
</tbody>
</table>

1) Values inside the brackets denote the standard deviation of the corresponding ultimate compressive strength/compressive Young’s modulus.

Fig. 18 Density 130 kg/m³ comp. test comparison X, Y and Z

Fig. 19 Density 170 kg/m³ comp. test comparison X, Y and Z
4.3 Shear Test

The shear test was performed only at a density of 130 kg/m³ because the adhesive broke at densities of 170 and 210 kg/m³, and the shear test was based on the XY, YZ, and ZX directions. In addition, the test for each foaming position was performed at room temperature. Test temperature conditions were carried out at room temperature, -20, -70, -120, and -170°C. The control mode used displacement control.

As shown in Fig. 21 and Fig. 22, the shear ultimate strength and shear modulus were different depending on the foaming location and foaming direction.

In the case of the XY direction specimen, the shear ultimate strength and shear modulus of the bottom tended to be high. The shear ultimate strength increased by about 8.5% compared to the upper and 11% compared to the middle. The shear modulus increased by about 4.9% compared to the upper and 18.5% compared to the middle.

In the case of the specimen in the YZ direction, the shear ultimate strength and shear modulus of the bottom tended to be high as in the XY direction. The shear ultimate strength increased by about 9.3%
compared to the upper and 4.5% compared to the middle. The shear modulus increased by about 4.2% compared to the upper and 8.7% compared to the middle.

In the case of the ZX-direction specimen, the shear ultimate strength and shear modulus of the bottom tended to be high. The shear ultimate strength increased by about 3.8% compared to the upper and 24.1% higher than that of the middle. The shear modulus increased by 0.7% compared to the upper and 14.1% compared to the middle.

Also, there were differences in shear ultimate strength and shear modulus depending on the foaming direction.

In the case of the upper part, the shear ultimate strength and shear modulus in the ZX direction tended to be low. The shear ultimate strength decreased by 27.9% compared to the XY direction and decreased by 28% compared to the YZ direction. The shear modulus decreased by 27.9% compared to the XY direction and decreased by 28% compared to the YZ direction.

In the case of the middle part, the shear ultimate strength and shear modulus in the ZX direction tended to be low. The shear ultimate strength decreased by 38.4% compared to the XY direction and 42.4% compared to the YZ direction. The shear modulus was decreased by 36.5% compared to the XY direction and 44.7% compared to the YZ direction.

In the case of the bottom part, the shear ultimate strength and shear modulus in the ZX direction tended to be low. The shear ultimate strength decreased by 31.1% compared to the XY direction and 31.6% compared to the YZ direction. The shear modulus was decreased by 38.8% compared to the XY direction and 41.2% compared to the YZ direction.

As shown in Table 6 and Fig. 23, the shear ultimate strength and shear modulus tend to increase as the temperature decreases, and then decrease again after a certain temperature. However, in the case of the ZX-direction specimen, it shows a peculiar tendency to increase rapidly at -20°C and then decrease rapidly again. The standard deviation of all data can be found in Table 6.

5. Conclusions

In this study, compression, tensile, and shear tests were performed on R-PUF, a cryogenic insulation material. The compression test was performed at a test speed of 5 mm/min by dividing by angular density using a 100 × 100 × 50 hexahedral specimen. In the case of room temperature conditions, the tests were performed by dividing the foaming positions (upper, middle, and bottom) by direction. In addition, both shear and tensile tests were compared and evaluated under the same conditions as the aforementioned compression tests.

Through each test of the R-PUF material, the static properties of the material were confirmed.

Table 6 R-PUF shear test results

<table>
<thead>
<tr>
<th>Material (Specified density)</th>
<th>Temperature (°C)</th>
<th>Ultimate shear strength (MPa)</th>
<th>Shear modulus (MPa)</th>
</tr>
</thead>
<tbody>
<tr>
<td>R-PUF (130 kg/m³)</td>
<td>20</td>
<td>1.405 (0.041)</td>
<td>1.503 (0.011)</td>
</tr>
<tr>
<td></td>
<td>-20</td>
<td>1.324 (0.059)</td>
<td>1.438 (0.099)</td>
</tr>
<tr>
<td></td>
<td>-70</td>
<td>1.247 (0.045)</td>
<td>1.399 (0.032)</td>
</tr>
<tr>
<td></td>
<td>-120</td>
<td>1.084 (0.256)</td>
<td>1.494 (0.026)</td>
</tr>
<tr>
<td></td>
<td>-170</td>
<td>1.348 (0.073)</td>
<td>1.148 (0.005)</td>
</tr>
</tbody>
</table>

1) Values within the brackets denote the standard deviation of the corresponding ultimate shear strength/shear modulus.
middle position, but the ultimate tensile strength value was 10.80% lower. This can indirectly confirm that there are factors other than density that affect ultimate tensile strength.

As can be seen from Figs. 13‒15, the tensile test result of R-PUF had a large change according to the temperature. Further, the ultimate tensile strength for all densities of R-PUF tended to increase up to -70°C but decreased from -120°C. However, the tensile Young’s modulus continued to increase with decreasing temperature. So, curing occurred in R-PUF under a varying temperature.

In the compression test of R-PUF, the ultimate compressive strength of R-PUF in the Z direction was 10.50 and 10.57% lower than that in the X and Y directions, respectively. This lower ultimate compressive strength was also seen as a result of the orthotropic properties of R-PUF. Here, the compressive modulus tends to be different from the ultimate compressive strength, and the compressive modulus of the X direction specimen tends to be 38.70% lower than that of the Y direction specimen, and 31.81% lower than the Z direction specimen.

As shown in Tables 8 and 11, as mentioned earlier, the ultimate compressive strength was expected to increase in proportion to the density, but in the compression test, similar to the tensile test, the density of the specimen in the upper position was 4.72% higher than that of the specimen in the middle position, but it was confirmed that the ultimate compressive strength value was 5.84% lower.

### Table 7 Tensile test result X, Y and Z comparison

<table>
<thead>
<tr>
<th>Result</th>
<th>X direction</th>
<th>Y direction</th>
<th>Z direction</th>
</tr>
</thead>
<tbody>
<tr>
<td>Ultimate tensile strength (MPa)</td>
<td>Upper 2.978</td>
<td>Upper 2.853</td>
<td>Upper 1.350</td>
</tr>
<tr>
<td></td>
<td>Middle 3.141</td>
<td>Middle 3.048</td>
<td>Middle 1.861</td>
</tr>
<tr>
<td></td>
<td>Bottom 3.603</td>
<td>Bottom 3.600</td>
<td>Bottom 1.343</td>
</tr>
<tr>
<td>Tensile modulus (MPa)</td>
<td>Upper 36.008</td>
<td>Upper 34.483</td>
<td>Upper 20.240</td>
</tr>
<tr>
<td></td>
<td>Middle 36.045</td>
<td>Middle 35.092</td>
<td>Middle 51.183</td>
</tr>
<tr>
<td></td>
<td>Bottom 41.682</td>
<td>Bottom 41.629</td>
<td>Bottom 21.914</td>
</tr>
<tr>
<td></td>
<td>Average 37.912</td>
<td>Average 37.068</td>
<td>Average 31.112</td>
</tr>
</tbody>
</table>

### Table 8 Density value Upper, Middle and Bottom comparison

<table>
<thead>
<tr>
<th>Density (kg/m³)</th>
<th>Upper position</th>
<th>Middle position</th>
<th>Bottom position</th>
</tr>
</thead>
<tbody>
<tr>
<td>X direction</td>
<td>123.89</td>
<td>119.89</td>
<td>128.36</td>
</tr>
<tr>
<td>Y direction</td>
<td>127.40</td>
<td>121.00</td>
<td>128.67</td>
</tr>
<tr>
<td>Z direction</td>
<td>128.83</td>
<td>122.09</td>
<td>130.71</td>
</tr>
<tr>
<td>Average</td>
<td>126.71</td>
<td>120.99</td>
<td>129.25</td>
</tr>
</tbody>
</table>

### Table 9 Tensile test result upper, middle and bottom comparison

<table>
<thead>
<tr>
<th>Result</th>
<th>X direction</th>
<th>Y direction</th>
<th>Z direction</th>
</tr>
</thead>
<tbody>
<tr>
<td>Ultimate tensile strength (MPa)</td>
<td>Upper 2.978</td>
<td>Upper 3.141</td>
<td>Upper 3.603</td>
</tr>
<tr>
<td></td>
<td>Middle 2.853</td>
<td>Middle 3.048</td>
<td>Middle 3.600</td>
</tr>
<tr>
<td></td>
<td>Bottom 1.350</td>
<td>Bottom 1.861</td>
<td>Bottom 1.918</td>
</tr>
<tr>
<td>Average</td>
<td>2.394</td>
<td>2.683</td>
<td>2.849</td>
</tr>
</tbody>
</table>

### Table 10 Comp test result X, Y and Z comparison

<table>
<thead>
<tr>
<th>Result</th>
<th>X direction</th>
<th>Y direction</th>
<th>Z direction</th>
</tr>
</thead>
<tbody>
<tr>
<td>Ultimate compressive strength (MPa)</td>
<td>Upper 1.712</td>
<td>Upper 1.818</td>
<td>Upper 1.467</td>
</tr>
<tr>
<td></td>
<td>Middle 1.702</td>
<td>Middle 1.687</td>
<td>Middle 1.918</td>
</tr>
<tr>
<td></td>
<td>Bottom 2.005</td>
<td>Bottom 1.918</td>
<td>Bottom 1.465</td>
</tr>
<tr>
<td>Average</td>
<td>1.806</td>
<td>1.808</td>
<td>1.617</td>
</tr>
<tr>
<td>Comp. modulus (MPa)</td>
<td>Upper 30.336</td>
<td>Upper 55.974</td>
<td>Upper 48.69</td>
</tr>
<tr>
<td></td>
<td>Middle 30.449</td>
<td>Middle 50.897</td>
<td>Middle 47.69</td>
</tr>
<tr>
<td></td>
<td>Bottom 40.172</td>
<td>Bottom 57.816</td>
<td>Bottom 51.664</td>
</tr>
<tr>
<td>Average</td>
<td>33.652</td>
<td>54.900</td>
<td>49.348</td>
</tr>
</tbody>
</table>
Additionally, the R-PUF compression test results showed a large change in the ultimate compressive strength and compressive Young’s modulus of R-PUF according to the temperature change. As shown in Figs. 18–20, it was confirmed that the ultimate compressive strength and compressive modulus increased as the temperature decreased. In the case of a density of 130 kg/m³, the ultimate compressive strength and compressive modulus increased by 98.52% and 78.12%, respectively, at -170°C compared to 20°C. In the case of density of 170 kg/m³, ultimate compressive strength and compressive modulus increased by 140.42% and 122.79% respectively at -170°C compared to 20°C. At a density of 210 kg/m³, the ultimate compressive strength and compressive modulus increased by 136.52% and 191.51%, respectively, at -170°C compared to 20°C. This correlation appeared to be a result of the hardening phenomenon of R-PUF.

Meanwhile, in the R-PUF shear test, the ultimate shear strength and shear modulus of R-PUF on the ZX plane were the lowest from the viewpoint of the foaming direction. It was 32.36% and 37.23% lower than those on the XY and YZ planes by 32.36 and 37.23% and 34.02 and 40.86 %, respectively. These lower values on the ZX plane indicated the weakness of R-PUF that came from R-PUF manufacturing characteristics.

As shown in Tables 8 and 13, The results of the shear test confirmed that the density had the greatest effect on the results of the shear test, as expected. The density of the highest bottom compared to the middle with the lowest density was 6.83%, ultimate shear strength was 11.45% higher, and shear modulus was 12.89% higher. As the density increased, ultimate shear strength and shear modulus were increased.

As shown in Fig. 23, it can be seen that the shear test results according to the temperature do not have a large effect on the ultimate shear strength of R-PUF was largely affected by temperature, but the shear modulus was not. So, these effects appeared to be a phenomenon caused by the curing of R-PUF.

Overall, the density was the factor having the greatest influence on the ultimate strength and Young’s modulus of R-PUF, but there were likely other factors of such a great influence as well. Further, it was confirmed that the Z direction and the ZX plane were the weakest direction and plane of R-PUF, respectively. Finally, the higher the density of R-PUF, the greater the hardening phenomenon of R-PUF according to the temperature drop.

<table>
<thead>
<tr>
<th>Table 11 Comp. test result Upper, Middle and Bottom comparison</th>
</tr>
</thead>
<tbody>
<tr>
<td>Ultimate compressive strength (MPa)</td>
</tr>
<tr>
<td>Upper position</td>
</tr>
<tr>
<td>X direction</td>
</tr>
<tr>
<td>Y direction</td>
</tr>
<tr>
<td>Z direction</td>
</tr>
<tr>
<td>Average</td>
</tr>
</tbody>
</table>

<table>
<thead>
<tr>
<th>Table 12 Shear test result XY, YZ and ZX comparison</th>
</tr>
</thead>
<tbody>
<tr>
<td>Result</td>
</tr>
<tr>
<td>Ultimate shear strength (MPa)</td>
</tr>
<tr>
<td>Upper</td>
</tr>
<tr>
<td>Middle</td>
</tr>
<tr>
<td>Bottom</td>
</tr>
<tr>
<td>Average</td>
</tr>
</tbody>
</table>

<table>
<thead>
<tr>
<th>Table 13 Shear test result upper, middle and bottom comparison</th>
</tr>
</thead>
<tbody>
<tr>
<td>Result</td>
</tr>
<tr>
<td>Ultimate shear strength (MPa)</td>
</tr>
<tr>
<td>XY Plane</td>
</tr>
<tr>
<td>YZ Plane</td>
</tr>
<tr>
<td>ZX Plane</td>
</tr>
<tr>
<td>Average</td>
</tr>
</tbody>
</table>

<table>
<thead>
<tr>
<th>Shear modulus (MPa)</th>
</tr>
</thead>
<tbody>
<tr>
<td>XY Plane</td>
</tr>
<tr>
<td>YZ Plane</td>
</tr>
<tr>
<td>ZX Plane</td>
</tr>
<tr>
<td>Average</td>
</tr>
</tbody>
</table>
Conflict of Interest

The author declare that they have no conflict of interests.

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References


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A Study on Plate Bending Analysis Using Boundary Element Method

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KEY WORDS: Boundary element method, Plate theory, Level ice, Semi analytical method, Boundary integral equation

ABSTRACT: This study presents a method for level ice-structure interaction analysis to estimate the fatigue damage of arctic structures by applying plate theory to the behavior of level ice. The boundary element method (BEM), which incurs a lower computational cost than the finite element method (FEM), was introduced to solve the plate bending problem. The BEM formulation was performed by applying the BEM to plate theory. Finally, to check the validity of the proposed method, the BEM results and FEM results obtained using the ABAQUUS commercial software were compared. The response results of the BEM analysis agreed well with those of the FEM analysis. Based on the results of the analysis, the BEM approach is considered to be very powerful in level ice-structure interaction analysis for estimating level ice-induced fatigue damage. Further work is being conducted to perform level ice fracture analysis based on the stress field calculated using the boundary element method.

1. Introduction

The use of the Arctic route and the demand for ships operating in the polar region have greatly increased over time owing to global warming. Moreover, the structural stability of vessels operating in the polar regions has attracted increasing attention, and various studies to estimate the ice load have been conducted. Increases in computer performance have allowed studies to analyze the behaviors of ice using computer simulations. The methods used in these studies can be broadly classified as direct numerical methods or semi-analytical methods.

Among direct numerical methods, the finite element method (FEM) is mainly used for numerical analysis. The FEM has been used for many years to solve realistic engineering problems with linear and nonlinear behaviors involving arbitrary shapes and loads in 2D and 3D spaces. Such direct numerical methods have the advantage of obtaining sufficiently realistic results for fracture behaviors if material properties and elements are modeled according to physical phenomena, because they use the FEM to model ice. However, direct numerical methods have the disadvantage of high computational cost when the size of the elements becomes small enough to obtain a realistic result. Furthermore, this approach must be accompanied by non-linear analysis because of the non-linear properties of the material until failure; this also increases the analysis time. Therefore, direct numerical methods are basically used for detailed analysis of a short time domain.

Jeon and Kim (2021) modeled the interaction between level ice and structures by performing a finite element collision simulation using a damage-based erosion model.

In contrast, semi-analytical methods aim to find a analytical solution by introducing specific situations and various assumptions. These methods have the advantage of very short analysis times because they obtain analytical solutions, unlike numerical analysis methods, which require long computational times. Therefore, this type of method is mainly used for long-term analysis. Consequently, in certain situation, the equation provides reliable results. However, there is a disadvantage in that it is difficult to obtain an accurate solution in this way, considering the complex shapes of ice and nonlinear characteristics of the material. One typical example of this type of method is the Simulator for Arctic Marine Structures (SAMS) developed by NTNU (Norwegian University of Science and Technology) in Norway (Lubbad and Løset, 2011; Lubbad et al., 2018; Raza et al., 2019).

This study proposes a numerical method for level ice-structure interaction analysis as a basic tool for long-term fatigue analysis of ships operating in polar waters. A method with a short analysis time, such as a semi-analytical method, is suitable for long-term analysis simulations of fatigue due to contact between level ice and structures such as ships operating in polar waters. However, existing semi-
analytical methods still have problems in terms of the accuracy of the results because various assumptions are introduced, as mentioned above. Therefore, we try to solve this problem by using a numerical method with a low computational cost for level ice-structure interaction analysis. The proposed analysis method uses the boundary element method (BEM) as a numerical method. This is the first attempt at using the BEM to analyze level ice-structure interaction, for which the BEM is expected to provide high analytical accuracy and a low computational cost.

The BEM is more advantageous in terms of computational cost than the FEM because elements are divided only at boundaries. Furthermore, when the level ice is broken and a new boundary is created in the conventional FEM, the stiffness matrix must be newly constructed. However, in the case of BEM, the construction time of a new stiffness matrix can also be shortened because only the stiffness matrix components corresponding to the changed boundary elements need to be modified. Furthermore, the third and fourth derivative values of the deflection of the plate should be calculated to determine the stress response of the plate. In the case of FEM, significant accuracy problems occur depending on the type of element. However, the BEM is highly effective at finding the differential values (e.g., stress, strain, moment, shear force) of the response (e.g., deflection) within the domain because the differential value within the domain is directly derived from the Rayleigh-Green identity, which will be discussed later (Katsikadelis, 2002).

A governing equation for the plate bending problem is first established by introducing the plate theory in Section 2.1. In Section 2.2, the BEM for the governing equation is formulated using the BEM. To ensure efficient calculations in this formulation, a constant boundary element was used to lower the computational cost. Analyses using the established BEM methodology were performed for square and triangular models under various boundary conditions and loads. The BEM analysis results were then compared with the FEM analysis results obtained using the commercial finite element structural analysis software Abaqus.

2. Theoretical Background

2.1 Plate Theory

A plate is a flat shape with a relatively small thickness \( h \) and is defined by the middle plane that bisects the plate thickness, the plate

\[
D \nabla^4 w = f \quad \text{in} \quad \Omega
\]  
(1)

Here, \( D \) is the bending stiffness of the plate, which is defined by the modulus of elasticity \( E \), Poisson's ratio \( \nu \), and plate thickness \( h \), as follows:

\[
D = \frac{E h^3}{12(1-\nu^2)}
\]  
(2)

where \( \nabla^4 \) is a biharmonic operator, for which the Laplacian operator is applied consecutively. Using the general Hooke’s law of 3D isotropy, the plate’s bending moment and torsional moments \( M_x \), \( M_y \), and \( M_{xy} \) can be derived from the strain-deflection relationship of the thin plate as follows (Armenakas and Katsikadelis, 1989):

\[
M_x = -D \left( \frac{\partial^2 w}{\partial x^2} + \nu \frac{\partial^2 w}{\partial y^2} \right)
\]  
(3)

\[
M_y = -D \left( \frac{\partial^2 w}{\partial y^2} + \nu \frac{\partial^2 w}{\partial x^2} \right)
\]  
(4)

\[
M_{xy} = D(1-\nu) \frac{\partial^2 w}{\partial x \partial y}
\]  
(5)

The bending moment and effective shear force defined at the boundary are differential operators \( V \) and \( M \), which can be expressed as follows:

\[
V = -D \left[ \nabla^2 + (\nu - 1) \frac{\partial^2}{\partial t^2} \right]
\]  
(6)

\[
M = -D \left[ \frac{\partial}{\partial n} \nabla^2 - (\nu - 1) \frac{\partial}{\partial n} \left( \frac{\partial^2}{\partial n \partial t} \right) \right]
\]  
(7)

here the \( n \)-direction is the normal direction at the boundary point, the \( t \)-direction is the tangent direction at the boundary point, and the \( s \)-direction is the arc direction at the boundary point on the boundary line with a curvature. The \( s \) - and \( t \)-directions coincide at the boundary line, which is a straight line.

The boundary conditions of the plate are expressed as Eqs. (8) and (9). The fixed, simply supported, and free boundary conditions are defined in accordance with the values of \( \alpha_i \) and \( \beta_i \) (\( i = 1,2,3 \)).

\[
\alpha_1 w + \alpha_2 Vv = \alpha_3 \text{ on } \Gamma
\]  
(8)

\[
\beta_1 \frac{\partial w}{\partial n} + \beta_2 Mv = \beta_3 \text{ on } \Gamma
\]  
(9)
Additional boundary conditions are required owing to the discontinuous nature at a corner point where the normal and tangent directions at the boundary change discontinuously. The boundary condition at the kth corner point is given as follows:

$$ c_{ik} w + c_{ik} \{ T_v \}_k = c_{ik} $$  \hfill (10)

where $c_{ik}$ $(i=1,2,3)$ is determined by the boundary conditions at both boundaries of the corner point. $T$ is a torsional moment operator that satisfies the relationship of $M_r = Tw$ and can be expressed as Eq. (11). The torsional moment has discontinuous values owing to the discontinuous changes in the normal and tangent directions at the corner point. It is a fictitious corner force, equal to the difference in $\{ \theta \}$ values, and can be expressed as Eq. (12).

$$ T = D(1 - \nu) \frac{\partial^3}{\partial n \partial \theta} $$  \hfill (11)

$$ \{ T_v \}_k = T_{v_k} - T_{v_r}  \hfill (12) $$

2.2 Boundary Element Method Formulation of Plate Theory

The BEM formulation is performed using a constant boundary element to reduce the computational cost. This formulation starts from the Rayleigh-Green identity based on the Gauss-Green theorem. The Rayleigh-Green identity is an identity that is established for two random functions $u$ and $v$, where the fourth derivative is continuous within the domain $\Omega$ and the third derivative is continuous at the domain boundary $\partial \Omega$. It represents the relationship expressed as follows:

$$ \int_{\Omega} [v \nabla^4 w - w \nabla^4 v] d\Omega = \int_{\Omega} \left[ \frac{\partial}{\partial n} \nabla^2 w - \frac{\partial}{\partial n} \nabla^2 w - \frac{\partial}{\partial n} \nabla^2 w + \frac{\partial}{\partial n} \nabla^2 w \right] d\Gamma $$  \hfill (13)

Eq. (13) can be reorganized as Eq. (14) by using the operator introduced in section 2.1. Eq. (14) is called the generalized Rayleigh-Green identity.

$$ D \int_{\Omega} [v \nabla^4 w - w \nabla^4 v] d\Omega = \int_{\Omega} \left[ v \nabla^4 w - w \nabla^4 v + \frac{\partial}{\partial n} M_v + \frac{\partial}{\partial n} M_w \right] d\Gamma + \sum_k \left( v \{ T_v \}_k \right) $$  \hfill (14)

The deflection $w$, which is a response of the plate, is calculated through the relationship with function $v$ using Eq. (14), which is the Rayleigh-Green identity. The function $v$ used here is the fundamental solution of the biharmonic equation, and indicates a particular solution of Eq. (15).

$$ D \nabla^4 v = \delta(Q-P) \quad \text{in} \ \Omega $$  \hfill (15)

In Fig. 2, $v$ denotes a rotational symmetry function, $P$ denotes the source point at the center of the rotational symmetry of function $v$ in the domain, and $Q$ denotes the field point representing the value of the response field. Furthermore, $p$ and $q$ denote the source point and field point at the boundary, respectively.

Values inside the domain can be calculated using the Rayleigh-Green identity and the fundamental solution. When the source point of $v$ in Eq. (10) is positioned inside the domain, the deflection inside the domain is calculated using the $w$, $\{T_v\}$ value at $w$, one of the $M_v$, $V_v$, and the corner point at the boundary, as shown in Eq. (17).

$$ w(P) = \int_{\Omega} v \nabla^4 \Omega + \int_{\Gamma} \left[ v \nabla^4 w - w \nabla^4 v + \frac{\partial}{\partial n} M_v + \frac{\partial}{\partial n} M_w \right] d\Gamma - \sum_k \left( v \{ T_v \}_k \right) $$  \hfill (17)

The term $\int_{\Omega} v \nabla^4 \Omega$, which still remains as the domain integral, is calculated by applying the boundary integral and the Rayleigh-Green identity again, and converting to the boundary integral $\int_{\partial \Omega} v \nabla^4 \Omega$ (Katsikadelis, 2014).

2.2.1 Boundary integral equation

The values inside the domain can be obtained from the boundary values, and the boundary values are calculated from the boundary
condition and boundary integral equation. The first boundary integral equation can be obtained by matching the source point in the Rayleigh-Green identity to the boundary and corner points, respectively. This can be expressed as follows (Katsikadelis et al., 1977):

\[
\frac{\alpha}{2\pi} w(p) = \int_{\Gamma} v f d\Gamma + \int_{\Gamma} \left[ \nabla \cdot \left( \begin{array}{c} V_w \ -w \nabla V_t - \frac{\partial V_t}{\partial n} M_v + \frac{\partial V_v}{\partial n} M_t \end{array} \right) \right] d\Gamma - \sum_k \left\{ \begin{array}{c} V_t \ 3_k - w \begin{array}{c} V_t \end{array} \end{array} \right\} \ (18)
\]

This \( \alpha \) value is different from \( \alpha_k \) in Eq. (8). As shown in Fig. 2, \( \alpha \) indicates the angle inside the domain at source point \( p \). When the source point \( p \) is matched to the corner point at which the \( n \) and \( t \) directions change discontinuously, \( \alpha \) is the inner angle value of the corner. If the source point is positioned at a smooth boundary where the \( n \) and \( t \) directions are not discontinuous, \( \alpha \) is calculated as \( \pi \).

The second boundary integral equation can be obtained through the \( \nu \)-direction differentiation of \( \nu \) when point \( P \) inside the domain approaches to point \( p \) at the boundary, and can be expressed as follows (Bezine, 1978):

\[
\frac{1}{2} \nu_1(p) = \int_{\Gamma} v_i f d\Gamma + \int_{\Gamma} \left[ \nabla \cdot \left( \begin{array}{c} V_{31} - w V_{31} - \frac{\partial V_{31}}{\partial n} M_v + \frac{\partial V_{21}}{\partial n} M_t \end{array} \right) \right] d\Gamma - \sum_k \left\{ \begin{array}{c} V_{31} \ 3_k - w \begin{array}{c} V_{31} \end{array} \end{array} \right\} \ (19)
\]

where \( v_i \) is a function that has been directionally differentiated in the \( \nu \) direction, \( v \) which is the direction that is normal to the boundary at point \( p \) in Fig. 2. It can be expressed as follows:

\[
v_i = \frac{1}{8\pi D} r_v (1 + 2\pi v)
\]

The linear algebraic equation can be constructed by using both the boundary conditions and boundary integral equations obtained above, and the boundary values can be calculated through this process. The method of calculating detailed boundary elements involving the calculation of singular points of matrix elements and near-singular points is introduced in Katsikadelis (2002).

3. BEM Verification for Plate Problem

3.1 Analysis Model

The BEM code of the plate bending problem was verified using two plate models. Square and triangular models were used as simple analysis models. For these two models, the elements were divided into boundary and finite elements, as shown in Fig. 3. The physical properties of the square model are summarized in Table 1, and the material properties of the triangular model are summarized in Table 2.

For both triangular and square models, a Poisson’s ratio of 0.3 and a modulus of elasticity of \( 2\times10^8 \) N/m² were used. The square model consisted of 7,500 finite elements and 400 boundary elements. The triangular model consisted of 3,043 finite elements and 242 boundary elements.

<table>
<thead>
<tr>
<th>Table 1 Properties of square model</th>
</tr>
</thead>
<tbody>
<tr>
<td>Variable</td>
</tr>
<tr>
<td>Number of elements</td>
</tr>
<tr>
<td>Poisson’s ratio</td>
</tr>
<tr>
<td>Young’s modulus</td>
</tr>
<tr>
<td>Thickness</td>
</tr>
<tr>
<td>Length</td>
</tr>
<tr>
<td>Width</td>
</tr>
</tbody>
</table>

(a) Square model (FEM)  
(b) Square model (BEM)  
(c) Triangular model (FEM)  
(d) Triangular model (BEM)

Fig. 3 Finite element method (FEM) and BEM models for square and triangular models.
Table 2 Properties of triangular model

<table>
<thead>
<tr>
<th>Variable</th>
<th>FEM</th>
<th>BEM</th>
</tr>
</thead>
<tbody>
<tr>
<td>Number of elements</td>
<td>3,043</td>
<td>242</td>
</tr>
<tr>
<td>Poisson’s ratio</td>
<td>0.3</td>
<td></td>
</tr>
<tr>
<td>Young’s modulus</td>
<td>$2\times10^8$ N/m$^2$</td>
<td></td>
</tr>
<tr>
<td>Thickness</td>
<td>0.05 m</td>
<td></td>
</tr>
<tr>
<td>Base length</td>
<td>5 m</td>
<td></td>
</tr>
<tr>
<td>Height</td>
<td>2.5 m</td>
<td></td>
</tr>
</tbody>
</table>

To verify the BEM code for the plate problem, analyses was performed under various boundary and load conditions. Then, the analysis results were compared with the analysis results obtained using the FEM. First, the responses obtained using fixed boundaries and distributed loads were checked. For this purpose, a fixed boundary constraint was given to all boundaries for the square model, and a load of $f = -10y+25$ (N) was applied over the entire domain of the plate.

To verify the responses by simply supported and distributed loads, a simply supported constraint was given to the boundary for the triangular model. Then, analysis was performed by applying the load of $f = -10y+25$ (N) over the entire domain of the plate. This corresponds to cases 1 and 2 in Table 3.

In the case of the boundary condition of a semi-infinite ice sheet such as level ice, it is similar to the situation in which a free boundary is applied at the boundary of the level ice that comes in contact with the ship, and a strong constraint condition is applied at all boundaries except the free boundary. Therefore, the case of exposure to a concentrated unit load in the boundary condition including the free boundary was also analyzed. For the square and triangular models, the free boundary was applied to the boundary at $y = 0$ m, and the fixed boundary was applied to all boundaries except the free boundary. Then, for the square model, a concentrated unit load of 1 N was applied to the position of $x = 0.5$ m and $y = 1.5$ m. For the triangular model, a concentrated unit load of 1 N was applied to the position of $x = 2.5$ m and $y = 1$ m. These correspond to cases 3 and 4 in Table 3.

To obtain the responses of the BEM, Eqs. (18) and (19) were concatenated to form a linear algebraic equation. These values were then used to obtain the deflection in the domain and the differential terms of the deflection using Eq. (17). The differential values of the calculated deflection were used to calculate the bending and torsional moments using Eqs. (3) through (5). For comparison, the FEM responses were calculated through an analysis performed using the commercial structural analysis software Abaqus. The finite elements, which are S8R secondary shell elements, were calculated under the same boundary condition and load.

3.2 Analysis Results

The deflection, bending moment, and torsional moment of the plate were calculated as response results of the BEM and FEM. The response results for cases 1 and 2 are illustrated in Figs. 4 and 5, respectively.

Figs. 4 and 5 show that the FEM results are similar to the BEM results for both bending and torsional moments. In the case of the torsional moment, the signs of the FEM results and the BEM results are different because of the difference in the predetermined sign convention for the torsional moment. To provide a closer comparison in case 1, the results were compared at $y = 0.75$ m, where the fluctuation of values is large in

Table 3 Boundary conditions and loads (C: Clamped, SS: Simply supported, F: Free, CON: Concentrated unit load, DIS: Distributed load)

<table>
<thead>
<tr>
<th>Case</th>
<th>Model geometry</th>
<th>Boundary condition</th>
<th>Load</th>
</tr>
</thead>
<tbody>
<tr>
<td>case 1</td>
<td>Square model</td>
<td>C</td>
<td>DIS ($f = -10y+25$ [N])</td>
</tr>
<tr>
<td>case 2</td>
<td>Triangular model</td>
<td>SS</td>
<td></td>
</tr>
<tr>
<td>case 3</td>
<td>Square model</td>
<td>C and F</td>
<td>CON (at $x = 1.5, y = 0.5$)</td>
</tr>
<tr>
<td>case 4</td>
<td>Triangular model</td>
<td>C and F</td>
<td>CON (at $x = 2.5, y = 1$)</td>
</tr>
</tbody>
</table>

![Fig. 4](image-url) Comparison between FEM and BEM results (Case 1)
A Study on Plate Bending Analysis Using Boundary Element Method

Fig. 4 Comparison between FEM and BEM results (Case 1) (Continuation)

Fig. 5 Comparison between FEM and BEM results (Case 2)
terms of the deflection and bending moment, and at \( x = 1.5 \) m for the torsional moment. The comparison results are shown in Fig. 6. Similarly, in case 2, the results were compared at locations with large fluctuations, and the results are shown in Fig. 7. Here, the deflection and bending moments were compared at \( x = 2.5 \) m, and the torsional moment was compared at \( y = 0.3 \) m.

In case 1, the deflection in the BEM result shows slightly smaller responses in general than that in the FEM result. As shown, the bending and torsional moments coincide almost exactly. In case 2, it can be seen that the deflection, bending moment, and torsional moment all coincide almost exactly. Here, torsional moments were compared by matching signs.

The FEM and BEM response results for cases 3 and 4 are shown in Figs. 8 and 9, respectively.
Fig. 8 Comparison between FEM and BEM results (Case 3)

(a) Deflection (FEM)  (b) Deflection (BEM)

(c) Bending moment $M_x$ (FEM)  (d) Bending moment $M_x$ (BEM)

(e) Bending moment $M_y$ (FEM)  (f) Bending moment $M_y$ (BEM)

(g) Twisting moment $M_{xy}$ (FEM)  (h) Twisting moment $M_{xy}$ (BEM)

Fig. 9 Comparison between FEM and BEM results (Case 4)

(a) Deflection (FEM)  (b) Deflection (BEM)

(c) Bending moment $M_x$ (FEM)  (d) Bending moment $M_x$ (BEM)
As shown in Figs. 8 and 9, the deflection, bending moment, and torsional moment results obtained using the FEM and BEM are quite similar. Furthermore, comparing the responses of cases 3 and 4 also confirms that the contours of the responses are similar because the bottom edge is a free boundary and the remaining edges have a similar situation involving a fixed support. In case 3, the torsional moment increases to become sufficiently large and then decreases as it approaches the boundaries, except for the lower boundary. However, in case 4, it does not become small enough as it approaches the boundary, and has its maximum and minimum values at the boundary. It seems that this difference appears because the boundaries other than the lower boundary of case 4 are relatively closer, compared to those in case 3, to the point where the concentrated unit load is applied. As in cases 1 and 2, the signs of the torsional moment of the FEM and BEM results are calculated differently owing to the difference in the sign convention.

![Fig. 9 Comparison between FEM and BEM results (Case 4) (Continuation)](image)

![Fig. 10 Comparison between FEM and BEM results along the path (Case 3)](image)
For a more in-depth comparison of cases 3 and 4, a comparison according to the path is shown in Figs. 10 and 11. As in cases 1 and 2, these cases were compared at locations with large fluctuations. In case 3, deflection and bending moments were compared at \( y = 0.75 \) m, and the torsional moment was compared at \( x = 1.5 \) m. In case 4, deflection and bending moments were compared at \( x = 2.5 \) m, and the torsional moment was compared at \( y = 0.3 \) m.

The results for both cases 3 and 4 show that the BEM results are very similar to the FEM results. Moreover, it can be seen that the value of the bending moment changes significantly at the location at which the concentrated unit load is applied.

### 4. Conclusions

This study proposed a numerical methodology for level ice-structure interaction analysis for application to long-term fatigue analysis of ships operating in polar waters. First, the behavior of level ice was applied to the plate bending problem. In addition, a governing equation for plate behavior was established by introducing plate theory. To solve the governing equation, the BEM, which has high accuracy and a low computational cost, was introduced. The BEM formulation of the governing equation was also performed. BEM and FEM analyses were conducted under various boundary conditions and loads for simple square and triangular models, and the results were compared. Based on the above analysis results, the following conclusions can be drawn.

1. An analysis method with a low computational cost is needed for long-term fatigue analysis of ships operating in polar waters in contact with level ice. A numerical methodology using the BEM was proposed for this analysis.

2. The governing equation of the plate theory was formulated using the BEM after applying the behavior of level ice to the plate problem.

3. The BEM results were compared with the analysis results of Abaqus, a commercial finite element structural analysis program, to verify the accuracy of the BEM methodology.

4. Both the analysis results performed under distributed load at fixed and simply supported boundaries and the analysis results under concentrated unit load at the boundaries including the free boundary were similar to the analysis results of Abaqus, a commercial finite element structural analysis program.

5. The proposed method showed results that were sufficiently similar to the finite element results, using very few boundary elements compared to the number of finite elements. Therefore, this method is highly suitable in terms of computational cost and accuracy for application to many flat bending analysis scenarios for fatigue caused by ice load.

6. It is necessary to assume the condition of an elastic foundation because the level ice at sea is under buoyancy. Therefore, we plan to conduct additional research considering buoyancy.

7. Furthermore, we will perform fatigue life evaluation by conducting long-term fatigue analysis using the BEM methodology after buoyancy is added.

### Conflict of Interest

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


Author ORCIDs

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<th>ORCID</th>
</tr>
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<tbody>
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<tr>
<td>Kim, yooil</td>
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1. Introduction

Subsea power cables are exposed to a wide range of unforeseen external loads in the marine environment during installation and operation. There is an urgent need for a system capable of accurately analyzing and testing the mechanical strength of the cables to achieve their target service life, while withstanding the harsh marine environment. The mechanical properties of subsea power cables can be categorized into bending stiffness, tensile stiffness, and torsional stiffness. These properties are instrumental in determining their behavior.

The components of a typical subsea power cable include the following: a conductor for power transmission, insulation material for wrapping the conductor (cross-linked polyethylene: XLPE), lead sheath, sheath, fiber, armor wire, and yarn (polypropylene: PP) to protect the armor wire (See Fig. 1). Of these, the conductors and armor wires use metallic materials: copper or aluminum is mainly used for conductors, and carbon steel is used for armor wires. As described above, subsea power cables have composite hierarchical structures in which different materials are employed and multiple component layers with two types of structure (cylindrical and helical) are integrated in arbitrary sequences. Among the different component layers, the conductors and armor wires account for a significant proportion of the mechanical properties of subsea power cables. These components have a helical structure. Because these are twisted in a certain direction, the mechanical properties of the cable vary depending on the direction of rotation. In addition, it is difficult to predict the mechanical properties of the cables because these are significantly affected by the cross-sectional area, physical properties of the material, and pitch angle of the helical elements.

Fig. 1 Components of subsea power cables
The mechanical properties of subsea power cables can be predicted based on experimental, theoretical, and numerical analysis using the finite element method (FEM). Lutchansky (1969) and Knapp (1979) proposed theoretical models for analyzing the stiffness and axial stress distribution of armor wires. In their research, bending, axial tensile, and torsional stiffness tests of subsea power cables were performed to investigate the correlation between the proposed equations and experimental results. Based on the research, subsequent studies on the mechanical properties of a range of subsea cables such as subsea power cables, umbilicals, and submarine pipelines were conducted through physical experiments and improved equations for the theory. Vaz et al. (1998), Coser et al. (2016), Ekeberg and Dhaigude (2016), Komperød et al. (2017), and Delizisis et al. (2021) investigated mechanical properties such as tensile stiffness and bending stiffness through experiments with full-scale subsea cables. Furthermore, Witz and Tan (1992), Huang and Vassalos (1993), Kebadze (2000), Skeie et al. (2012), and Komperød (2017) conducted studies on the analysis of the mechanical properties of subsea cables and prediction of the stress distribution in armor wires using numerical models, i.e., theoretical formulas. However, there has been limited research for verifying the validity of the proposed equations. Similar to the cases of other fields of research, the advancement in computer performance and the development of commercial software over the past decades have had a substantial impact on the research capabilities for analyzing the mechanical properties of subsea power cables. For example, the following are software programs specialized for the mechanical analysis of pipes and cables with composite hierarchical structures, such as subsea power cables: CableCAD; Helica; UFLEX; and commercial software based on the FEM, such as ABAQUS, COMSOL, and ANSYS. Shaw (2011), Lu et al. (2017), Tjahjanto et al. (2017), and Chang and Chen (2019) investigated the behavior of subsea cables by performing 3D finite element analysis. In addition, they compared the results of the numerical analysis with theoretical formulas or experimental results. Although these studies have demonstrated the effectiveness of numerical analysis based on FEM, numerical simulations using FEM incurs high computational cost.

In this study, experimental and theoretical analyses were performed to predict the axial stiffness of subsea power cables. Axial stiffness is a measure of the resistance to deformation along the longitudinal direction under an applied tension. In the experimental analysis, a uniaxial tensile test was performed on a 6.5 m three-core AC inter-array subsea power cable specimen by using a 10 MN hydraulic testing system. In the theoretical analysis, the axial stiffness of the subsea power cable specimen was predicted based on the theoretical framework presented by Witz and Tan (1992). Furthermore, the predicted results were compared with the experimental results.

### 2. Experimental Analysis

In CIGRE Technical Brochure (TB) 623 (CIGRE, 2015), the mechanical properties of subsea power cables are defined in terms of torsion, tension, bending, compression, impact, fatigue, penetration, friction coefficient, etc. In addition, the experimental methods for analyzing each property are described in detail. In this study, a uniaxial tensile test was performed to analyze the mechanical properties of subsea power cables subjected to tensile loading (axial stiffness), with reference to CIGRE TB 623. Subsea power cables (e.g., umbilicals, wire ropes, and mooring ropes) are slender bodies that are affected substantially by tensile loading in the longitudinal direction. To experimentally analyze the slender body specimen, the testing system described below was designed and fabricated to continuously withstand large loadings and to have a large stroke range of the crosshead to secure the length of the specimen and conveniently mount the specimen on the testing system. The hydraulic testing system for subsea power cables used in this experimental study has the following specifications: a maximum load of 10 MN, permissible specimen length of 4–13 m, maximum displacement of 700 mm, and maximum speed of 600 mm/min. Fig. 2 presents a schematic diagram of the hydraulic testing system used in the experimental analysis.

A three-core AC inter-array subsea power cable was used in the experiment. Three-core AC inter-array subsea power cables are used for transmitting the power generated by offshore wind turbines to the offshore platform. In terms of axial stiffness tests, subsea power cables have two boundary conditions: fixed end–fixed end and fixed end–free end. In this experiment, only uniaxial tensile load was applied to the subsea power-cable specimen without rotation of the cross-section considering the fixed boundary conditions at both ends. An armor pot jig was specially designed and fabricated for modeling the complete fixed end condition at both ends of the cable. The armor pot jig was used for reproducing complete fixation by pouring silicone after connecting the fabricated metal frame to the end of the cable. The armor pot jig was connected to the hydraulic testing system at both ends. One end was connected to a hydraulic actuator to apply load control (See Fig. 3). The load and displacement data of the specimen
Experimental and Theoretical Study on the Prediction of Axial Stiffness of Subsea Power Cables

were measured as functions of time and stored in the hydraulic actuator.

The length of the specimen was 6.5 m. An initial longitudinal displacement of 0.5 mm (a load of approximately 20 kN) was applied before the test to minimize the deflection caused by the self-weight. Three tests were performed on the same specimen. Fig. 4 presents the load-displacement curve of the specimen. As is evident from the figure, the subsea power-cable specimen shows a linear load-displacement curve in the three tests conducted. Table 1 outlines the axial stiffness values of the subsea power cables. The lowest axial stiffness value was obtained in the first test. This was because of the presence of significantly small gaps between each component layer during the fabrication of subsea power cables (Witz and Tan, 1992). The gap was minimized or removed after the first uniaxial tensile test. Consequently, the axial stiffness measured from the second and third loadings were constant.

3. Theoretical Analysis

In this study, the theoretical models for predicting the mechanical properties of subsea power cables proposed by Witz and Tan (1992) were used. The equations are presented by dividing between cylindrical elements and helical elements. Consequently, these have advantage in terms of application to subsea power cables and to umbilicals and flexible pipes, which are hierarchical structures composed of cylindrical and helical elements. In addition, variations in the thickness and radius of each layer can be predicted because the interactions between component layers can be considered.

3.1 Theoretical Analysis of a Cylindrical Element

First, the equations for cylindrical elements proposed by Witz and Tan (1992) are based on the assumption of homogeneous materials. Fig. 5 shows the stress components of the cylindrical element.

The torsional load $M_r$; pressures on the inner and outer surfaces, respectively ($q_{in}$, $q_{out}$); and the stress components within the element when the cylindrical element is subjected to a constant axial load $F$ are expressed as follows:

\[
\sigma_{\alpha} = \frac{F}{2\pi R t_e} \quad (1)
\]
\[
\sigma_{\theta} = \frac{q_{in}-q_{out}}{t_e} R \quad (2)
\]
\[
\tau = \frac{M_r}{2\pi R^2 t_e} \quad (3)
\]

$t_e$ = thickness of the element  
$\sigma_{\alpha}$ = axial stress  
$R$ = radius of the cylinder  
$\sigma_{\theta}$ = circumferential stress  
$\tau$ = shear stress

Assuming that uniform deformation occurs across the cylindrical element under a constant load, the strain components are expressed as follows:

\[
\varepsilon_{\alpha} = \frac{\Delta L}{L} \quad (4)
\]
\[
\varepsilon_{\theta} = -\frac{\Delta R}{R} \quad (5)
\]
\[
\varepsilon_{\tau} = \frac{\Delta t}{t_e} \quad (6)
\]
In the equation for a cylindrical element, it is assumed that a constant out-of-plane stress is present owing to the pressures on the inner and outer surfaces. That is, the radial stress \( \sigma_r \) can be expressed as Eq. (8):

\[
\sigma_r = \frac{1}{2} (q_{in} + q_{out})
\]

Assuming that an isotropic material is applied, the stress–strain relationship in the elastic domain is as follows:

\[
\varepsilon_r = \frac{1}{E} (\sigma_r - \nu \sigma_\theta - \sigma_\gamma)
\]

\[
\varepsilon_\theta = \frac{1}{E} (\sigma_\theta - \nu \sigma_r - \sigma_\gamma)
\]

\[
\varepsilon_\gamma = \frac{1}{G} \tau
\]

\( E \) = Young’s modulus \( v \) = Poisson’s ratio \( G \) = Shear modulus

The stress components of the cylindrical element are derived as follows from Eqs. (9)–(12):

\[
\sigma_x = \frac{E(1-v)}{(1+v)(1-2v)} \left[ \varepsilon_x + \frac{v}{1-v} (\varepsilon_y + \varepsilon_z) \right]
\]

\[
\sigma_y = \frac{E(1-v)}{(1+v)(1-2v)} \left[ \varepsilon_y + \frac{v}{1-v} (\varepsilon_x + \varepsilon_z) \right]
\]

\[
\sigma_z = \frac{E(1-v)}{(1+v)(1-2v)} \left[ \varepsilon_z + \frac{v}{1-v} (\varepsilon_x + \varepsilon_y) \right]
\]

\( \tau = G\gamma \)

3.2 Theoretical Analysis of Helical Elements

A distinct structural characteristic of subsea power cables is the presence of component layers of the helical elements. In the case of three-core cables, the core composed of the conductor of the subsea power cable, insulation material protecting the conductor, lead sheath, sheath, yarn, and armor wires have helical structures. The initial bending curvature and torsion of the helical elements according to the radius and pitch angle of the helical element layer are as follows:

\( K_0 = 0 \)

\( K'_0 = \frac{\cos^2 a}{R} \)

\( K''_0 = \frac{\sin a \cos a}{R} \)

Eqs. (24)–(26) show the final bending curvature of the helical elements when deformation occurs in the pitch angle of the helical component layer and helical elements owing to an arbitrary external force:

\( K_i = 0 \)

\( K'_i = \frac{\cos^2 a_i}{R - \Delta R} \)

\( K''_i = \frac{\sin a_i \cos a_i}{R - \Delta R} \)

\( K_i = \) final bending curvature in the normal direction

\( K'_i = \) final bending curvature in the binormal direction

\( K''_i = \) initial twisting in the axial direction

\( a_i = \) initial helical angle of the strip

\( \Delta R = \) constriction in helical radius

In the theoretical analysis of helical elements, the equation of elastic...
equilibrium presented by Love (2013) can be expressed as follows if the external force, shear force, and moment applied to the helical elements are assumed to be constant:

\[-N K_1 + T K'_1 + X = 0\]  
\[-B K_1 + H K'_1 - N' = 0\]

(27)  
(28)

\(N\) = shear force in the binormal direction  
\(B\) = bending moment in the binormal direction  
\(T\) = axial force  
\(X\) = distributed lateral force in the negative normal direction

If the helical elements undergo deformation in the elastic domain, the bending moment and torsional load are proportional to the bending curvature and the variation in twisting, respectively:

\[\beta' = \beta (1 + \varepsilon_t) - 1\]  
\[\phi' = \phi (1 + \varepsilon_t) - 1\]

(29)  
(30)

\(\beta'\) = binormal bending stiffness of the strip  
\(\phi'\) = torsional stiffness of the strip

The strain in the axial direction of the helical elements is expressed as Eq. (31):

\[\varepsilon_a = \frac{T}{EA}\]

(31)

\(\varepsilon_a\) = axial strain of the strip  
\(EA\) = axial tensile stiffness of the strip

According to the geometric relationship between the initial helical element and the helical elements after deformation, \(\varepsilon_a\) can be derived as a function of \(\varepsilon_t\) as follows:

\[\varepsilon_a = \frac{\sin \alpha}{\sin \alpha_t} (1 + \varepsilon_t) - 1\]

(32)

\(\alpha_t\) = helical angle of the stressed strip

Considering the geometric relationship again, \(\alpha_t\) can be expressed as a function of \(\Delta R, \varepsilon_t, \) and \(\phi\):

\[\alpha_t = \arctan \left[ \tan \left( \frac{1 + \varepsilon_t}{\Delta R} \frac{0L}{2\pi} \right) \right]\]

(33)

\(L\) = initial pitch length of the helical strip

The stress distribution in the normal direction of the helical element can be expressed in terms of the difference between the external and internal surface pressures: \(q_{int} - q_{ext} = \Delta q\). Here, \(b\) is the effective width of the helical element. From Eqs. (27) and (28), the pressure difference between the outer and inner surfaces of the helical element is rearranged and derived as Eq. (34):

\[q_{int} - q_{ext} = \frac{1}{b} \left[ (-B K_1 + H K'_1) K_1 - T K'_1 \right]\]

(34)

The axial load generated in the entire layer of a helical element is equal to the sum of the axial loads of each helical element. Eq. (35) illustrates the axial load generated when \(n\) elements are present in the helical element layer.

\[F = n (T_x + \cot \phi)\]

(35)

3.3 Theoretical Analysis of Structural Characteristics of Subsea Power Cables Based on Composite Hierarchical Integration

In this section, we describe the method for theoretically analyzing a composite hierarchical structure considering the interaction of each layer when the cylindrical and helical layers are integrated in an arbitrary sequence. First, it is assumed that in the composite hierarchical structure, the respective cylindrical and helical layers have equal longitudinal strain (\(\varepsilon_t\)) and twisting angle (\(\phi\)) per unit length. The respective layers constituting the composite hierarchical structure interact with each other while undergoing different variations in radius or thickness under the action of an arbitrary external force. Furthermore, a governing equation is required to derive the expressions for the interactions of the respective layers. If a subsea power cable has \(m\) cylindrical layers out of the total \(n\) layers, we obtain the parameters of \(n\) variations in radius (\(\Delta R_i\)) and variations in thickness for \(m\) elements (\(\Delta \ell_i\)). In conclusion, the theoretical model of Witz and Tan (1992) uses a nonlinear governing equation with \(\Delta R\) as a single parameter. The process of deriving this governing equation is as follows:

The equation of equilibrium of the cylindrical element (Eq. (18)) and equation of equilibrium of the helical element (Eq. (34)) are expressed by the functions \(f_{c,i}\) and \(f_{h,i}\), respectively. The subscript of the outer and inner surface pressure \(q\) indicates the number of the layer composing the subsea power cables. The number increases in the direction from the innermost layer to the outermost layer.

\[q_i - q_{int} = f_{c,i} (\Delta R_i) \]  
\[q_i - q_{int} = f_{h,i} (\Delta R_i) \]  
\[q_{i+1} - q_i = f_{c,i} (\Delta R_i, \Delta \ell_i) \]  
\[q_{i+1} - q_i = f_{c,i} (\Delta R_{i+1}, \Delta \ell_{i+1}) \]

(36)  
(37)  
(38)  
(39)

\(q_{int}\) = internal pressure  
\(q_{ext}\) = external pressure
In addition, the subscripts c and h denote cylindrical elements and helical elements, respectively. The cylindrical elements are identified by using a prime symbol in a subscript. The contact pressure \( q_{i-1} \) in Eq. (36) is offset by adding all the equations of equilibrium. This yields the following:

\[
q_{ad} - q_{cd} = \sum_{k=1}^{i-1} f_{h,k}(\Delta R_k) + f_{c,i}(\Delta R_i, \Delta t_i) + \sum_{k=i+1}^{j'} f_{h,k}(\Delta R_k) + \sum_{k=1}^{n} f_{c,k}(\Delta R_k)
\]  

(37)

If the longitudinal deformation \( \Delta L \) of the structure and the twisting angle \( \phi \) are known, the function \( f_{h,k} \) has a single parameter (\( \Delta R_k \)), and the function \( f_{c,i} \) has two parameters (\( \Delta R_i \) and \( \Delta t_i \)). As is evident from Eq. (37), the thickness of the cylindrical element in the structure reduces, whereas that of the helical element does not. Owing to the interaction between adjacent helical element layers, the same variation in radius occurs (\( \Delta R_{i-1} = \Delta R_i \)). Furthermore, the variation in thickness of the cylindrical element is equal to the difference in radius variation between the adjacent helical element layers (\( \Delta \tau_i = \Delta R_{i-1} - \Delta R_i \)). Therefore, Eq. (37) can be expressed as a function of radius variation, \( \Delta \tau_i \).

\[
q_{ad} - q_{cd} = \sum_{k=1}^{i-1} f_{h,k}(\Delta R_k) + f_{c,i}(\Delta R_i, \Delta \tau_i) + \sum_{k=i+1}^{j'} f_{h,k}(\Delta R_k) + \sum_{k=1}^{n} f_{c,k}(\Delta R_k)
\]  

(38)

Here, we need to express the function \( F' \) in terms of a single parameter (\( \Delta R_i \)). To achieve this, \( \Delta \tau \) needs to be expressed as a function of \( \Delta R_i \). From Eqs. (18) and (19), the internal contact pressure of the cylindrical element can be obtained as follows:

\[
q_{i-1} = q_i \frac{\Delta R_i}{\tau_{i-1}} + \mu_i \Delta R_i + \delta_i
\]  

(39)

\[
q_i = \frac{E(1-\nu)}{(1+\nu)(1-2\nu)} \left( 1 + \frac{t_{uv}}{2R(1-\nu)} \right)
\]

\[
\mu_i = \frac{E(1-\nu)}{(1+\nu)(1-2\nu)} \left( \frac{v}{v} \right) + \frac{t_{uv}}{2R}
\]

\[
\delta_i = \frac{E(1-\nu)}{(1+\nu)(1-2\nu)} \left( 1 + \frac{t_{uv}}{2R} \right)
\]

In addition, if the value of the initial internal pressure is known, from Eq. (36), \( q_{i-1} \) can be expressed as a combination of hierarchical helical elements as shown in Eq. (40):

\[
q_{i-1} = q_{ad} + \sum_{k=1}^{i'-1} f_{h,k}(\Delta R_k)
\]  

(40)

The following is derived by substituting Eq. (39) into Eq. (40):

\[
q_{i-1} = q_{ad} + \left[ \sum_{k=1}^{i'-1} f_{h,k}(\Delta R_k) + q_{ad} - \mu_i \Delta R_i - \delta_i \right] \frac{t_{uv}}{q_i}
\]  

(41)

Eq. (38) (the governing equation) can be rearranged as follows:

\[
q_{i-1} - q_{cd} = F'(\Delta R_i, \Delta t_i) = F(\Delta R_i)
\]  

(42)

The Newton–Raphson method was applied to solve the governing equation \( F(\Delta R_i) \) as described above.

### 3.4 Three-core AC Subsea Power Cable

The cross-section of subsea power cables can be divided into two parts in terms of mechanical strength: (1) the part that combines the conductor constituting the center of the cable, insulation wrapping the conductor, lead sheath, sheath, and shaped filler used to maintain the original shape of the cable, and (2) the layer of armor wires to increase the mechanical strength of the subsea power cables. Here, the mechanical strength of the centrally located insulation, lead sheath, sheath, and shaped filler may be considered negligible. This is because these have a significantly low elastic modulus or cross-sectional area ratio compared with the conductor and armor wires. Consequently, the cable is more affected by the deformation caused by the friction force between adjacent layers than the deformation caused by an external force acting on the subsea power cable. Therefore, in the theoretical analysis of this study, it was assumed that the mechanical strength of insulation, lead sheath, sheath, and shaped filler layers with low elastic moduli may be omitted. Rather, a simplified core combining the conductor, insulation, lead sheath, and sheath was assumed and applied to the theoretical analysis (See Fig. 6). The values of the physical properties of yarn and armor wires required for theoretical analysis were obtained from Tjahjanto et al. (2017). In addition, the average elastic modulus of 36,500 MPa and Poisson's ratio of 0.4 were applied for the simplified core considering the elastic modulus of each layer and ratio of the cross-sectional area.

Accordingly, the subsea power cables used in the theoretical analysis were simplified to a composite hierarchical structure with four layers of helical elements (simplified core, first yarn, armor wires, lead sheath, and shaped filler).
Table 2 Parameters applied to theoretical analysis

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Value</th>
<th>Parameter</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>L</td>
<td>6.5 m</td>
<td>( R_1 ) (Simplified core)</td>
<td>34 mm</td>
</tr>
<tr>
<td>( \Delta L )</td>
<td>4 mm</td>
<td>( R_2 ) (1st Yarn)</td>
<td>62.6 mm</td>
</tr>
<tr>
<td>( \phi )</td>
<td>0 radian</td>
<td>( R_3 ) (Armor wires)</td>
<td>66.1 mm</td>
</tr>
<tr>
<td>( a_1 ) (Simplified core)</td>
<td>0.14 radian</td>
<td>( R_4 ) (2nd Yarn)</td>
<td>70.7 mm</td>
</tr>
<tr>
<td>( a_2 ) (1st Yarn)</td>
<td>1.05 radian</td>
<td>( q_{int} )</td>
<td>0 N</td>
</tr>
<tr>
<td>( a_3 ) (Armor wires)</td>
<td>0.23 radian</td>
<td>( q_{ext} )</td>
<td>0 N</td>
</tr>
<tr>
<td>( a_4 ) (2nd Yarn)</td>
<td>1.05 radian</td>
<td></td>
<td></td>
</tr>
</tbody>
</table>

Fig. 7 Theoretical result: Load-displacement curve and comparison with experimental results

and second yarn). Because all the components were helical elements, the thickness variation of each layer is not considered in the analysis. The thicknesses of the simplified core (1 core), first yarn, armor wires, and second yarn were 55, 2.2, 4.8, and 4.4 mm, respectively.

The values of the parameters required for the theoretical analysis are summarized in Table 2. In subsea power cables, there is no internal pressure as in the case of umbilicals or pipelines. Thus, the internal pressure \( q_{int} \) is 0 N. The external pressure \( q_{ext} \) was also considered as 0 N because no external pressure was applied to the cable in the experiment.

Fig. 7 shows the load-displacement curve predicted by the theoretical analysis. Although the governing equation Eq. (42) is a non-linear function, the load-displacement (longitudinal deformation) curve is linear. The axial stiffness calculated from the theoretical analysis is 286 MN. The radius variation \( \Delta R \) is 0.07 mm, which is insignificant (= 0.1% of the initial radius (approximately 75 mm)).

4. Discussion and Conclusion

In this study, experimental and theoretical analyses were performed to evaluate the axial stiffness of three-core AC subsea power cables. For the experimental analysis, a 6.5 m inter-array subsea power cable specimen was fabricated to perform uniaxial tensile test. In addition, the axial stiffness of the specimen was predicted using the theoretical model presented by Witz and Tan (1992). Furthermore, the predicted results were compared with the experimental results. The theoretical model of Witz and Tan (1992) predicted higher axial stiffness (error rate of approximately 6.6%) compared with the experimental results. The following are considered to be the causes of the error between the experimental and theoretical analysis results. First, because the model does not consider the interaction between elements in each layer as in the theoretical assumptions, it does not consider the resistance generated by interference or friction between elements. Second, the theoretical model does not consider the plastic deformation of materials that may occur in the manufacturing process of subsea power cables. That is, the mechanical strength of the helical elements may be overestimated because it is assumed that the helical element layer does not have a normal-direction curvature during production. In manufacturing, plastic deformation of the material may occur when the pitch angle of the helical elements is excessive or the radius of the helical element layer is significantly small. This, in turn, reduces the mechanical strength of the entire cable. Third, errors may be caused by oversimplification of the material properties (modulus of elasticity) and mechanical properties (bending stiffness, torsional stiffness) of the central part of the subsea power cable, i.e., a three-core structure. Nevertheless, the theoretical model used in this study is convenient to apply and has a significantly high computation speed compared with numerical analysis based on FEM. Therefore, it is considered an efficient method for predicting the mechanical behavior of subsea power cables. However, further testing and validation by comparing the results of the FEM-based numerical analysis are required for quantitatively verifying the feasibility and effectiveness of the method.

Although the uniaxial tensile test in this study was performed under fixed boundary conditions at both ends, the real-world subsea power cables are exposed to fixed-end-free end boundary conditions in the process of manufacturing, shipping, transshipment, burial, and installation. It is known that the axial stiffness of subsea power cables decreases under the fixed end-free end boundary condition. In addition, a more in-depth analysis of the stress distribution within each component and the initial deformation occurring during manufacturing of the cable is required. To achieve this, it is necessary to further analyze the axial stiffness according to the boundary conditions and the stress distribution within the components, and to modify the theoretical models to reduce the above-mentioned errors.

Conflict of Interest

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

References


Author ORCIDs

<table>
<thead>
<tr>
<th>Author name</th>
<th>ORCID</th>
</tr>
</thead>
<tbody>
<tr>
<td>Nam, Woongshik</td>
<td>0000-0002-9969-2574</td>
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<td>Chae, Kwangsu</td>
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<tr>
<td>Lim, Youngseok</td>
<td>0000-0002-0596-4656</td>
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Proposal of Parameter Range that Offered Optimal Performance in the Coastal Morphodynamic Model (XBeach) Through GLUE

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KEY WORDS: XBeach, GLUE, Maengbang beach, Quantization, Uncertainty, Objective calibration

ABSTRACT: The process-based XBeach model has numerous empirical parameters because of insufficient understanding of hydrodynamics and sediment transport on the nearshore; hence, it is necessary to calibrate parameters to apply to various study areas and wave conditions. Therefore, the calibration process of parameters is essential for the improvement of model performance. Generally, the trial-and-error method is widely used; however, this method is passive and limited to various and comprehensive parameter ranges. In this study, the Generalized Likelihood Uncertainty Estimation (GLUE) method was used to estimate the optimal range of three parameters (gamma, facua, and gamma2) using morphological field data collected in Maengbang beach during the four typhoons that struck from September to October 2019. The model performance and optimal range of empirical parameters were evaluated using Brier Skill Score (BSS) along with the baseline profiles, sensitivity, and likelihood density analysis of BSS in the GLUE tools. Accordingly, the optimal parameter combinations were derived when facua was less than 0.15 and simulated well the shifting shape, from crescentic sand bar to alongshore uniform sand bars in the surf zone of Maengbang beach after storm impact. However, the erosion and accretion patterns nearby in the surf zone and shoreline remain challenges in the XBeach model.

1. Introduction

Morphological changes in nearshore areas result from complex, nonlinear processes variable by the interaction of waves, sediment transport, ocean current, and water depth. Additionally, coastal phenomena of various scales occur because of spatiotemporal variability, making it difficult to accurately identify the behavior of various morphological changes in nearshore areas up until now. Because of these complex nearshore processes, modeling morphological changes involves uncertainties caused by boundary conditions, input data, model calculation equations, and parameters (Baquerizo and Losada, 2008). Thus, quantitative evaluation toward predicting uncertainties is essential for accurate numerical modeling of morphological changes in nearshore areas.

XBeach (Roelvink et al., 2009), an open-source numerical model for morphological changes in nearshore areas developed by Deltares in the Netherlands, is widely used because it can simulate morphological changes by combining with various fluid dynamic and morphodynamic processes based on physical phenomena. However, the XBeach model was initially developed to simulate a collapse in a barrier island located on the coast of the States of Louisiana and Texas because of Hurricane Katrina. Previous studies have indicated the issue of erosion induced by excessive prediction of sand movement in the open sea direction under storm conditions where the default settings of the parameters are typical (Do et al., 2018; Elsayed and Oumeraci, 2017; McCall et al., 2010). Furthermore, parameterization processes are included to represent nonlinear processes not yet completely identified, which essentially requires the calibration of parameters to enhance the model's accuracy under various sea and wave conditions. For the calibration of parameters in XBeach, the use of a new empirical constant setting based on experimental and field data (Do et al., 2018; Jin et al., 2021), the proposal of a new empirical formula, and the introduction of parameters based on physical phenomena (De Vet, 2014; Elsayed and Oumeraci, 2017; McCall et al., 2010) have been attempted. However, the trial-and-error method has been widely used as a typical calibration method. Thus, the trial-and-error method is a passive calibration method based on physical phenomena.
method performed on the basis of the research experience and knowledge of model users, and it is difficult to comprehensively consider a range of parameters. As such, a combination of parameters proposed by the trial-and-error method cannot be applied if it is out of the target sea area. It also connotes the risk of the user’s subjectivity being inherent (Ruessink, 2006; Simmons et al., 2017). To overcome this obstacle, Simmons et al. (2017) combined XBeach with the generalized likelihood uncertainty estimation (GLUE), which has been used in other research fields for systematic calibration and parametric objectivity, to conduct a study on estimating new parameters and assessing uncertainties.

The GLUE technique is known as a systematic parameter calibration method that can remedy the drawback that the user’s subjectivity could be inherent when selecting parameters through trial-and-error. The basic premise is that GLUE is performed on the basis of equifinality, which can derive the same level of prediction performance at the various input conditions caused by nonlinear interaction and uncertainties when modeling is conducted to reproduce a specific phenomenon (Beven, 2006). Afterward, a combination of numerous parameters created by Monte Carlo sampling is numerically simulated, thereby selecting the optimal combination of the parameters based on the likelihood calculated by quantifying the accuracy of the simulated results and removing various uncertainties. Simmons et al. (2017) applied the GLUE technique to Lido Di Classe in Italy as a study sea, in which the calibration of XBeach had already been conducted using a trial-and-error method by Harley et al. (2016), to select the optimal combination of parameters and compared their result with that of Harley et al. (2016). Thus, they discovered that GLUE required more numerical simulation cases and took more time to calculate than the trial-and-error method did, but the prediction performance was successfully improved by objectively considering a wide range of parameters. They also suggested the changing sensitivity of XBeach parameters according to the nearshore morphological change (overwash or collision regime) via the storm effect defined by Sallenger (2000) using the GLUE technique. Simmons et al. (2019) selected three nearby beaches, including Narrabeen beach in Sydney, Australia, as a study area and quantified and calibrated the one-dimensional (1D) simulation results through GLUE. In doing so, they successfully simulated beach erosion after a storm in Narrabeen, and XBeach showed a significant improvement in prediction performance when calibrating parameters using one or more storm event datasets, suggesting the importance of calibration for numerical simulation. They also generalized the optimal parameters of the three beaches through GLUE and proposed different parameter values for each beach, reported as an effect of beach characteristics.

As described above, previous studies demonstrated successful one-dimensional (1D) numerical simulation results when applying GLUE to XBeach. However, for regions where morphological changes in the alongshore direction are prominent, such as the eastern coast of Korea, where a crescentic sand bar was developed, it is essential to have a numerical simulation incorporating two-dimensional (2D) coastal processes, such as cross-shore sediment transport along with alongshore sediment transport and sediment transport by a nearshore current. Thus, this study verified the applicability of GLUE to 2D XBeach simulation results, not studied much until now, proposed an optimal range of parameters, and analyzed uncertainties.

2. Study Methodology and Field Data Analysis

2.1 Introduction of the XBeach Model and GLUE

XBeach has been widely used to simulate hydraulic characteristics and the morphodynamic response of nearshore areas, such as dunes and sandy beaches, during storms in various coastal engineering fields. XBeach is a 2D numerical model that calculates wave propagation and morphological changes by combining fluid dynamics and morphodynamic processes and with high accuracy when modeling is conducted to reproduce specific phenomena. It has the advantage of simulating infragravity waves using a nonlinear shallow water equation under the surf-beat mode. However, nonlinear processes are characterized by inherent numerous free parameters because of inadequate understanding of breaking waves, sediment transport, etc., which are represented through parameterization in XBeach (Bolle et al., 2010; McCall et al., 2010; Roelvink et al. 2009). It also has a limitation in that the default parameter setting in the model excessively assesses sediment transport in the open sea direction and induces excessive erosion under general storm conditions because it was designed to simulate a barrier island collapse caused by Hurricane Katrina at the time of the development. To overcome this roadblock, calibration of parameters is essentially required, and several studies have been conducted on calibration.

Generally, XBeach is calibrated through a passive trial-and-error method considering only partial parameters based on the researcher’s experience and prior studies. However, this study employed GLUE as a new parameter calibration technique. GLUE is a departure from a passive calibration method that considers only a single parameter at a time, as is done in a trial-and-error method, but instead selects a range of parameters when conducting a calibration to produce many combinations through Monte Carlo sampling. Thereby, it takes recourse to an active calibration method to conduct numerical simulations and quantification systematically and sequentially. As GLUE conducts numerical simulations of many combinations, it relatively takes a great deal of simulation time, but it has the advantage of increasing the reliability of the final proposed parameters and quantitatively analyzing uncertainties inherent to numerical models.

2.2 Seas, the Subject of Research

The subject area of this study is Maengbang beach, located in Samcheok, Gangwon-do, which is approximately a 4-km-long stretch, from the southwestern to northeastern direction. It is a quasi-linear beach, where Osipcheon River is located in the north and Maeupcheon River in the south near Samcheok Port, which is downstream of Osipcheon River, and Deokbongsan Mountain, which is downstream...
of Maeupcheon River. The foreshore slope of Maengbang beach is a range of 8°–15°, but the average slope on the sea floor is 1.4°–2°, which is relatively stable. The average beach width in the longitudinal direction of the beach is 52.48 m, and the minimum and maximum beach widths are 34.9 m and 135.5 m, respectively, with a difference of approximately 100 m. Because of this, arc-shaped beach cusps of various sizes are found onshore. A crescentic sandbar is also continuously maintained, a typical feature of the eastern coast of South Korea in the water. Jin et al. (2020) reported a correlation between the alongshore variability of beach width and the location of the crescentic sandbar. The mean tide range is approximately 0.2 m, indicating a low tide environment. Thus, the effect of the tide is relatively minimal, but waves are dominant, with high risk due to high waves. In addition, the erosion grade evaluation conducted on the current status of coastal erosion by the Ministry of Oceans and Fisheries revealed that all sections in Maengbang beach were C (concerned erosion) or D (serious erosion) from 2016 to 2019. This was adjudged considering the increasing trend of high waves caused by climate change, the reduction in the buffer zone, and the increase in reflected waves because of the shore protection built at the rear. Fig. 1 schematizes the geographical location of Maengbang beach and the grid area used in XBeach modeling.

2.3 Wave Observation Data

Wave observation was conducted employing acoustic waves and a current profiler (AWAC, Nortek) installed in the open sea around Maengbang beach to acquire a water level change every 60 min with an interval of 1 s. The AWAC was installed at a depth of approximately 30 m in the observation location (W1, latitude: 37°24' 11.22" N, longitude: 129°13' 34.56" E), and observations have been conducted constantly since February 2017. Fig. 2 schematizes the time-series wave data (significant wave height, peak wave period, and peak wave direction) in Maengbang beach in 2019 when storm scenarios occurred as well as a wave rose diagram (significant wave height and mean wave direction). The wave data for some sections around January were missing. Other than that, complete data were obtained. As shown in Fig. 2, incident waves, the significant wave height of which was less than 1.0 m, found in Maengbang beach in 2019 accounted for approximately 71% of all waves, but high waves above 2.0 m explained only 5.4%. Approximately 82% of the peak wave period was less than 8 s, and approximately 70.5% of the peak wave directions were less than 77.5°. Most average wave directions of incident waves were found in the NE section, nearly perpendicular to the shoreline of Maengbang Beach. This was due to the effect of wind direction being similar to wave direction and the effect of wind drift distance according to the geographical feature of the eastern coast of Korea, stretching northeast (Cho and Kim, 2019).

Furthermore, the features of wave direction were distinctive according to the season. Therefore, overall, the average wave direction was 45°–90° in summer and 0°–45° in winter, which showed a fluctuation. High waves, the significant wave height of which was more than 3 m, were concentrated from September to December.

**Fig. 1** Geographical characteristic and modeling grid of Maengbang beach; (a) Regional map of Samcheok in South Korea with Maengbang beach, (b) Satellite photograph of Maengbang beach with the location of wave gauge (AWAC), (c) Model domain extent and grid of the XBeach model (Google, 2020).
Wave characteristics in Maengbang beach

Fig. 2 Wave characteristic in Maengbang beach: (a) Time-series of significant wave height ($H_{\text{m0}}$), peak period, and peak wave direction at W1 (red line: peak time of significant wave height for four typhoons that affected Maengbang beach); (b) Wave of significant wave height and mean wave direction rose in W1 with the mean angle of coastline.

Table 1 Summary of wave data during each typhoon from August 29 to October 30 in 2019

<table>
<thead>
<tr>
<th>Typhoon event</th>
<th>Lingling</th>
<th>Tapah</th>
<th>Mitag</th>
<th>Hagibis</th>
</tr>
</thead>
<tbody>
<tr>
<td>Peak date</td>
<td>2019.09.07 14:00</td>
<td>2019.09.23 08:00</td>
<td>2019.10.05 12:00</td>
<td>2019.10.12 22:00</td>
</tr>
<tr>
<td>$H_{\text{m0}}$ (m)</td>
<td>1.7</td>
<td>4.5</td>
<td>3.8</td>
<td>4.2</td>
</tr>
<tr>
<td>$T_{p}$ (s)</td>
<td>7.2</td>
<td>10.2</td>
<td>10.7</td>
<td>10.5</td>
</tr>
<tr>
<td>$D_{p}$ (º)</td>
<td>98.3</td>
<td>69.1</td>
<td>42.1</td>
<td>41.0</td>
</tr>
<tr>
<td>$D_{\alpha}$ (º)</td>
<td>102.2</td>
<td>64.3</td>
<td>41.0</td>
<td>51.0</td>
</tr>
</tbody>
</table>

Fig. 3 Observed wave data between August 29 and October 30 in 2019 (significant wave, peak period, peak direction) at AWAC (W1) (color boxes: each period affected by the typhoon).

because of typhoons in summer and swell waves in winter.

Four typhoons (Lingling, Tapah, Mitag, and Hagibis), in particular, had direct or indirect impacts consecutively on the Korean Peninsula for around two months, through September 2019 and October 2019. Typhoon Lingling, the first typhoon in that period, affected the beach indirectly as it was going north to the West Sea; its significant wave...
height at the peak was only 1.7 m, the peak wave period was 10.2 s, and the peak wave direction was E, close to 90°. The most significant wave height during the four typhoons was 4.52 m at the time of Typhoon Tapah, the peak wave period was 10.2 s, and the peak wave direction was 69°. Typhoon Mitag was the only typhoon that passed directly through Maengbang beach. The significant wave height at the peak was 3.8 m, the peak wave period was 10.7 s, which was the highest value, and the peak wave direction was 42°, nearly perpendicular to the shoreline. The significant wave height during Typhoon Hagibis was 4.2 m, the second largest, the peak wave period was 10.5 s, and the peak wave direction was 53.4°. Table 1 summarizes the wave observation data at the time of the most significant wave height during the four typhoons. Fig. 3 shows the wave data schematized from August 29 to October 30 in 2019, the storm strike period, used as input data in XBeach in this study.

2.4 Bed Elevation Data Before and After the Storms

The bed elevation was surveyed from Hanjaemit Beach to Deoksan Port at intervals of 50 m up to -25 m in the open sea, conducted eight times in 2019 to observe bed elevation and beach profile. The bed elevation was measured using an observation ship equipped with a global navigation satellite system (GNSS) and the AquaRuler 200 Series, a precision echo sounder. The beach profile survey was conducted up to the upper beach profile +6 m area using a real-time kinematic GNSS (RTK-GNSS), a virtual reference station method. The reliability of the observation data improved through comparison and calibration by overlapping some observation sections of the beach profile and bed elevation survey. Morphological changes were rarely observed outside the observed bed of -10 m. Thus, it was judged that the depth of closure of sand movement was in the vicinity. Although crescentic sandbars were repeatedly generated and removed within the littoral cell of Maengbang beach, the location of the sandbars was continuously maintained without significant change. However, as the sand in the surf zone moved in the open sea direction after the typhoon scenario that occurred in the summer of 2019, the curved shape of the crescentic sandbar changed into a longshore sandbar, stretched straight in parallel with the shoreline, and patterns of erosion and accretion appeared alternately in the shoreline. Figs. 4(a) and 4(b) show the bed elevation of Maengbang beach and formed crescentic and longshore sandbars on August 29 and October 30 in 2019, before and after the storm scenarios, respectively. Fig. 4(c) shows the closure depth of sediment transport and elevation difference before and after the storms. This study employed bed elevation data before and after the storms as input and calibration data to numerically model the erosion in the subaerial beach and the morphological changes in the dynamic surf zone due to the four continuous typhoons using XBeach.
reducing simulation time as much as possible when setting the model. The 2D Joint North Sea Wave Project (JONSWAP) spectrum was used on the basis of wave data provided by the AWAC mentioned in Section 2.3 to numerically simulate a wave-induced current as the offshore wave boundary condition, and only wave data with significant wave height greater than 2 m were used so as to reduce the simulation time. To consider the bed elevation caused by tide and storm surge, data from the Donghae Port Tide Station, the closest to the study area and provided by the Korea Hydrographic and Oceanographic Agency (KHOA), were used as the offshore water level boundary condition. The wave observation location (W1) in the AWAC was positioned around the edge of the open sea direction of the grid, as shown in Fig. 1(c), to avoid deforming the initial conditions of the waves within the model. The grid resolution was set to have a distance in the cross-shore direction narrowly in the concerned area (surf and swash zones), where morphological changes occurred relatively frequently, whereas the distance was made wider in the direction of the un Concerned area (open sea), where morphological changes rarely occurred, to reduce the simulation time. Curvilinear and non-equidistance grids (192 × 239 grids in cross-shore and alongshore directions) were created (Table 2). Additionally, a non-erodible layer option was applied to Deokbongsan or coastal roads, with the sediment thickness mentioned in a previous study that had a significant impact on simulation results (Do and Yoo, 2020).

To simulate offshore fluid dynamic and morphodynamic processes during a storm, the currently widely used and proven surf-beat mode was used among many modes in XBeach that showed a difference in the hydraulic analysis of wave propagation. The surf-beat mode derives radiation stress after taking the offshore boundary conditions of the waves within the model. The grid resolution was set to have a distance in the cross-shore direction narrowly in the concerned area (surf and swash zones), where morphological changes occurred relatively frequently, whereas the distance was made wider in the direction of the un Concerned area (open sea), where morphological changes rarely occurred, to reduce the simulation time. Curvilinear and non-equidistance grids (192 × 239 grids in cross-shore and alongshore directions) were created (Table 2). Additionally, a non-erodible layer option was applied to Deokbongsan or coastal roads, with the sediment thickness mentioned in a previous study that had a significant impact on simulation results (Do and Yoo, 2020).

<table>
<thead>
<tr>
<th>Grid direction</th>
<th>Concerned area (m)</th>
<th>UnConcerned area (m)</th>
<th>Number of grids</th>
</tr>
</thead>
<tbody>
<tr>
<td>Cross-shore</td>
<td>1–6</td>
<td>7–11</td>
<td>192</td>
</tr>
<tr>
<td>Alongshore</td>
<td>14–18</td>
<td>19–22</td>
<td>239</td>
</tr>
</tbody>
</table>

in the calculation to obtain distributed and parallel processing with 36 nodes to reduce simulation time in the numerical simulation.

3.2 Selection of Wave Breaking and Sediment Transport Equations

XBeach contains various model equations usable to simulate morphological changes and sediment transport in the surf and swash zones. Thus, model users can select and apply equations on the basis of topographic characteristics or environment of the study area. Maengbang beach shows significant topographic variability in both cross-shore and alongshore directions because of the erosion and accretion patterns in the crescentic sandbar and shoreline, as aforementioned. Considering the characteristics of Maengbang beach, this study focused on a wave-breaking equation, which showed significant prediction performance sensitivity to nearshore elevation change, and the sediment transport equations, which exhibited excellent prediction performance by study area terrain and environment, among many types of equations. In the default setting of the latest XBeach version (XBeachX), Roelvink's equation (Roelvink, 1993) is set for the wave-breaking equation, and the Van Thiel–Van Rijn equation (van Rijn, 2007a; van Rijn, 2007b) is set for the sediment transport equation. Roelvink's equation experimentally and probabilistically estimates wave energy dissipation by wave breaking using Eqs. (1)–(3) (Roelvink, 1993). Here, the wave-breaking probability in Roelvink’s equation is an empirical formula type combined with the wave breaking coefficient (parameter gamma) and the ratio of wave height to water depth.

$$D_w = 2\frac{\alpha}{T_{wp}} Q_w E_w$$

$$Q_w = 1 - e^{-\left(\frac{H_{max}}{H_{max}}\right)^\gamma}$$

$$H_{max} = \gamma (b + 6H_{max})$$

Here, $D_w$ indicates the dissipation because of wave breaking, $\alpha$ indicates the wave dissipation coefficient, $Q_w$ indicates the wave breaking probability, $E_w$ indicates the wave energy, $T_{wp}$ indicates the represented wave period, $H_{max}$ indicates the RMS (Root-mean-square) wave height, $H_{max}$ indicates the peak wave height, and $\gamma$ indicates the breaker index (gamma).

However, Daly et al. (2012) highlighted that Roelvink's equation underestimated the dissipation due to wave breaking in complex terrain with rapidly increasing water depth. To overcome this limitation, Daly’s equation, the same as Roelvink's equation for others but determines whether wave breaking occurs by a binary value in the wave-breaking probability calculation, introduces an advective-deterministic approach whereby a wave-breaking characteristic advects into a wave speed, as presented in Eq. (4). Through this equation, wave breaking can be calculated in an on off manner based on wave height even in a terrain where the water depth changes.
significantly.

\[
Q_t = \begin{cases} 
1 & \text{if } H_{rms} > \gamma_h \\
0 & \text{if } H_{rms} < \gamma_h 
\end{cases}
\]

Here, \( \gamma_h \) represents the threshold that determines whether wave breaking of irregular waves stops, which corresponds to parameter \( gamma_2 \) in XBeach. Through various experimental data, Daly verified that Daly’s equation significantly improved wave-breaking prediction performance in a terrain where the water depth rapidly increased. On this basis, we determined that Daly’s equation was appropriate for Maengbang beach, regarded as complex water-depth terrain due to the continuous development of the crescentic sandbar. However, Roelvink’s equation, in contrast with Daly’s equation, taking more time in the parameter calibration process.

Next, XBeach calculates sediment concentration using the water depth-averaged advection-diffusion equation (Eq. (5)) to simulate sediment transport (Galappatti and Vreugdenhil, 1985).

\[
\frac{\partial hC}{\partial t} + \frac{\partial hC^E}{\partial x} + \frac{\partial hC^D}{\partial y} = D_h \frac{\partial^2 C}{\partial x^2} + \frac{\partial}{\partial y} \left[ D_h \frac{\partial C}{\partial y} \right] = \frac{hC_{eq} - hC}{T_s}
\]

(5)

Here, \( C \) indicates the instantaneous concentration of the depth-averaged sediment, \( D_h \) indicates the diffusion of sediment, \( h \) indicates the water depth, \( u^E \) and \( v^D \) indicate the \( x \) and \( y \) components in the Euler flow velocity, \( C_{eq} \) indicates the equilibrium concentration of sediments, and \( T_s \) indicates the reaction time due to entrainment. The entrainment and sedimentation of sediments are determined on the basis of the difference between sediment instantaneous concentration \( C \) and sediment equilibrium concentration \( (C_{eq}) \), which becomes a source term of the sediment transport equation.

\[
C_{eq} = \max \left\{ \frac{1}{2}C_{max} + \min \left\{ C_{eq-2}, \frac{1}{2}C_{max} \right\}, 0 \right\}
\]

(6)

Here, \( C_{max} \) represents the threshold of the maximum sediment concentration a user can set. The equilibrium concentration components of bed load and suspended load are needed to calculate the sediment equilibrium concentration (Eq. (6)). This equation can select the van Thiel–van Rijn equation, the default equation of the model, or the Soulsby equation (Soulsby, 1997; van Rijn, 2007c) according to the study sea. First, the van Thiel–van Rijn equation is as follows:

\[
C_{eq-b} = \frac{A_{bh}}{h} \left( \sqrt{v_{mq}} + 0.018 \frac{u_{rms,2}^2}{\epsilon_d} - U_{cr} \right)^{2.4}
\]

(7)

\[
C_{eq-s} = \frac{A_{sh}}{h} \left( \sqrt{v_{mq}} + 0.018 \frac{u_{rms,2}^2}{\epsilon_d} - U_{cr} \right)^{4.4}
\]

(8)

Here, \( A_{bh} \) and \( A_{sh} \) indicate the bed and suspended load coefficients, \( v_{mq} \) indicates the Euler flow velocity, \( u_{rms,2} \) indicates the empirical formula considering a turbulent flow due to orbital velocity and wave breaking, and \( U_{cr} \) indicates the threshold velocity of the sediment's initial movement, determined by the summation of weighted value after calculating the effects of sea current and waves separately. Additionally, the flow velocity of the sediment stirring in the sediment equilibrium concentration is a function of the infra-gravity wave, Euler's average flow velocity, and the wave's orbital velocity.

Next, the Soulsby equation considers the drag force coefficient by shear stress in the sediment stirring the velocity term, which is different from what the van Thiel-van Rijn equation says, to provide a relationship between the average flow velocity and bottom shear stress. It includes parameter \( z_i \), a length whereupon the bottom friction acts to represent the surface area whereupon the shear stress acts. Furthermore, sea current and wave are not separated when calculating \( U_{cr} \), and the Soulsby equation is as follows:

\[
C_{eq-b} = \frac{A_{bh}}{h} \left( \sqrt{v_{mq}} + 0.018 \frac{u_{rms,2}^2}{\epsilon_d} - U_{cr} \right)^{2.4}
\]

(9)

\[
C_{eq-s} = \frac{A_{sh}}{h} \left( \sqrt{v_{mq}} + 0.018 \frac{u_{rms,2}^2}{\epsilon_d} - U_{cr} \right)^{4.4}
\]

(10)

\[
C_d = \left| \frac{0.40}{\ln \left( \frac{\text{max}(h, 10z_i)}{z_i} \right)} \right|^2
\]

(11)

Here, \( C_d \) represents the drag force coefficient and \( z_i \) represents the length whereupon the bottom friction acts, represented as the parameter \( z_i \) in XBeach. Furthermore, in the two sediment transport equations (Eqs. (9) and (10)) stated above, the entrainment and sedimentation of the sediments were represented from the perspective of the initial movement threshold of single particles. These two equations have been applied to seas and researched with many characteristics in previous studies. De Vet (2014) verified that the van Thiel-van Rijn equation showed better prediction performance of suspended load equilibrium concentration in a sea where overwash or breaching occurred than that which the Soulsby equation did. However, Orzech et al. (2011) revealed that a higher Brier skill score (BSS) was obtained when using the Soulsby equation of XBeach 2D modeling for beaches in Monterey Bay, USA, with rip channel and waveform shoreline (Megacusp) being features similar to those found on Maengbang beach. Pender and Karunarathna (2013) accurately reproduced the observation results through 1D modeling using the Soulsby equation during the storm at Narrabeen beach in Australia, a straight shore with a low tidal range environment, similar to the conditions prevalent on Maengbang beach. These previous studies implied that the sediment transport equation that showed the optimal performance differed depending on sea characteristics and environments. In our study, the Soulsby equation was selected as the
sediment transport equation because it showed better prediction performance in conditions similar to those of Maengbang beach.

3.3 Selection and Rationale of the Calibration Parameters in the Numerical Model

In this study, resistance change according to changes in the dilatancy (pore volume) and bdslpeffdir (sediment transport direction adjustment) options were used out of the options in the experimental equation applied in De Vet (2014) to consider a process based on actual physical phenomena. Furthermore, dilatancy is known to improve model performance and mitigate excessive erosion in high flow velocity, considering the fluid pressure exerted on the inside of the sand because of changes in pore volume (De Vet, 2014; van Rhee, 2010). According to Talmon et al. (1995), bdslpeffdir applies a correlation in the sediment transport direction based on a bottom slope. Additionally, for the Chezy bottom friction coefficient, which improved the prediction performance of morphological change in the subaerial beach as it was known to reduce erosion when its value increased, this study used 40 m\(^{1/2}\)/s, which showed a performance improvement when applied to Maengbang beach in a study by Jin et al. (2020), instead of using the model's default value (55 m\(^{1/2}\)/s) (De Vet, 2014; Jin et al., 2020). Furthermore, for the median grain size (D50) in the XBeach model, assumed to be a single value in the model although a fine-grained distribution is found in the real, natural beach to the open sea, Jin et al. (2020) determined that the average value of the entire median grain size showed a better performance than the average value of the median grain size around the sea level in Maengbang beach did. Thus, the average median grain size of 0.4 mm of the entire water depth was used on the basis of analysis results of seabed conditions in Maengbang beach on August 29, 2019, where the initial water depth was observed before the storm strike. At the same time, although the morphological acceleration factor (morfac) showed an effect of highly reducing a simulation time in many studies on XBeach for reducing simulation time, this study used morfac = 10, known to have low sensitivity to modeling results (Lindemer et al., 2010; Simmons et al., 2019; Vousdoukas et al., 2012).

Next, sensitivity and uncertainties were precisely analyzed through the systematic calibration of GLUE, and parameters that were expected to significantly improve in model performance were selected when choosing optimal parameters. As mentioned above, Maengbang beach is a region with significant alongshore variability in both the subaerial beach and underwater. The Daly equation, which improved the wave-breaking prediction in the sudden drop in water depth, is considered very important in improving prediction performance in nearshore modeling on the entire eastern coast of South Korea, including Maengbang beach, where the water depth rapidly changes due to the development of a crescentic sandbar. The parameter \(\gamma_2\) is a threshold that determines when the breaking of irregular waves starts in the Daly equation. It is known that as the parameter value increases, dissipation due to wave breaking decreases. It has also been used frequently in several previous studies as a calibration parameter to improve morphological changes due to wave breaking (Daly et al., 2012; Harley et al., 2016; Jin et al., 2020; Kalligeris et al., 2020; Williams et al., 2012). Parameter \(\gamma_2\) is also a threshold that determines when the breaking of irregular waves stops, thought to significantly contribute to the wave breaking prediction. However, few studies have been conducted on the sensitivity of model performance according to calibration when compared with \(\gamma_2\). Thus, we

<table>
<thead>
<tr>
<th>Calibration parameter</th>
<th>Description</th>
<th>Setting or range at initial phase (Default value)</th>
<th>Setting or range at subsequent phase (Default value)</th>
</tr>
</thead>
<tbody>
<tr>
<td><strong>break</strong> (Wave breaking formula)</td>
<td>Types of breaking formula</td>
<td>roelvink _daly (roelvink2)</td>
<td>roelvink _daly (roelvink2)</td>
</tr>
<tr>
<td><strong>form</strong> (Sediment transport formula)</td>
<td>Types of equilibrium sediment concentration formula</td>
<td>soulsby _vanrijn (vanthiel_vanrijn)</td>
<td>soulsby _vanrijn (vanthiel_vanrijn)</td>
</tr>
<tr>
<td>Dilatancy</td>
<td>Switch to apply erosion inhibition depending on the pore volume change</td>
<td>1</td>
<td>1</td>
</tr>
<tr>
<td></td>
<td>(0)</td>
<td>(0)</td>
<td></td>
</tr>
<tr>
<td>Bdslpeffdir</td>
<td>Switch to apply direction of the sediment transport depending on bed slope based on Talmon et al. (1995)</td>
<td>1</td>
<td>1</td>
</tr>
<tr>
<td></td>
<td>(0)</td>
<td>(0)</td>
<td></td>
</tr>
<tr>
<td>Chezy coefficient</td>
<td>Bed friction coefficient of Chezy formula (m(^{1/2})/s)</td>
<td>40</td>
<td>40</td>
</tr>
<tr>
<td></td>
<td>(55)</td>
<td>(55)</td>
<td></td>
</tr>
<tr>
<td>D50</td>
<td>Median grain diameter of sediment (mm)</td>
<td>0.4</td>
<td>0.4</td>
</tr>
<tr>
<td></td>
<td>(1)</td>
<td>(1)</td>
<td></td>
</tr>
<tr>
<td>Morfac</td>
<td>Morphological acceleration factor</td>
<td>10</td>
<td>10</td>
</tr>
<tr>
<td>Gamma</td>
<td>Threshold to determine the start point of wave breaking in Daly formula</td>
<td>0.4–0.9</td>
<td>0.4–0.68</td>
</tr>
<tr>
<td></td>
<td>(0.55)</td>
<td>(0.55)</td>
<td></td>
</tr>
<tr>
<td>Facua</td>
<td>Parameter to determine the wave skewness and asymmetry in time-averaged flow</td>
<td>0–1</td>
<td>0.1–0.3</td>
</tr>
<tr>
<td></td>
<td>(0.1)</td>
<td>(0.1)</td>
<td></td>
</tr>
<tr>
<td>gamma2</td>
<td>Threshold to determine the stop point of wave breaking in Daly formula</td>
<td>0–0.4</td>
<td>0–0.2</td>
</tr>
<tr>
<td></td>
<td>(0.3)</td>
<td>(0.3)</td>
<td></td>
</tr>
</tbody>
</table>
selected \( \gamma_2 \) along with \( \gamma \), which determines wave breaking, as calibration parameters. Moreover, the \( \text{facua} \) parameter exhibits the effect of sediment transport due to skewness and asymmetry of waves. Generally, the larger the value the more the sediment transport in the shoreline direction.

When calibrating XBeach, \( \text{facua} \) requires a careful adjustment to improve prediction performance. And it is a parameter known to show the highest sensitivity, the reason why it has been mentioned as one of the main parameters in several previous studies (Bugajny et al., 2013; Cueto and Otero, 2020; Elsayed and Oumeraci, 2017; Harley et al., 2016; Kombiadou et al., 2021; Nederhoff et al., 2015; Orzech et al., 2011; Splinter and Palmsten, 2012; Vousdoukas et al., 2012).

Furthermore, a previous study on the adoption of GLUE in the first numerical simulation results of XBeach also selected \( \gamma \) and \( \text{facua} \) as calibration parameters to successfully improve performance. Thus, \( \gamma, \gamma_2, \) and \( \text{facua} \) were selected as the calibration parameters using GLUE. In this study, the initial parameter set of these three parameters was named the “INI set.” Table 3 summarizes the description of the used parameters and parameters to be calibrated through GLUE. Fig. 5 depicts the overall flow chart of GLUE. As shown in Fig. 5, the elements and ranges of parameters to be calibrated were first selected in the GLUE analysis, and then, parameter values were divided at regular intervals. Several parameter combinations made through this were numerically simulated, and based on the simulation results, a likelihood was calculated for each combination. Through this likelihood, sensitivity, likelihood probability density, and uncertainties were analyzed using the GLUE tool. In this study, two GLUE analyses were conducted; in the first sort phase, an entire range of parameters specified in the XBeach manual (Deltares, 2018) was considered toward selecting a precise range where prediction uncertainties were removed. In the subsequent precise phase, a range and a combination of optimal parameters that can maximize the numerical simulation performance were selected. More detailed descriptions and study results are sequentially described in Sections 4 and 5 (Fig. 5).

4. Selection of Initial Range of Parameters through GLUE

4.1 2D Calibration Method Assigned with Objectivity

The GLUE analysis overcomes the limitation of the existing trial-and-error method and assigns objectivity to calibration results. Thus, the entire range for each parameter was selected as the initial calibration area in the sort phase. Previous GLUE generated a random combination through Monte Carlo sampling when performing discretization of the set continuous range, but the current study generated a discrete combination at regular intervals to reduce the simulation time, which, otherwise, exponentially increased due to the combination of a long modeling period, 2D numerical simulation, and GLUE use. Thus, \( \gamma \) was divided into six (0.4–0.9) at an interval of 0.1, \( \text{facua} \) was divided into 21 (0–1) at an interval of 0.05, and \( \gamma_2 \) was divided into five (0–0.4) at an interval of 0.1, thereby generating 609 INI sets to perform numerical simulation 609 times. The interval of \( \text{facua} \) was more finely divided as it affected sediment transport, and its importance during modeling was emphasized in a previous study. Because of the characteristic of the thresholds that started and stopped wave breaking, \( \gamma, \gamma_2, \) and \( \gamma_2 \) could not have the same parameter value. Therefore, 21 combinations with the same value were excluded from the initial 630 combinations.

In contrast with the previous studies on GLUE that were numerically simulated with 1D, a new 2D evaluation standard is required to consider the prediction performance of 2D simulation results. However, although much advancement in the improvement of erosion prediction performance through storms through parameter calibration on XBeach has been achieved with numerical models that showed erosion-dominant features, improvements in beach recovery and sedimentation prediction are yet to be achieved (Daly, 2017; Kombiadou et al., 2021). Thus, when conducting a 2D performance evaluation, if a dominant deposition profile is included, the evaluation index value is standardized downward, resulting in difficulties in quantitative evaluation. Considering this fact, this study selected a profile where beach regression or volume erosion was dominant as the baseline beach profile (hereafter referred to as the “baseline profile”).
to be used as the evaluation standard. To comprehensively consider as many erosion profiles of many areas as possible, the baseline profile was selected so as not to let erosion profiles be adjacent to each other. Additionally, the profile erosion near No. 207 was attributed to the effect of Maeupcheon being located on the right-hand side, which was considered in that study. Thus, the profile was excluded in the selection of baseline profile. Fig. 6 shows the shoreline and volume changes that occurred on Maengbang beach before and after the typhoon scenarios. The four baseline profiles in the yellow lines were used in the first GLUE analysis. Subsequently, the three profiles in purple lines were added in the second GLUE analysis. Thus, seven baseline profiles were used as explained in Section 5. Furthermore, to consider the erosion in the subaerial beach, the importance of which has been stressed from the perspective of coastal disaster prevention, the swash zone (water depth of 0–4 m) was selected as the water depth at which to conduct performance evaluation.

BSS is an evaluation index used to closely review and evaluate a cross-sectional change in the prediction model proposed by van Rijn et al. (2003). It has been widely used as a significant quantitative standard for the prediction performance of numerical models in the field of coastal engineering. It is calculated by Eq. (12). In this study, the BSS shows how good the prediction performance of water depth after the storm is in comparison with the observed water depth after the storm based on the observed initial water depth as the evaluation standard.

\[ BSS = 1 - \frac{\sum (z_m - z_p)^2}{\sum (z_m - z_b)^2} \]  

where, \( z_m \) indicates the observed water depth after the storm, \( z_p \) indicates the water depth result after the numerically simulated storm, \( z_b \) indicates the water depth observed before the storm, which becomes the baseline water depth, \( t \) indicates the number of numerical simulations, and \( n \) (or \( N \)) indicates the number of baseline profiles. BSS has a value between 0 and 1. The closer the value to 1 the better the prediction performance. And the closer the value to 0 the worse the performance. Table 4 presents the model performance according to the BSS values proposed in van Rijn et al. (2003).

As many simulation results must be processed due to the GLUE’s characteristics, it is necessary to have a certain quantitative standard to determine significant simulation results. Thus, a BSS threshold can be set based on the modeling objective and the model performance as
measured by a BSS value. Based on this threshold, behavior and non-behavior were defined toward determining whether the simulation result of the combination showed a significant prediction. In this study, the behavior was defined as a combination, the average BSS value of which was derived by averaging BSSs in the baseline profiles for each parameter combination, was more than 0.3 (Eq. (13)).

$$\text{trial}_{i} = \text{behaviouralrun}, \frac{\sum_{i=1}^{n} \text{BSS}_{i}}{N} \geq 0.3$$

$$\text{trial}_{i} = \text{non-behaviouralrun}, \frac{\sum_{i=1}^{n} \text{BSS}_{i}}{N} < 0.3$$

Fig. 7 shows the average BSS values of the baseline profiles for each combination derived through the aforementioned process in the sort phase of GLUE. In this figure, combinations showing behavior were indicated in blue, combinations showing non-behavior in yellow, and combinations showing the maximum value in red. As shown in Fig. 7, unreasonable and low performances were mostly shown in the entire range of the parameters. Only some of the combinations showed a significant result, a behavior rate of approximately 10.18% (62 out of 609 profiles) because of the high sensitivity of model performance of facua in a certain limited range. The combination showing the maximum mean BSS was gamma = 0.9, facua = 0.1, and gamma2 = 0.1, which was 0.8429 of mean BSS. Other various combinations were also close to 0.84 of mean BSS. This implies that the equifinality displayed in a complex XBeach model was well identified through GLUE.

Afterward, to exclude the identified equifinality, a weighted likelihood calculated on the basis of BSS value was assigned to each combination and used as the standard of quantitative evaluation. A likelihood refers to the possibility that the used combination of parameters produces the optimal result when numerically simulating an observed event. Here, the likelihoods of all combinations of non-behavior defined above were set to 0, and the likelihoods of only the combinations of behavior were calculated using Eq. (14).

$$\text{Likelihood}_{\text{BSS}} = \frac{\sum_{i=1}^{n} \text{BSS}_{i}}{\sum_{i=1}^{n} \text{BSS}_{i}}$$

Here, n indicates the number of parameter combinations showing behavior, and BSS$_i$ indicates each BSS value of the combination showing behavior out of all parameter combinations. Based on this equation, likelihood indicates a proportion of one BSS value out of all BSS values. Thus, the likelihood of the combination will be small if the sum of BSS values derived in all combinations is large, even if the BSS value is derived high from a specific parameter combination, and the likelihood will be large if the sum of the derived BSS values is small. As such, if a likelihood is utilized, relative prediction performance can be considered to exclude equifinality. And it can be an evaluation index that can select a more significant combination out of all parameter combinations in a relative sense.

### 4.2 Selection of Likelihood-based Precision Range to Improve Prediction Reliability

Generalized sensitivity analysis (GSA), conducted through likelihoods assigned to the combinations of parameters, is a method for quantifying and evaluating the extent to which change in each parameter can partially affect the simulation result. In the GLUE, the GSA technique of Hornberger and Spear (1981) is extended to provide a ranking of sensitivity for each combination based on likelihood. This sensitivity analysis was performed using the difference between cumulative density function (CDF) and prior distribution function. Beven and Binley (1992) pointed out that setting a prior distribution, compared with CDF in the sensitivity analysis, with a wide range and uniform distribution function was suitable as the standard reference. Thus, this study assumed the prior distribution function with a continuous uniform distribution for all ranges of the parameters.
Additionally, the Kolmogorov–Smirnov D statistic (K-SD) was used to quantify the difference between the CDF and continuous uniform distribution function (Thorndahl et al., 2008). K-SD shows the maximum value of the differences between the two functions. In the GSA, the closer the K-SD to 0 the smaller the sensitivity of the parameter to the simulation result, and the closer the K-SD to 1 the larger the sensitivity. The cumulative likelihood of each parameter value was diagrammed using a probability density function (PDF) to refine a range of parameters that showed significant performance in the study sea. The simulated result through the combination, including the parameter value, showed a more relatively significant prediction performance as the PDF value increased. Using this PDF and the relativity of likelihood, a range of parameters that can improve prediction performance and reduce uncertainties can be objectively selected.

Fig. 8 shows the diagrams of CDF and PDF of the INI set, analyzed through the above description. As shown in Fig. 8, in the GLUE sort phase, the highest sensitivity was shown as K-SD = 0.86254 when facua was 0.1, K-SD = 0.23176 when gamma2 was 0, and K-SD = 0.10468 when gamma was 0.4. Although the sensitivities of gamma and gamma2 tended to decrease as the parameter value increased, likelihoods showed relatively uniform accumulation. However, the sensitivity of facua rapidly increased in a low parameter value so that combinations using values greater than 0.15 showed non-behavior that did not show a significant prediction performance as their cumulative likelihood value was 0. Additionally, the maximum PDF value was shown when facua was 0.05 and the cumulative BSS value was 15.0567, showing a sole trend of PDF concentration in a limited range (0–0.15). The maximum PDF value was exhibited when gamma was 0.7, and the cumulative BSS value was 7.6553. The maximum PDF value was exhibited when gamma2 was 0.1, and the cumulative BSS value was 9.2277, showing a PDF shape distributed over all ranges. As the maximum PDF value was shown differently depending on the number of first discrete parameter values, direct comparison between gamma and gamma2 was limited (gamma: six discrete values, gamma2: five discrete values). However, the maximum PDF value of facua was higher even when the number of discrete numbers was approximately four times greater than that of the two parameters mentioned above (facua: 21 values). This implies that calibrating at fine intervals of facua is critical when modeling the region, and the excessive erosion of XBeach can be appropriately avoided by carefully adjusting the sediment transport direction via facua.

Through the sort phase of the GLUE in the foregoing discussion, sensitivities of each parameter and a range that showed behavior can be identified, and a range of non-behavior, the prediction uncertainty of which was largely owing to a low likelihood, can be excluded. Through the later GLUE analysis in the precision phase, a precision range of parameters was refined to propose the optimal combination of parameters. The range of gamma was refined to 0.4–0.68 at 0.04 intervals as the parameter values, where the K-SD and maximum PDF values derived were 0.4 and 0.7, respectively, located relatively far from each other. As the values where K-SD and the maximum PDF value were derived in facua were relatively close, the precision range of facua was set to 0.05–0.3 at 0.025 intervals. For gamma2, the precision range was refined to 0–0.2 at 0.04 intervals. A total of 528 refined parameter sets were named “RE set” in this study, and numerical simulations were performed 528 times again.
5. Proposal of Optimal Range and Combination with the Precision Range through GLUE

5.1 Calculation of Optimal Range Based on Likelihood to Improve Prediction Performance

To identify the outline of the combination of significant performance and applicability of GLUE on 2D simulation results of Maengbang beach in the sort phase, four baseline profiles (Profile 56, 99, 123, and 163) were adopted as the evaluation standard for the erosion-dominant characteristics of the XBeach model. In the precision phase of GLUE, three profiles (Profile 82, 109, and 145) were added to the existing baseline profiles, as shown in Fig. 6, to improve the alongshore evaluation performance, and the swash zone (0–4 m of water depth) was maintained as the evaluation water depth. Thus, although non-behavior showed a higher appearance rate overall, 161 out of 528 parameter sets showed behavior (BSS ≥ 0.3), which showed approximately 30.49% of behavior rate. This was a significantly improved behavior rate when compared with approximately 10.18% in the sort phase, which suggests that the range of behavior sets can be refined through repetitive GLUE analysis. The maximum BSS value was exhibited out of all sets when gamma = 0.64, facua = 0.075, and gamma2 = 0.16, and its mean BSS = 0.6891. The somewhat lower BSS compared to that of the sort phase due to the effect of the added baseline profiles. Fig. 9 schematizes the CDF and PDF in the analyzed RE set. According to Fig. 9, when facua was 0.1, K-SD = 0.7231, which showed the maximum sensitivity, and when gamma2 was 0, K-SD = 0.17992, and when gamma was 0.4, K-SD = 0.10932. All three parameters derived the maximum sensitivity from the same parameter values, and the large and small relationships were also maintained, producing the largest sensitivity in facua, followed by gamma2 and gamma. The refined precision range resolved the trend of limiting the sensitivity of facua to a specific value only to some extent compared to that of the initial range. However, it still showed a trend of sensitivity concentrated on 0.1 or lower. Thus, careful adjustment of facua is still required during calibration.

When facua was 0.075, the PDF was 0.3288, significantly higher than the other two parameters. The maximum PDF value was calculated, which was higher than that of the sort phase. When gamma was 0.64 and gamma2 was 0, the maximum PDF value was revealed, indicating a tendency for a slightly lower maximum PDF value than that in the sort phase. PDF was more leveled through the repeated GLUE analysis than it was in the sort phase, but the derived maximum cumulative BSS value increased. This was due to the improved performance and reduced uncertainties by the refined precision range in Maengbang beach. This implies that if GLUE calibration is continuously repeated, the optimized range that can be universally used while minimizing uncertainties can be derived.

Furthermore, the gamma and gamma2 parameters, which produced relatively leveled PDF, represented wave breaking and will show higher sensitivities due to alongshore variability, such as a water depth by beach profile, than that shown by facua, which reflected wave asymmetry. Thus, the leveled PDF was revealed because of the larger effect of the evaluation method that averaged BSSs of all baseline profiles adopted in this study. To overcome this issue, a measure of a solution is required, in which the profile-specific calibration mentioned in Simmons et al. (2019) can also be applied to the 2D simulation results. Note that the parameters used to derive the
maximum K-SD and maximum PDF values did not necessarily match, indicating that high sensitivity did not directly correlate with the performance of simulation results. After completing the calibration, optimal performances were shown when \( \text{facua} \) was lower and \( \gamma \) was higher than the default value in the model (Fig. 9). This was due to the physical characteristics included in the parameters. For example, when \( \gamma \) increased, wave breaking increased, and wave energy dissipation decreased due to the increase in the threshold of wave-breaking generation. Therefore, wave energy reaching the shoreline increased, which increased erosion near the shoreline. However, when \( \text{facua} \) decreased, sediment transport decreased in the shoreline direction, increasing erosion near the shoreline. In this regard, \( \gamma \) and \( \text{facua} \) were calibrated in the direction that can improve the excessive erosion in XBeach, which is consistent with the findings of previous studies (Elsayed and Oumeraci, 2017; Simmons et al., 2019). Table 5 summarizes the quantitative evaluation results of sensitivities and likelihood probability analysis in the sort and precision phases of GLUE performed in Sections 4 and 5.

### 5.2 Limitation of baseline profiles and performance evaluation to select optimal combinations

The parameters can be quantitatively analyzed, and the optimal ranges of parameters that reduce uncertainties can be selected through a sequentially conducted GLUE analysis. However, this process was an evaluation method considering only some erosion-dominant profiles out of all profiles to suppress the downward leveling of quantitative evaluation results owing to the characteristics of XBeach during GLUE analysis. Hence, we are skeptical about the calibration result from the swash zone (water depth 0-4 m) of seven baseline profiles and whether it can represent the entire study sea. Thus, we conducted GLUE calibration in the precision phase one more time with the evaluation standards, considering most profiles and water depths in Maengbang beach and the baseline profiles. As a result, only one out of 528 combinations showed behavior, which was \( \gamma = 0.68, \text{facua} = 0.1 \), and \( \gamma_2 = 0.04 \). Its average BSS value was 0.321; this combination was named "COMB3." Furthermore, the optimal combination selected through seven baseline profiles in Section 5.1 was \( \gamma = 0.64, \text{facua} = 0.075 \), and \( \gamma_2 = 0.16 \). Its average BSS was 0.6891 and was named "COMB1" in this study. Next, the result that combined parameter values and that showed that the cumulative likelihood values were the highest for each parameter among the analysis results obtained in Section 5.1 was \( \gamma = 0.64, \text{facua} = 0.075, \) and \( \gamma_2 = 0.04 \). Its average BSS value was 0.5826 and was named "COMB2." Finally, to examine the entire region of Maengbang beach without using the baseline profiles, calibration evaluation standards were set to the beach profiles (Profiles 30-200) in the center of the beach and the surf and swash zones (water depth -8-4 m) to average BSS values derived in their profiles. Table 6

<table>
<thead>
<tr>
<th>Parameter name</th>
<th>Analysis type</th>
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<tr>
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<td>gamma</td>
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<td>0.1093 (0.4)</td>
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<td></td>
<td>Maximum BSS</td>
<td>7.6555 (0.7)</td>
<td>9.7825 (0.64)</td>
</tr>
<tr>
<td></td>
<td>Maximum likelihood density</td>
<td>0.2008 (0.7)</td>
<td>0.1330 (0.64)</td>
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<td>K-S D value</td>
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<td>facua</td>
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<td>24.186 (0.075)</td>
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<td>0.3288 (0.075)</td>
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<td></td>
<td>K-S D value</td>
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<td>0.1799 (0)</td>
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<tr>
<td>gamma2</td>
<td>Maximum BSS</td>
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<td>13.234 (0)</td>
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<tr>
<td></td>
<td>Maximum likelihood density</td>
<td>0.2421 (0.1)</td>
<td>0.1799 (0)</td>
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</table>

### Table 5 Calculated quantitative analysis values (K-S D value, Maximum BSS, Maximum likelihood probability density value) of simulated result in sort and precise phase. A value in the bracket is the parameter value from which the analysis value is derived.

### Table 6 Calibrated parameter and calculated maximum BSS value for optimum 2D numerical modeling result at Maengbang beach
Proposal of Parameter Range that Offered Optimal Performance in the Coastal Morphodynamic Model (XBeach) Through GLUE

summarizes the calibrated parameter combinations and performance evaluation results using GLUE.

Fig. 10 schematizes the measured morphological change before and after a series of storms on Maengbang beach and 2D numerical modeling results of three calibrated combinations, in which the solid red line indicates the baseline profile. As shown in Fig. 10, both COMB1 and COMB2 simulated the erosion at a water depth of -5~2 m in the surf zone and accretion at a water depth of -8~5 m relatively well and successfully simulated the longshore sandbar, parallel to the shoreline formed after the storm strike. However, COMB1 showed a trend of relatively overestimated erosion and accretion in the surf zone compared to the observed values, whereas the accretion at a water depth of -2~0 m was underestimated. As a result, the error of erosion and accretion in COMB2 was relatively alleviated compared to that of COMB1. However, it also revealed a limitation in accurately predicting locations of erosion and accretion compared to the observed values. The alleviation of erosion and accretion error in COMB2 compared to COMB1 was because of \( \text{gamma}2 \), which indicated a correlation between \( \text{gamma}2 \) value and erosion and accretion at the surf zone. When \( \text{gamma}2 \) increased, it generated a wave-breaking stop more quickly as the breaking wave entered from the sandbar crest into the sandbar trough, which reduced the sediment transport and erosion caused by the breaking wave at a water depth of -5~2 m. Additionally, COMB1 and COMB2 did not simulate the erosion and accretion patterns in the shoreline due to the overestimation of erosion near the shoreline as the location of the longshore sandbar formed at a water depth of -8~5 m was produced in the open sea direction, farther than the observed value. This was because erosion-dominant baseline profiles were considered the evaluation standard.

COMB3 exhibited a reduced erosion pattern near the left-hand side and central shoreline of Maengbang beach compared with COMB1 and COMB2, and the excessive erosion at a surf zone of -5~2 m, as shown in Figs. 10(b) and 10(c), was considerably alleviated. This implies that the performance evaluation using erosion-dominant baseline profiles can induce excessive erosion. Moreover, COMB2 accurately simulated the accretion in the longshore sandbar formed at a water depth of -8~5 m. Thus, the overestimated accretion at a water depth of -2~0 m, which was revealed in COMB1 and COMB2, was considerably alleviated. However, the location of the longshore sandbar at a water depth of -8~5 m was still somewhat deviated from the open sea, and the repeated erosion and accretion pattern was not simulated well although the erosion near the shoreline was alleviated. Furthermore, a considerable error was produced on the right-hand side in the simulation results of Maengbang beach because the effect of Maepucheon and sediment transport was not considered in the modeling.

In summary of the above results, when calibrating the 2D numerical modeling, the combinations of the parameters derived from the erosion-dominant baseline profiles induced excessive erosion and accretion in the surf zone and excessive erosion near the shoreline. These limitations were significantly alleviated through calibration that considered many profiles and water depths, but selecting many profiles and water depths as the evaluation standard from the start of GLUE still exposed limitations due to the downward leveling of the evaluation results caused by the characteristics of the XBeach model. Thus, the optimal range that excludes uncertainties using two GLUE analyses is first calculated through erosion-dominant baseline profiles, and then, applying many profiles and water depths when selecting the
final optimal combinations will significantly improve the calibration performance of the 2D simulation results.

6. Conclusions

We improved the unsystematic and passive drawbacks of the trial-and-error method, generally used for parameter calibration to improve the performance of XBeach, a nearshore morphological change model. We also simulated nearshore morphological changes on the eastern coast of South Korea using GLUE, a systematic calibration technique that can exclude uncertainties inherent in nearshore models. The study area was Maengbang beach, which showed typical characteristics of the eastern coast of South Korea, and four consecutive typhoons struck in 2019. Maengbang beach experienced considerable beach erosion and morphological changes in the surf zone during the storms. Additionally, the crescentic sandbar and arc-shaped beach, which are morphological features of the eastern coast of South Korea, are well developed. Thus, it is a place where a considerably complex nearshore process occurs due to the large alongshore variability and water-depth changes. This study employed a 2D JONSWAP spectrum of wave data that satisfied 2 m or higher significant wave height condition as the offshore wave boundary condition. For the offshore water level boundary condition, data from the Donghae Port Tide Station were used to numerically simulate the study area while reducing the long simulation time of GLUE. Curvilinear and non-equidistance grids (192 × 239 grids in the cross-shore and alongshore directions), whose resolution was higher in the longshore direction. The Daly equation, which improved the wave-breaking prediction performance in regions where the water depth rapidly dropped, and the Soulsby equation, which showed a good prediction performance of sediment concentration in beaches that were like Maengbang beach, were set as wave-breaking and sediment transport equations. For the parameters, dilatancy and bedsheepfdir were applied, which are options in the experimental equation based on physical phenomena that alleviate the erosion. Furthermore, Chezy = 40 m$^{1/2}$/s and D50 = 0.4 mm were used, which showed a good performance of numerical simulation of Maengbang beach in a previous study (De Vet, 2014; Jin et al., 2020). Furthermore, the morphological change acceleration factor was set as 10 to shorten the simulation time. For the systematic calibration parameters used to analyze uncertainties through GLUE, parameters gamma and gamma2, which were included in the Daly equation that considerably improved a wave-breaking prediction based on a rapid change in water depth, as shown in Maengbang beach, and parameter facua, which was widely known as the importance of sediment transport prediction in a previous study, were selected. The 609 initial combinations (INI set) of the parameters were generated in the sort phase by combining discrete parameters at regular intervals when applying GLUE, and 528 combinations of the parameters (RE set) were generated in the precision phase to conduct sensitivity and likelihood probability density analysis. We also conducted 2D numerical simulations, which were in contrast with previous GLUE studies. Thus, we needed a standard to comprehensively evaluate many profiles. Four profiles whose erosion was the most dominant were nominated as the baseline profiles and used as the evaluation standard, considering the erosion-dominant characteristics of XBeach. Four BSS values of water depth 0-4 m in the profiles were calculated and averaged. The CDF and PDF were evaluated through the likelihood calculated in the behavior whose average BSS value was 0.3 or higher, and the result in the sort phase of GLUE (the first analysis) indicated that when facua was 0.1, K-SD = 0.86254, when gamma2 was 0, K-SD = 0.23176, and when gamma was 0.4, K-SD = 0.10468 in Maengbang beach. Furthermore, facua exhibited the highest sensitivity. In contrast, gamma and gamma2 showed relatively even behavior in all ranges in terms of PDF, while facua was concentrated on 0-0.1, showing non-behavior if it exceeded 0.15. Thus, it is essential to finely adjust facua when modeling Maengbang beach, through which excessive erosion of XBeach can be alleviated. The precision range of the parameters was refined based on the CDF and PDF in the sort phase as follows: gamma was 0.4-0.68 at 0.04 intervals, facua was 0.05-0.3 at 0.025 intervals, and gamma2 was 0-0.2 at 0.04 intervals. The GLUE analysis was conducted by generating 528 combinations and numerically simulating them. Here, four baseline profiles in the sort phase were expanded to seven in the precision phase (the second analysis) to create a more valid model performance evaluation standard. As a result, in the precision phase, Maengbang beach produced K-SD = 0.7231 when facua was 0.1, K-SD = 0.17992 when gamma2 was 0, and K-SD = 0.10932 when gamma was 0.4, demonstrating that facua revealed much higher sensitivities as shown in the sort phase. PDF was relatively leveled compared to that of the sort phase, but the cumulative BSS value in the parameter value increased, implying that a range that was improved further and reduced uncertainties in Maengbang beach would be suggested through the GLUE analysis. Thus, by repeating the GLUE calibration, a range that can be universally applied to the study sea can be selected. Furthermore, gamma and gamma2 showed a more even likelihood distribution than facua. This was because gamma and gamma2 were more affected by the alongshore direction variability in Maengbang beach than facua. Therefore, when proposing universal parameters in the study sea through GLUE, the selection of gamma and gamma2 is limited, and if facua is selected in the range of 0.05-0.1, it will produce a good prediction performance in Maengbang beach. Furthermore, GLUE calibration was conducted to alleviate the excessive erosion in XBeach, yielding the maximum cumulative likelihood when facua was 0.75 and gamma was 0.64. Through these quantitative evaluations, COMB1: gamma = 0.64, facua = 0.075, and gamma2 = 0.16 (BSS = 0.6891), which finally showed the highest average BSS value, and COMB2: gamma = 0.64, facua = 0.075, and gamma2 = 0 (BSS = 5826), which combined parameter values showing the maximum cumulative likelihood in each parameter, were selected as candidates of optimal parameter combinations in Maengbang beach, thereby comparing the 2D simulation results. Furthermore, the
difference in calibration performance between using partial baseline profiles of Maengbang beach and using many continuous profiles as the evaluation standard was compared. To achieve this, the surf and swash zones (water depth of -8–4 m) in the center of the beach (profiles 30–200) were set as the evaluation standard of calibration, and COMB3: $\gamma = 0.68$, $\alpha = 0.1$, and $\gamma_2 = 0.04$ (BSS = 0.321), which solely showed behavior, were also compared. Therefore, COMB1 and COMB2 successfully formed the erosion and accretion pattern in the surf zone of Maengbang beach and the longshore sandbar after the storm. However, the erosion and accretion at a water depth of -8–2 m were relatively overestimated compared to the observed value, and the erosion at a water depth of -2–0 m was underestimated. COMB2 exhibited an alleviated error with the observed value compared to that of COMB1, which was due to the quicker wave breaking when breaking waves progressed from the sandbar crest to the trough as $\gamma_2$ increased, thereby reducing the sediment transport and the erosion in the surf zone caused by the breaking waves. COMB3 also accurately simulated the pattern of erosion and accretion, as well as the formation of the longshore sandbar on Maengbang beach. The error with the observed result, in particular, was more alleviated than that of COMB1 and COMB2. However, it still showed a limitation in producing an accurate location of the longshore sandbar and simulating an erosion and accretion pattern in the shoreline. Because using only erosion-dominant baseline profiles as the evaluation standard caused the increase in erosion in the overall simulation results, two GLUE analyses were conducted to calculate the optimal range, and finally, the optimal combination was selected. At this time, it would be more suitable to apply evaluation standards that can consider most of the profiles. This study required a considerable numerical simulation time because of the relatively long modeling period of 63 days and the use of 2D numerical simulations. Accordingly, many conditions were included to reduce simulation time, and the number of numerical simulations was smaller than that of the 1D GLUE study. Nonetheless, our study contributed to the applicability of 2D modeling using GLUE, quantitative performance evaluation of nearshore modeling on the eastern coast of South Korea, and analysis of uncertainties. If future studies apply various parameters and eastern coasts, it is considered that it can improve the reliability of nearshore modeling on the eastern coasts of South Korea through GLUE.

**Conflict of Interest**

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

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1. Introduction

Owing to the continuing increase in global energy demand, the consumption of fossil fuels, a traditional non-renewable energy resource, is also showing a steady increase. Fossil fuels are suitable as a major energy source because of their storage practicality and capability of generating a large amount of energy; however, they are also the source of growing environmental concerns owing to the excessive emission of greenhouse gases such as carbon dioxide, NOx, and SOx. Considering their limited reserves, fossil fuels are expected to deplete. In preparation, sustainable and eco-friendly energy generation methods are being sought. Among the different types of renewable energy, wave energy is a source of energy with a near-infinite potential for power generation (Roh et al., 2020). In addition, wave energy is the energy source with the highest energy density per unit area among marine energy sources (Kim et al., 2014). However, because wave energy is significantly affected by the unpredictable irregularities and seasonal variability of ocean waves, the development of advanced technology is required to ensure stable energy extraction. Power generation methods for extracting wave energy can be divided into wave-activated bodies, oscillating water columns (OWCs), and overtopping devices (Seo et al., 2020).

With OWC wave energy converters (WECs), incident waves enter a chamber, a closed space in the oscillating water column, to change the elevation, which leads to a change in the volume of the air inside the chamber. In this manner, the wave energy is first converted into airflow energy. The generated airflow rotates the air turbine installed in the nozzle at the top of the chamber; this process involves the secondary conversion of the airflow energy into mechanical rotation energy to generate electricity (Kim et al., 2007). OWC WECs have a highly reliable power generation method and ease of maintenance, as the power take-off (PTO) is located above the water surface, drawing active research interest (Boccotti, 2003; Elhanaf et al., 2016). Numerous experimental studies have been conducted on OWC development (Hong et al., 2006; Kim et al., 2012), as well as numerical analysis studies using computational fluid dynamics (CFD) (Elhanaf, 2017; Simonetti et al., 2018; Vyzikas et al., 2017). In
addition, a comparative analysis has reported on modeling an open chamber without an air turbine effect and orifice chamber modeling that considers the turbine effect (Koo et al., 2010).

In addition, by installing OWC WECs integrated with the bottom-fixed breakwater along the coast (Park et al., 2018), more generator modules can be installed, which leads to an excellent energy extraction effect and advantage in securing the installation site. However, for the breakwater-linked OWC WECs, the skirt of the chamber is inclined because of the side inclination of the breakwater (approximately 30 degrees). Considering the inclined structure, there is a high possibility of fluid viscous damping the incident wave at the end of the chamber skirt. Therefore, to increase the energy extraction efficiency of the OWC chamber, incident wave energy must be transferred into the chamber while minimizing the viscous damping of the incident wave generated at the end of the skirt (Wang and Ning, 2020).

In this study, numerical analysis and experiments were conducted on the viscous damping effect according to the shape of the OWC chamber skirt of a bottom-fixed breakwater-linked OWC WEC in coastal regions, and the two sets of the obtained results were compared. Experiments on a breakwater-linked OWC WECs model were performed using a two-dimensional mini wave tank. As the typical inclination angle of the bottom-fixed breakwater installed in the domestic coastal region is 33.7°, the inclination angle for the experimental model of the breakwater-linked OWC was fixed at 33.7°.

For the shape of the chamber skirt, two different shapes—pointed and rounded—were assumed for a comparative evaluation of the wave elevations in the chamber accordingly; the fluid viscous damping effect of the incident wave was comparatively analyzed for the two shapes. In addition, for the same experimental model, the CFD program (Star CCM+) was used to simulate the displacement of water surface with regular waves of different periods, and the results were compared with the experimental results to verify the validity of the computation. Through this study, the fluid viscous damping effect was analyzed with different OWC chamber skirt shapes, and the feasibility of a structural design capable of achieving maximum energy extraction while minimizing incident wave energy loss is discussed.

## 2. Numerical Model

### 2.1 CFD Analysis

A numerical analysis was performed using Star-CCM+ (Ver. 15.06), a commercial software based on modeling viscous fluid. The governing equations for the fluid domain are a continuity equation and the RANS (Reynolds-averaged Navier-Stokes) equation. An implicit unsteady condition is applied for time domain analysis. To simulate the multiphase reactions of continuous flows, the physical properties of two fluid phases—water and air—were defined based on the Eulerian multiphase method (EMP). The EMP solves the transport equations for mass, momentum, and energy of each phase based on the assumption that all phases share the same pressure field. The interface was defined as a free surface, and the volume of fluid (VOF) method was used to represent the displacement of the free surface. A segregated flow solver was set to model the flow, and a realizable k–ε turbulence model was applied for turbulence analysis. A linear regular wave was used as the incident waves. Table 1 lists all CFD simulation setting.

Fig. 1 presents a schematic of the computational domain and boundary conditions for a numerical model of the breakwater-linked inclined OWC. The impermeable rigid boundary conditions were applied using the wall boundary conditions for the rear wall of the OWC, seabed, skirt, and side wall (inner wall in the figure) in the 3D computational domain. To make conditions similar to the experimental conditions, a no-slip condition was applied to the fluid particles on the wall. For the outer surface of the computational domain (the outer surface in the figure), the boundary condition of an x-axis symmetric plane was applied to reduce the total number of nodes and the resulting computation time. To generate an incident wave from the wave maker on the right side of the computational domain, a velocity inlet boundary condition was set, and a forcing zone that can forcefully maintain the condition was applied at 2 m. By applying the forcing zone, the computational domain can be reduced by another solution (in this case, the velocity field of the incident wave) on the domain instead of the solution of the discretized Navier-Stokes equation.

In the experiment with a wave tank, the energy component of the evanescent mode decreases exponentially per the horizontal length

<table>
<thead>
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<th>Table 1 CFD simulation setting</th>
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<td><strong>Software</strong></td>
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<td><strong>Time domain</strong></td>
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<td><strong>Governing equation</strong></td>
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<td><strong>Free surface model</strong></td>
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<td><strong>Turbulence model</strong></td>
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![Fig. 1 Computational domain of an inclined OWC (Open chamber)](image-url)
moving away from the wave maker, and a wave with an energy component approximately 25% larger than the predicted incident wave height is generated near the wave maker. This condition is the evanescent mode of the reflected wave, and it is affected by the standing wave of the reflected wave generated by the motion of the wave maker (Kwon et al., 2019). To prevent this phenomenon in the simulation, a forcing zone was set near the piston-type wave maker. To define the interface of the nozzle outlet where the air flows out, a pressure outlet boundary condition was applied to the upper surface of the computational domain and upper surface of the rear wall of the OWC, while the static pressure condition of the external pressure was set as the pressure boundary conditions.

For the mesh spacing in the computational domain, 1/100 of the wavelength of the incident wave was applied in the direction of the wave propagation, and 1/20 of the wave height of the incident wave was applied in the direction of the wave height. A trimmer mesh suitable for modeling the wave tank was applied. To represent the wave displacement inside the OWC chamber, the mesh size was further subdivided into 1/32 in the wave propagation (horizontal) direction and 1/8 in the wave height (vertical) direction to increase the accuracy of wave elevation measurements inside the OWC chamber. The total number of meshes used in the simulation was set to 3 million.

In the experiment with a two-dimensional mini wave tank, as it is difficult for the hydrodynamic characteristics of the small-scale turbine model to follow Froude’s similarity law, the open chamber and orifice chamber conditions were applied for simulation, instead of a precise simulation of a turbine model. Comparing the results of the two chamber conditions, the effect of applying PTO was analyzed. In addition, the energy conversion performance of the incident wave energy was examined, and the phenomenon of amplification of the incident wave elevation after entering the OWC chamber was discussed. The length ($L$) of the computational domain was set to 5.62 m, as in the wave tank used in the experiment. The length of the actual tank is 6 m, but the piston-type wave maker was installed at a slight distance from the end wall of the tank, and this small separation was considered when setting the computational domain. The depth and width of the mini wave tank were 0.32 and 0.3 m, respectively. In the computational domain, a boundary condition of a symmetric x-axis (wave propagation direction) was set, and the width ($Y$) was set to 0.15 m. In addition, the height of the computational domain, including the water depth of 32 cm, was set to 50 cm, with the computation performed in three dimensions.

3. Experiment Equipment and Settings

3.1 Two-dimensional Mini Wave Tank

In this study, a two-dimensional mini wave tank of Inha University was used. The mini wave tank was 6 m in length, 0.5 m in height, and 0.3 m in width, and the bottom and sides were transparent acrylic such that waves were observable from any direction (Fig. 2). A piston-type wave maker was used. The generated waves reflect from the OWC installed on the end wall in the propagation direction and re-enter the wave maker. Therefore, the wave before the reflected wave is re-reflected from the wave maker and reaches the OWC was used for the analysis of the results. This setup excludes the influence of the reflected wave when generating the incident wave and uses only the target incident wave. The incident wave period was in the range of 0.8 to 1.2 s, with waves made at intervals of 0.1 s, and the range of wavelength was 0.98 to 1.86 m. The wave height of the incident wave was fixed at 2 cm to generate a uniform and regular wave height. For the comparative evaluation of the generated incident wave using numerical analysis and experiments, the data sampling rate was 20 per second (20 Hz). Table 2 outlines the experimental conditions for each wave period and wavelength.

![Fig. 2 Inclined OWC installed in the two-dimensional mini wave tank](image2)

Table 2 Wave conditions for wave tank experiment

<table>
<thead>
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<td>0.8 s</td>
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<td>1.2 s</td>
<td>1.861 m</td>
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Fig. 3 is a schematic of the experimental setting of the two-dimensional mini wave tank for an inclined OWC chamber with an open chamber. $B$ denotes the gap (width) of the OWC chamber, $d$ the draft of the skirt, and $\theta$ the inclination angle between the inclined OWC chamber and seabed. In addition, $h$ is the water depth, $W_i$ is the wave gauge, and $L_s$ is the distance from the wave maker to the wave gauge. The thickness of the skirt was fixed to 2 cm to maintain the structural strength of the OWC experimental model without affecting
the wave elevation in the chamber. An ultrasonic wave gauge was used to measure the incident wave height, and the distance \( L \) between the wave maker and wave gauge \( W \) was set to 2 m to avoid the evanescent mode generated by the wave maker. The signal measured by the ultrasonic wave gauge was amplified using an amplifier (AMP) and sent to a data acquisition device (DAQ), and the measurement data was then stored in a computer for analysis.

3.2 Ultrasonic Wave Gauge

To accurately measure the incident wave height, an ultrasonic wave gauge (TSPC-30S2, Senix) was used (Fig. 4). Table 3 presents the specifications of the wave gauge. The ultrasonic wave gauge measures the wave height by emitting ultrasonic waves in the vertical direction and measuring the time reflected from the sea surface with a transducer. As ultrasonic waves propagate slower in air than in water and are affected by temperature changes, a built-in temperature compensation circuit was provided in the wave gauge.

![Ultrasonic wave gauge (TSPC-30S2)](image)

Table 3 Specifications of TSPC-30S2

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<td>Sampling rate</td>
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<td>Interface</td>
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3.3 Inclined OWC Model

The inclined OWC model used in the experiment was designed with a CAD program and made using acrylic material. In Fig. 2, a two-dimensional mini wave tank installed with the top of the OWC chamber open (open chamber) is presented, and the OWC skirt is marked by red taping. As measuring the wave elevation inside the inclined OWC chamber is difficult with an ultrasonic wave gauge that can measure only the vertical direction, a graph paper was attached to the wall of the two-dimensional mini wave tank, experimental images were taken frame by frame, and the wave height inside the OWC chamber was measured at steady state.

![Two different shapes of an inclined OWC skirt](image)

4. Results of Numerical Analysis and Experiments

4.1 Comparison of Incident Wave Elevations

In this study, numerical analysis was conducted under various wave conditions to examine the hydrodynamic characteristics of the inclined OWC chamber. If the generation and propagation of the incident wave are not performed accurately, the flow inside the OWC chamber cannot be considered reliable. Therefore, checking that the wave is properly generated at the incident wave boundary of the numerical wave tank and propagated without dissipation to the OWC chamber is important. In addition, the elevation of the incident wave was measured using a wave gauge, and whether a regular wave was properly generated and propagated with the same period as the incident wave period was evaluated.

In Fig. 7, the time-series data of the incident wave generated from the experiment and CFD analysis were compared. The two waves show good agreement. Here, the x-axis is made dimensionless by dividing the time interval by the incident wave period, and the y-axis represents the measured incident wave elevation. The point at which the wave elevation was measured is 2 m away from the incident wave.
boundary, and wave-making was confirmed to have been performed properly by generating regular waves as the incident wave.

### 4.2 CFD analysis

The real seas applied in the numerical simulation of this study were set as Ganjeolgot Cape near Ulsan, an area with a small tidal range. This location was chosen based on the smooth operating conditions of the breakwater-linked inclined OWC WEC. The experiments and numerical analysis were performed by reducing the water depth, height, and period of the real seas according to Froude’s similarity law. Considering the specifications of the experiment wave tank, the reduction scale was 1/40 compared to that of the real seas. Table 4 presents the specifications of the 1/40 scale-inclined OWC model used in the numerical analysis and two-dimensional mini wave tank experiment. The depth of the real seas was 12.8 m, and the depth for the 1/40 scale model was 0.32 m. Considering parametric analysis and wave tank specifications, the gap (\( \beta \)) of the OWC chamber was set to 0.06 m, the draft of the skirt to 0.05 m, and the thickness of the skirt to 0.02 m. In addition, the angle between the rear wall of the skirt and tank bottom was set to 33.7°, similar to the inclination of a typical bottom-mounded breakwater on the coast of the Korean Peninsula. The length of the numerical wave tank was set the same as the experimental condition with the wave tank. Table 5 shows the wave conditions for the numerical analysis and experiment of the inclined OWC chamber. Five conditions (cases) were set by increasing the period of the incident wave of the model condition at regular intervals (0.1 s). Previous studies confirmed that the displacement of the water surface inside the OWC chamber is greatly affected by the draft (\( d \)) of the skirt and gap (\( \beta \)) of the OWC chamber (Koo et al., 2012).

Table 6 compares the wave elevation inside the OWC chamber for different incident wave cases under the open chamber condition for the pointed- and rounded-skirt conditions. \( H \) represents the incident wave height, \( H_a \) the wave height within the OWC chamber, and \( RH_p \) and \( RH_R \) represent the ratio of the relative wave height in the chamber against the incident wave height under the pointed- and rounded-skirt conditions, respectively. The difference ratio for the two conditions (difference ratio, DR, %) can be represented as \( \frac{(RH_R - RH_p)}{RH_p} \times 100 \).

For the ratio of relative wave height in the chamber to the incident wave height (\( RH_p \) and \( RH_R \)), the maximum values measured at the wave period of 1.1 s (model condition) was approximately 4.6, regardless of the skirt shape. As the incident wave period nears 1.1 s, the water surface displacement in the chamber increases, and the displacement decreases for the incident wave period conditions larger than 1.1 s. In addition, a relative wave height in the rounded-skirt condition was measured larger than the pointed-skirt condition for all incident wave periods, and in Case 2 (wave period of 0.9 s), the DR (%) was approximately 47%. The result indicates that the fluid viscous

---

**Table 4** Main specification of the OWC model (unit : m)

<table>
<thead>
<tr>
<th>Component</th>
<th>Value</th>
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</thead>
<tbody>
<tr>
<td>Skirt draft (( d ))</td>
<td>0.05</td>
</tr>
<tr>
<td>Chamber gap (( \beta ))</td>
<td>0.06</td>
</tr>
<tr>
<td>Angle (( \theta ))</td>
<td>33.7°</td>
</tr>
<tr>
<td>Water depth (( h ))</td>
<td>0.32</td>
</tr>
<tr>
<td>Water tank length</td>
<td>5.62</td>
</tr>
<tr>
<td>Water tank half breadth</td>
<td>0.15</td>
</tr>
</tbody>
</table>

**Table 5** Wave conditions for open chamber OWC simulation

<table>
<thead>
<tr>
<th>Case</th>
<th>Wave period (s) [real]</th>
<th>Wave period (s) [model]</th>
<th>Wave length (m) [model]</th>
<th>Wave height (m) [model]</th>
</tr>
</thead>
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<tr>
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<td>0.977</td>
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<td>5.692</td>
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<td>0.02</td>
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<td>1.0</td>
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<tr>
<td>4</td>
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<td>1.1</td>
<td>1.645</td>
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<tr>
<td>5</td>
<td>7.589</td>
<td>1.2</td>
<td>1.861</td>
<td>0.02</td>
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</table>

**Table 6** Comparison of wave elevation between the cases of pointed and rounded skirts in open chamber condition (\( H = 0.02 \) m)

<table>
<thead>
<tr>
<th>Case</th>
<th>Wave period (s) [model]</th>
<th>Pointed skirt ( H_a ) (m)</th>
<th>Rounded skirt ( H_a ) (m)</th>
<th>Pointed skirt ( RH_p )</th>
<th>Rounded skirt ( RH_R )</th>
<th>DR(^{(1)})(%)</th>
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</thead>
<tbody>
<tr>
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<td>0.016</td>
<td>0.023</td>
<td>0.8</td>
<td>1.16</td>
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<td>0.034</td>
<td>0.050</td>
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<td>47.1</td>
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<tr>
<td>3</td>
<td>1.0</td>
<td>0.062</td>
<td>0.085</td>
<td>3.11</td>
<td>4.23</td>
<td>36.0</td>
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<tr>
<td>4</td>
<td>1.1</td>
<td>0.092</td>
<td>0.094</td>
<td>4.61</td>
<td>4.71</td>
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<tr>
<td>5</td>
<td>1.2</td>
<td>0.079</td>
<td>0.084</td>
<td>3.94</td>
<td>4.19</td>
<td>6.3</td>
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\(^{(1)}\)Difference ratio

---

Fig. 7 Comparison of incident wave elevations for numerical results and experimental data (\( T = 0.8 \) s, \( H = 2 \) cm)
damping generated when the incident wave enters the chamber is significantly smaller in the rounded-skirt condition than in the pointed-skirt condition. In other words, for the rounded-skirt shape, the incident wave energy consumption caused by the fluid viscous damping can be considered minimized, resulting in more wave energy flowing into the chamber.

In addition, through the DR (%) of the water surface displacement in the chamber, when the incident wave period is smaller than the resonance period (0.8–1.0 s), the water surface displacement in the rounded-skirt was at least 36% larger than that in the pointed-skirt condition. However, in a period larger than the resonance period (1.1 s or more), the displacement is relatively small, indicating that, under long-wave period conditions, incident wave energy can easily enter the chamber regardless of the shape of the skirt end, and the effect of viscous damping is relatively small. Therefore, to increase the energy extraction efficiency of the breakwater-linked OWC, designing a skirt shape that minimizes viscous damping under the operating conditions with a wave period smaller than the resonance period is necessary.

Table 7 shows the calculation result of the wave elevation inside the OWC chamber according to the skirt shape with an orifice in the inclined OWC. The orifice functions as an energy conversion device that performs the first conversion of the incident wave energy entering the OWC chamber into pneumatic energy. As the incident wave passes through the skirt and a wave crest is formed in the OWC chamber, the airflow in the OWC chamber is discharged through the orifice (nozzle). The discharged airflow energy is extracted from the incident wave energy in the chamber, and the wave elevation inside the OWC chamber decreases by as much as the extracted energy. In addition, as a wave trough is formed in the OWC chamber, the airflow enters the chamber through the nozzle, and the decrement of the wave elevation inside the OWC chamber is reduced by the incoming airflow energy. Thus, a type of damping acts on the displacement (rise and fall) of the water surface inside the OWC chamber. In addition, the water surface in the orifice chamber is significantly higher in the rounded-skirt condition than in the pointed-skirt condition, indicating that, under the rounded-skirt condition, the amount of residual wave energy in the chamber is considerably large even after subtracting the pneumatic energy extracted by the orifice. Moreover, the generated energy loss

### Table 7 Comparison of wave elevation between the cases of pointed and rounded skirts in orifice chamber condition ($H = 0.02$ m)

<table>
<thead>
<tr>
<th>Case</th>
<th>Wave period (s) [model]</th>
<th>Pointed skirt $H_{O1}$ (m)</th>
<th>Rounded skirt $H_{O1}$ (m)</th>
<th>Pointed skirt $RH_{O1}$</th>
<th>Rounded skirt $RH_{O1}$</th>
<th>DR (%)</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>0.8</td>
<td>0.014</td>
<td>0.034</td>
<td>0.69</td>
<td>1.68</td>
<td>143.4</td>
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<td>2</td>
<td>0.9</td>
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<td>0.045</td>
<td>1.41</td>
<td>2.23</td>
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<tr>
<td>3</td>
<td>1.0</td>
<td>0.034</td>
<td>0.062</td>
<td>1.72</td>
<td>3.09</td>
<td>76.7</td>
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<tr>
<td>4</td>
<td>1.1</td>
<td>0.041</td>
<td>0.068</td>
<td>2.03</td>
<td>3.39</td>
<td>67.0</td>
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<tr>
<td>5</td>
<td>1.2</td>
<td>0.040</td>
<td>0.072</td>
<td>1.99</td>
<td>3.61</td>
<td>81.4</td>
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1) Difference ratio

![Fig. 8](image_url)  
(a) Maximum flow at nozzle in pointed-skirt condition  
(b) Minimum flow at nozzle in pointed-skirt condition

**Fig. 8** Maximum and minimum flow rates at nozzle in pointed-skirt and rounded-skirt condition ($T = 0.8$ s, $H = 0.02$ m)
owing to fluid viscosity in the skirt corresponds to the difference between the rounded- and pointed-skirt results.

Fig. 8 shows a comparison between the maximum and minimum flow rates inside the OWC chamber with different skirt shapes with an orifice. The wave period was 0.8 s, and the wave height was 0.02 cm. The figure on the left shows the wave elevation, and the figure on the right shows the air flow rate in the orifice. In Figs. 8(a) and 8(c) the water surface displacement inside the OWC chamber indicates the moment when the elevation rose from the minimum level to the average level; at this time, the force to move upward is the largest, maximizing the air flow rate discharged through the orifice inside the OWC chamber. Conversely, in Figs. 8(b) and 8(d) the water surface displacement inside the chamber indicates the moment when the elevation is reduced from the maximum level to the average level; at this time, the force to move downward is the largest, and the airflow rate absorbed by the orifice becomes the maximum.

Table 8 shows the comparison of the water surface displacement in the open and orifice chambers with different skirt shapes. As the incident wave period increases, the difference in wave elevation in the orifice chamber increases compared with the open chamber, and the maximum difference is shown at a wave period of 1.1 s, which is the chamber resonance period. In particular, in the pointed-skirt condition, a wave elevation difference of up to 55.4% occurs depending on the presence or absence of an orifice (wave period of 1.1 s). Consequently, this difference in wave elevation is the effect of the pneumatic energy extraction through the orifice and the effect of the additional energy attenuation caused by the orifice condition. In other words, as wave energy is converted into pneumatic energy in the orifice condition, the wave elevation in the chamber is lowered and the difference is extracted as energy; for some of the energy, additional energy attenuation occurs during energy conversion.

Meanwhile, under the rounded-skirt condition, the ratio (DR) of the wave elevation difference between the open and orifice chambers is smaller than that in the pointed-skirt condition. Although a relatively large amount of wave energy flows into the chamber, and the influx of the energy is converted into pneumatic energy, the residual wave energy

<table>
<thead>
<tr>
<th>Case</th>
<th>Wave period (s)</th>
<th>$H_o$ (m) Open</th>
<th>$H_o$ (m) Orifice</th>
<th>Difference (m)</th>
<th>$H_o$ (m) Open</th>
<th>$H_o$ (m) Orifice</th>
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<th>DR ($)</th>
<th>DR (%)</th>
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<td>0.034</td>
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<td>0.041</td>
<td>0.051</td>
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<td>0.068</td>
<td>0.026</td>
<td>27.7</td>
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<td>0.072</td>
<td>0.012</td>
<td>14.3</td>
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$\text{DR} = \left( \frac{H_o \text{ Orifice} - H_o \text{ Open}}{H_o \text{ Open}} \right) \times 100$
energy is also considerably large, resulting in a small wave elevation difference (ratio). In other words, in the rounded-skirt condition, the wave elevation inside the chamber being significantly higher than in the pointed-skirt condition indicates that the residual wave energy remaining after conversion to pneumatic energy is large. If the size of the orifice is optimized, it maximizes the energy extracted and minimizes the wave elevation in the chamber. Moreover, at a wave period of 0.8 s under the rounded-skirt condition, the water surface displacement is larger in the orifice chamber condition than in the open chamber condition, signifying that, under the rounded-skirt condition of a specific wave period, although a large amount of incident wave energy is introduced and some energy is extracted owing to the orifice chamber, a large amount of residual energy remains. However, more detailed analyses such as those involving changes in the pneumatic pressure inside the chamber and particle kinetics are required to identify the exact cause of the result. In conclusion, this study shows that in the rounded-skirt condition, maximizing energy extraction is possible through orifice size optimization while minimizing energy attenuation.

4.3 Comparison of Experimental and CFD Simulation Results

In Fig. 9(a), the displacement of the water surface inside the chamber with different skirt shapes in the open chamber condition was compared with the numerical analysis and experiment results; overall, the results show a good agreement. The change in wave elevation according to the skirt shape is large for wave periods of 0.8–1.0 s—a range of relatively short waves—possibly because of the large decrease in viscous energy under the pointed-skirt condition as previously discussed. However, the difference in surface displacement is insignificant at the resonance period (1.1 s) and period conditions longer than the resonance period, possibly because the shape of the skirt has a minimal effect on the incident wave entering the OWC chamber owing to the characteristics of the long waves.

Comparing the numerical analysis and experimental results according to the skirt shape of an OWC with an orifice installed (Fig. 9(b)), the results show a generally good agreement. Compared to the result of the open chamber condition in Fig. 9(a), a clear resonance period cannot be confirmed, and the wave elevation inside the chamber can be seen to increase with the wave period. The difference between the pointed- and rounded-skirt results in the orifice chamber condition is because of the fluid viscous damping that occurs in the skirt. Comparing the results through the figures, the viscous damping under the pointed-skirt condition is seen to be considerably large for all values of the incident wave period. In addition, as the incident wave period increases, the energy loss owed to fluid viscosity is seen to gradually increase.

5. Conclusion

In this study, hydrodynamic characteristics according to the skirt shape of the inclined OWC WEC were comparatively analyzed using numerical analysis and experiments. To investigate the effect of fluid viscous damping occurring at the end of the skirt, pointed- and rounded-skirts were selected, and the displacement of the water surface in the chamber was compared with the different skirt shapes.

Under the open chamber condition without an orifice effect, the relative wave elevation inside the chamber compared to the incident wave height was measured to show a maximum ratio of ~ 4.6 at the resonance period of 1.1 s (model condition) regardless of skirt shape. In addition, a larger relative wave elevation was measured in the rounded-skirt condition compared to the pointed-skirt condition for all incident wave periods. In the experimental condition (wave period of 0.9 s), the wave elevation under the rounded-skirt condition was approximately 47% larger. More wave energy enters the chamber because incident wave energy consumed by fluid viscous damping is minimized in the rounded-skirt condition. Through the DR of the water surface displacement inside the chamber, when the incident wave period is smaller than the resonance period (0.8–1.0 s), the water
surface displacement in the rounded-skirt condition is at least 36% larger than that in the pointed-skirt condition. When the incident wave period is larger than the resonance period (1.1 s or more), the displacement is relatively small. These results indicate that, under long-period conditions, incident wave energy can easily enter the chamber regardless of the shape of the skirt, and the effect of viscous damping is relatively small. Therefore, to increase the energy extraction efficiency of breakwater-linked OWC, designing a skirt shape that minimizes fluid viscous damping in the operating condition with a wave period shorter than the resonance period is necessary.

In a chamber with an orifice installed, the water surface displacement was significantly larger in the rounded-skirt condition than in the pointed-skirt condition, indicating that, in the rounded-skirt condition, the residual wave energy in the chamber is considerable even after subtracting the pneumatic energy extracted by the orifice. If the size of the orifice is properly optimized, it maximizes the energy extraction and minimizes the wave elevation inside the chamber. In addition, the energy loss owing to fluid viscosity in the skirt is the difference between the rounded- and pointed-skirt results, and the viscous damping caused by the pointed-skirt is considerably large. The result confirms that, as the incident wave period increases, the energy loss due to fluid viscosity gradually increased.

In conclusion, this study demonstrates that under the rounded-skirt condition, maximum energy extraction can be achieved by optimizing the orifice size and minimizing energy attenuation.

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**Conflict of interest**

Weoncheol Koo serves as an editor of the Journal of Ocean Engineering and Technology but has no role in the decision to publish this article. No potential conflict of interest relevant to this article was reported.

**References**


Author ORCIDs

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1. Introduction

There have been a growing interest and developmental effort toward environment-friendly renewable energy owing to climate change and environmental pollution. The development of eco-friendly energy cannot be further delayed, in which global R&D efforts and cooperation are urgently required. Marine energy, one of the well-known eco-friendly energy resources, can be classified into wave energy, offshore wind energy, tidal current energy, and ocean thermal energy. In particular, wave energy is a highly promising energy source, considering its high energy density, overall amount of energy, and future potential. Several methods have been developed to convert wave energy to electrical energy; these methods can be classified into movable body, wave overtopping, or oscillating water column (OWC) depending on the primary energy conversion type. Among these, OWC-type energy conversion involves the displacement of the water level inside a chamber, as the incident wave flows into an energy converter, and subsequently, the airflow inside the chamber can rotate special air turbines, e.g., Wells turbine or impulse turbine, installed on a nozzle to convert energy. Such energy conversion method possesses the advantages of easy maintenance and repair of an energy conversion system and no direct contact with incident waves, as the major energy conversion system, e.g., power take-off (PTO), is located outside the water surface.

A fixed OWC wave energy converter (WEC) has been actively researched worldwide and produced extensive research achievements among various wave energy conversion methods, while being closest to commercialization. Therefore, a necessity has arisen to organize the R&D achievements with regard to an OWC WEC and provide basic information. To this end, this study has generally organized the achievements and various research content of a fixed OWC WEC in various countries. However, as it is impossible to organize and discuss all the research achievements, this study focused on the primary energy conversion of various OWC structures. The results of secondary energy conversion, e.g., PTO system, will be introduced in future studies. We expect that the readers will gather basic information for further detailed studies on the OWC.
The concept of an OWC WEC was first described in 1947; however, this specific term first appeared in a paper published in 1978 (Masuda and Miyazaki, 1978; Evans, 1978; Falcão and Henriques, 2016). Prior to 1978, such type of WECs has been referred to as the Masuda device. The OWC WEC has been most widely studied, and numerous prototype structures have been installed in real sea environments.

The OWC WEC can be divided into fixed and floating types. In this paper, the research achievements and results of the primary energy conversion of a fixed OWC WEC are divided into the following five categories: potential flow analysis, relevant wave tank experiments, computational fluid dynamics (CFD) analyses assuming viscous fluids, U-shaped OWC (U-OWC) studies that can amplify free surface displacement in a chamber, and study results of OWC prototypes that have been installed and operated in real sea environments.

A floating-type OWC WEC includes single and multiple air chambers and a backward bent duct buoy (BBDB), which possesses a relatively high energy conversion efficiency owing to its unique structure. The R&D achievements and study results of the floating-type OWC WEC will be comprehensively reviewed in future studies.

2. Analysis of Fixed OWC WEC

2.1 Potential Flow Analysis of OWC

The dynamic analysis of a fixed OWC WEC is based on a potential flow assumption that considers inviscid, incompressible, and irrotational fluids. Incident waves flowing into a chamber generate a vortex, which is the effect of fluid viscosity, in a skirt-shaped structure at the inlet of an OWC, thus causing energy attenuation. However, a potential flow analysis can be conducted under the assumption that the actual energy loss caused by fluid viscosity is not significant compared to the entire incident energy amount. Accordingly, the overall performance of an OWC and extraction energy efficiency can be calculated.

The potential flow analysis involves generally a boundary element method (BEM) to solve boundary integral equations at fluid boundaries. Delauré and Lewis (2003) used the first-order mixed distribution panel method to analyze a three-dimensional fixed OWC device under regular and irregular wave conditions and confirmed its accuracy based on a comparison with the experimental results of irregular waves at a scale of 1:36. Josset and Clément (2007) analyzed fluid dynamic problems by uncoupling the internal chamber flow and exterior structure considered for the numerical analysis of an OWC in the time domain and solved the fluid dynamic problem of an OWC exterior structure, which requires long computation time. Thus, they developed a numerical analysis tool that is useful in each step of a plant project, starting from the pre-conception to plant monitoring of an OWC. In addition, numerical simulation results confirmed that productivity can be improved by 15.5% by replacing the Wells turbine of a fixed 400-kW OWC WEC located on the Pico Island of Portugal. Koo and Kim (2010) developed a fully nonlinear numerical wave tank capable of expressing free surface displacement using the mixed Eulerian–Lagrangian (MEL) method and simulated nonlinear wave motions of free surface inside a chamber (Fig. 1). Furthermore, a damping coefficient proportional to the average vertical velocity of the free surface was substituted in boundary conditions in order to implement the energy loss of a chamber skirt caused by fluid viscosity when incident waves flowed inside the chamber. Through this, more accurate results were obtained by adding energy loss due to viscosity to the potential flow analysis assuming an inviscid fluid. The maximum extractable energy was computed by calculating the available wave energy according to the volume ratio between the chamber and duct. Liu et al. (2010) also considered the MEL method and studied the interaction between incident waves and structures using the desingularized boundary integral equation method (DBIEM), which distributes the source outside the boundary of the fluid computational domain. Kim et al. (2021a) calculated the pneumatic damping coefficient, which has a similar meaning to the coefficient that converts the air pressure in a chamber into electrical energy, as a theoretical solution, and applied it to a two-dimensional numerical wave tank to obtain the energy extraction efficiency of an OWC. Such method differs from the method where the air damping coefficient is
determined by comparing with previous experimental results or using a trial-and-error method.

Several studies on the high-order boundary element method (HOBEM), which can better simulate the nonlinearity of each element than the constant panel method (CPM), have been reported. Ning et al. (2015) developed a fully nonlinear 2D numerical wave tank in the time domain using the HOBEM, which can improve the efficiency and accuracy of numerical computation results by modeling the CPM of the corners or edge shapes of OWC structures. Wang et al. (2018) compared the numerical analysis results according to nonlinearity and viscosity terms to analyze the effects of nonlinearity and viscosity on fluid dynamic efficiency. Fluid dynamic efficiency improved considering nonlinearity and viscosity when the amplitude of incident waves was small; however, the efficiency decreased when the amplitude of the incident waves increased, as the transmission of the second-order harmonic wave component weakened owing to strong nonlinearity.

Studies have been conducted on applying a dual chamber or changing the sea floor to improve the energy conversion efficiency of an OWC device. Rezanejad et al. (2013) analyzed the effects of stepped sea bottom on the OWC efficiency (Fig. 2). Two methods were used to solve the boundary value problem (BVP) of the computational domain: the matched eigenfunction expansion method and the boundary integral equation method (BIEM) satisfying boundary conditions. The performance of a device is not significantly affected if a sea floor step is positioned inside a chamber, whereas the
performance improves if a sea floor step is positioned outside the chamber. The performance of a device improves when the peak frequency decreases owing to a decrease in the water depth. An OWC can achieve relatively high efficiency if the OWC structure and step are designed, so that the second and third resonance occurs near the first resonance frequency. Moreover, it was confirmed that the horizontal distance between the OWC skirt and step edge needs to be closer to 1/4 of the incident wavelength to maximize the effects of the third resonance mechanism. In a subsequent study, it was confirmed that the efficiency of a dual chamber drastically improved in a wide frequency band in relation to that of a single chamber on a stepped sea floor (Rezanejad et al., 2015). The performance could have improved if at least one sea floor step was positioned outside the chamber. The performance improved when one step was located inside and outside the chamber; however, it was smaller than when two steps were positioned outside the chamber. The viscosity effect inducing vortex shedding near the step, which was ignored, can be considered for a more realistic prediction. Koirala et al. (2015) measured primary conversion efficiency by comparing the following cases using a 2D numerical model in the frequency domain: a dual chamber comprising two air chambers and a dual chamber comprising one air chamber (Fig. 3). The pneumatic pressure inside the chambers, reflection coefficient, and primary conversion and coupling efficiencies of each chamber were calculated using the BIEM. The dual-chamber OWC comprising two air chambers exhibited higher energy efficiency in a long wave region than the single-chamber OWC under the same physical conditions. Furthermore, the dual chamber comprising one air chamber exhibited two peak values on the primary conversion efficiency curve, and the primary conversion efficiency was extremely low when the wavelength ($\lambda$) was eight times the OWC length ($L$).

Several studies on an inclined OWC, which can be installed by linking with a breakwater, were conducted. With regard to the hydrodynamic problem of the OWC chamber, Kim et al. (2020) used the finite element method to perform a time domain analysis based on the linear potential flow theory. They confirmed that the energy conversion efficiency of the OWC chamber demonstrates a nonlinear response based on the incident wave height with regard to hydrodynamic performance. Kim et al. (2021b) reviewed the validity of applying a linear decomposition method (radiation and scattering problems) and calculating turbine-chamber interactions based on linear air pressure drop characteristics and confirmed that the results matched the irregular wave simulation results. Yang et al. (2021) compared the experimental results of a 2D fully nonlinear numerical wave tank to calculate the wave height inside the chamber considering the chamber width and changes in the chamber skirt draft and further computed the maximum amount of extractable wave energy by

![Fig. 4 Schematic of a sloped OWC system and comparison of each energy component (Yang et al., 2021)](image)

<table>
<thead>
<tr>
<th>Reference</th>
<th>Computational method</th>
<th>Chamber type</th>
</tr>
</thead>
<tbody>
<tr>
<td>Koo and Kim (2010)</td>
<td>BEM</td>
<td>Vertical chamber</td>
</tr>
<tr>
<td>Kim et al. (2021a)</td>
<td>BEM</td>
<td>Vertical chamber</td>
</tr>
<tr>
<td>Liu et al. (2010)</td>
<td>DBIEM</td>
<td>Vertical chamber</td>
</tr>
<tr>
<td>Ning et al. (2015)</td>
<td>HOBEM</td>
<td>Vertical chamber</td>
</tr>
<tr>
<td>Wang et al. (2018)</td>
<td>HOBEM</td>
<td>Vertical chamber</td>
</tr>
<tr>
<td>Rezanejad et al. (2013)</td>
<td>Matched eigenfunction expansion, BIEM</td>
<td>Stepped sea bottom</td>
</tr>
<tr>
<td>Rezanejad et al. (2015)</td>
<td>Matched eigenfunction expansion, BIEM</td>
<td>Stepped sea bottom, Dual-chamber</td>
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<tr>
<td>Koirala et al. (2015)</td>
<td>BEM</td>
<td>Dual-chamber</td>
</tr>
<tr>
<td>Kim et al. (2020)</td>
<td>FEM</td>
<td>Sloped chamber</td>
</tr>
<tr>
<td>Kim et al. (2021b)</td>
<td>FEM</td>
<td>Sloped chamber</td>
</tr>
<tr>
<td>Yang et al. (2021)</td>
<td>BEM</td>
<td>Sloped chamber</td>
</tr>
</tbody>
</table>

Note: BEM = boundary element method, BIEM = boundary integral equation method, DBIEM = desingularized boundary integral equation method, FEM = finite element method, HOBEM = high-order boundary element method
calculating the energy components of a WEC system (Fig. 4). Table 1 provides the comparisons of the potential-flow-based numerical analysis methods for OWC mentioned in this section and the shape of the OWC chamber.

2.2 OWC Wave Tank Experiments

Various wave tank experiments were conducted to test the performance of OWC and verify the numerical analysis results of a fixed OWC WEC. To verify the OWC numerical model (theoretical solution using the wave Green function) installed on the coast with an arbitrary topography, Wang et al. (2002) conducted an experiment for OWC models at a scale of 1:12, considering different seabed slopes in a wave tank (length: 32.0 m, width: 18.0 m, and depth: 1.0 m) under a regular wave condition. Gouaud et al. (2010) installed an OWC on an underwater three-dimensional mound (UTDM) in a large-scale ocean engineering basin and conducted an experiment considering regular and irregular wave conditions. They confirmed a significant increase in the capture–width ratio caused by the concentrated waves on the mound. Therefore, it was confirmed that the use of UTDM would result in economical efficiency by amplifying the energy flux at the inlet of the OWC. Koo et al. (2012) installed a fixed OWC WEC model on a 2D wave basin and measured the water surface displacement inside the chamber according to the incident wave frequency. Moreover, they examined the maximum water surface displacement depending on structural changes, i.e., chamber skirt draft and seabed slope angle, to identify the effects of different shapes. Allsop et al. (2014) tested a large-scale OWC model (approximately 1:5–1:9 model scale) in a wave tank to measure wave energy, water column movement, air pressure, and airflow according to the size of an orifice size. Accordingly, calibration data needed for a CFD model were expected. The scale effect for wave energy and performance was examined by comparing the results based on a small-scale model test. Viviano et al. (2018) compared the results of a small-scale experiment conducted in a random incident wave condition with those of a large-scale experiment (Allsop et al., 2014) and analyzed the scale effect for water column movement, reflected waves, and external force applied to an outer front wall. Moreover, they investigated the air compression effect through a small-scale experiment. Ning et al. (2016) conducted an OWC wave tank experiment to measure the water surface displacement inside a chamber according to the shape coefficient of structures and compared the results with previous numerical results (Ning et al., 2015). They verified that the surface displacement inside a chamber is significantly affected by incident wavelength and chamber width ratio, whereas the seabed slope is relatively less influential. In addition, the seiching phenomenon (maximum amplitude at both ends of a chamber and 0 amplitude in the center) occurs when the hydrodynamic efficiency is close to 0 and the wavelength (λ) is twice the chamber width (β). López et al. (2015) measured flow characteristics, i.e., water particle velocity, vorticity, kinetic energy, and turbulence kinetic energy, using particle imaging velocimetry (PIV) based on a phase-averaging procedure to analyze the effects of turbine damping and tidal level on the OWC device (Fig. 5). Thus, it was confirmed that the damping coefficient by the turbine is a major factor affecting the energy extraction efficiency.

Rezanejad et al. (2017) conducted a wave tank experiment for an OWC device placed on a stepped floor terrain and reported that the efficiency of an OWC is significantly affected by the incident wave period and turbine damping coefficient. A subsequent study (Rezanejad and Guedes Soares, 2018) confirmed that the frequency bandwidth with regard to OWC efficiency can be substantially improved when a dual-mass concept is applied and verified through an experiment that the performance improvement of an OWC device in a stepped sea floor condition corresponds to the implementation of a dual-mass system (Fig. 6).

Dizadji and Sajadian (2011) conducted a 2D wave tank experiment (16.0 m × 0.7 m × 0.5 m) for an inclined OWC model to identify the effects of each shape coefficient of the structure, including the inclination angle (30°–60°), and selected an optimization model for
maximum energy extraction, reporting 32% efficiency. Park et al. (2018b) evaluated the performance of a conventional type chamber with a right-angle OWC skirt and a sloped-type chamber based on a 2D wave basin (40.0 m × 0.6 m × 1.0 m) experiment and reported that the sloped-type chamber demonstrated excellent energy extraction performance and could be installed by linking with breakwater (Fig. 7). Furthermore, Lim et al. (2021) calculated a wave load applied to the OWC structure combined with sloped breakwater through CFD simulation and verified the stability of the WEC structures through a 2D physical model experiment (50.0 m × 1.2 m × 1.5 m).

![Physical model of the OWC device](image)

**Fig. 6** Physical model of the OWC device (Rezanejad and Guedes Soares, 2018)

![Experimental models installed in the 2D wave flume and measured relative wave heights](image)

**Fig. 7** Experimental models installed in the 2D wave flume and measured relative wave heights (Park et al., 2018b)
Ikoma et al. (2019) conducted a wave tank experiment for an OWC WEC (Projecting Wall-OWC model), in which a projecting wall for improving energy efficiency is installed on a double-dissipating caisson. The experiment confirmed that the water surface displacement and air pressure inside a chamber were significantly affected by the wave height. Furthermore, the use of the double-dissipating caisson was advantageous for the primary energy conversion of 80% or higher.

2.3 Viscous Flow Analysis of OWC

Owing to advancements in computer processing capabilities, a CFD analysis, which includes Navier–Stokes equations that can consider fluid viscosity within the computation domain as governing equations, is being actively used to predict the performance of an OWC WEC. The CFD model possesses the advantage of being able to consider strong nonlinearity, complex viscous effects, turbulence, and vortex. For example, the loss of incident wave energy can be estimated by applying the fluid viscosity effect generated in a specific shape region of a structure when incident waves enter the chamber. However, computational modeling is difficult, calculation time may drastically increase with the number of grids, and the experimental verification of the results is required. Open-source codes, e.g., REEF3D and OpenFOAM, or commercial CFD codes, e.g., Fluent and Star-CCM+, are generally used. Several studies that used REEF3D and OpenFOAM have been reported. Kamath et al. (2015) studied the PTO damping effect for a 2D OWC chamber using a REEF3D-based numerical model with incompressible Reynolds-averaged Navier-Stokes (RANS) (Fig. 8). They confirmed that the PTO damping coefficient, which is required to achieve the maximum hydrodynamic efficiency, increases with the incident wavelength and the hydrodynamic efficiency is influenced by incident wavelength, wave height, and PTO damping. Rajan et al. (2019) performed a numerical analysis based on the floor slope of an OWC device, PTO damping effect, and incident wave conditions and further analyzed the hydrodynamic efficiency by calculating chamber pressure, free surface velocity, and free surface rise.

Iturrioz et al. (2015) modeled a fixed detached OWC using OpenFOAM and verified the model using the experimental data (IH Cantabria’s OpenFOAM: IHFOAM) for free surface displacement, pneumatic pressure, and air velocity. IHFOAM, which is used to fit interaction between waves and structures, was proposed by Higuera et al. (2013). Furthermore, actual wave conditions were simulated using active wave absorption at the boundary and the simulation speed was increased by reducing the computational domain. Vyzikas et al. (2017b) compared the results of the multiphase RANS numerical model with the COAST experimental results of Plymouth University (UK) to simulate the interaction of the OWC under regular and irregular wave conditions.

Studies are actively being conducted using Fluent and Star-CCM+, which are commercial CFD codes. The results of conducting studies on OWC using Fluent are mentioned hereafter. Marjani et al. (2008) performed a simulation on flow characteristics inside a chamber by modeling a chamber and impulse turbine. Teixeira et al. (2013) investigated the optimal performance of an OWC installed onshore based on the relationship between chamber shape and turbine characteristics; the Fluent model, which handles incompressible flow problems based on the Navier–Stokes equations, was used with the two-step semi-implicit Taylor–Galerkin method. Similar results were obtained by comparing the flow variables acquired from Fluent with the results of a commercial model—Fluent. Luo et al. (2014) developed a 2D fully nonlinear CFD model and analyzed the efficiency of a fixed OWC equipped with a linear PTO. They confirmed that the optimal pneumatic damping coefficient of an OWC is dependent on the incident wave height, and the hydrodynamic extraction of an OWC device rather decreases as the wave height increases in the nonlinear wave condition. Therefore, the hydrodynamic system of an OWC is fully nonlinear, which cannot be accurately represented with the superposition of linear responses.

The following studies used Star-CCM+. López et al. (2014) implemented a 2D numerical model based on the RANS equations and the volume of fluid (VOF) technique for the analysis of the turbine and chamber and verified the calculation results by comparing them with the wave tank experimental results. The damping coefficients of various turbines were computed using the verified numerical model, and the OWC efficiency was calculated under regular and irregular wave conditions. Moreover, López et al. (2016) developed a method for determining a damping coefficient that optimizes the OWC

<table>
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<tr>
<th>C</th>
<th>Value</th>
<th>Implication</th>
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<tr>
<td>C0</td>
<td>0</td>
<td>No damping</td>
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<tr>
<td>C1</td>
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<td>Low damping</td>
</tr>
<tr>
<td>C2</td>
<td>2×10⁸</td>
<td>Low damping</td>
</tr>
<tr>
<td>C3</td>
<td>3×10⁸</td>
<td>Moderate damping</td>
</tr>
<tr>
<td>C4</td>
<td>4×10⁸</td>
<td>Moderate damping</td>
</tr>
<tr>
<td>Cexp</td>
<td>5×10⁸</td>
<td>From experimental data</td>
</tr>
<tr>
<td>C6</td>
<td>6×10⁸</td>
<td>High damping</td>
</tr>
<tr>
<td>C10</td>
<td>10×10⁸</td>
<td>High damping</td>
</tr>
</tbody>
</table>

(a) List of damping values used in the simulations

Fig. 8 List of damping values and hydrodynamic efficiency with a constant wave height $H = 0.06$ (Kamath et al., 2015)
performance using a 2D RANS-VOF numerical model. The comprehensive performance of an OWC was calculated by determining the optimal turbine damping coefficient for the impulse turbine model in a real sea environment (A Guarda Port in Spain).

Elhanafi et al. (2016) verified the numerical model by comparing the water surface displacement inside a chamber of the OWC with the wave tank experiment results using the fully nonlinear 2D RANS model and confirmed that the PTO damping coefficient and increase in the wave height play key roles in the generation of the vortex at the chamber inlet. In a subsequent study (Elhanafi et al., 2017), the RANS-VOF numerical model was used to investigate the effects of air compression on the OWC performance in the models, whose size scale ranged from 1:50 to actual size. Dai et al. (2019) compared the experimental results of two small- and large-wave tanks with the CFD calculation results to identify whether the hydrodynamic scale effect can be reproduced based on the CFD analysis and confirmed that the Reynolds number has a major impact.

Several studies on a multi-chamber OWC or the alteration in sea floors have been reported. Rezanejad et al. (2019) created sea floor stepped bottom at the chamber front to improve the efficiency of an OWC device using OpenFOAM and compared the results with the wave tank experiment for verification. Energy extraction and flow pattern characteristics around the floor step were presented based on the CFD analysis results. Mohapatra and Sahoo (2020) analyzed the effects of sea floor steps on the hydrodynamic performance of the OWC using Fluent. A PTO device was modeled using a porous zone to demonstrate the characteristics of an actual air turbine. The performance of an OWC was improved when the floor step was present, which corresponds to the results of one of the previous studies (Rezanejad et al., 2013) conducted using the BIEM. Shalby et al. (2019) developed an incompressible 3D CFD model for simulating a fixed multi-chamber OWC using Star-CCM+ and compared its results with the results of a wave tank experiment conducted at a 1:25 scale. A PTO system possessing an intermediate level of damping has the maximum Capture width ratio (CWR) in most periods excluding long waves.

Mahnamfar and Altunkaynak (2017) created a model considering various inclined angles (30°–47°) using FLOW 3D and compared the results with those of the wave tank experiment based on the Nash–Sutcliffe coefficient of efficiency as a performance evaluation measure. Park et al. (2018a) verified the reliability of the CFD analysis method by comparing the open chamber of an inclined OWC and the chamber model comprising an orifice against the 2D wave tank experiment results using Star-CCM+ (Fig. 9). The turbine effect was considered for the model with an orifice, and therefore, the wave elevation inside the chamber was decreased. Gaspar et al. (2020) used Fluent to compare a vertical chamber and a 40°-inclined chamber of OWC. The PTO system considered the Wells turbine, and the incident wave generation scheme considering active absorption was applied. The numerical analysis demonstrated that run-up/-down and sloshing occurred more evidently and the energy efficiency was higher in peak periods inside the inclined chamber. In a vertical chamber condition, the changes in energy extraction efficiency according to incident wave periods were not significantly high.

Liu et al. (2009a; 2009b) verified the integrated analysis of the interaction among the OWC, air chamber, and impulse turbine using an orifice model through CFD. A numerical wave tank was built based on the two-phase VOF model for generating 3D incident waves. The parameters affecting the energy conversion efficiency and performance of the WEC system, e.g., incident wave period, wave height, water depth, and duct diameter, were examined, and the results were compared with the results of the experiment conducted using a 50 m x 0.8 m x 1.2 m (length, width, and depth) wave tank. Bouali and Larbi (2017) used the commercially available CFX program to develop a numerical wave tank of the 3D fully nonlinear RANS-VOF model and developed the sequential optimization procedure for major parameters, e.g., PTO damping, structural characteristics, and incident wave conditions for the optimization of an OWC. Zhang et al. (2012)
developed a numerical technique based on the 2D two-phase model using the level-set immersed boundary method to analyze the water surface displacement and pneumatic pressure of the flow field inside the chamber. Table 2 provides the comparison of CFD codes and OWC model types used in the previous studies mentioned in this paper.

### 2.4 Research on U-OWC

Studies have been actively conducted on U-OWC, in which a U-shaped U-OWC is developed by installing a structure in front of the chamber for enhancing energy extraction efficiency and water surface displacement inside the OWC chamber. The concept of the U-OWC was first proposed by Boccotti (2003). Boccotti (2007a) compared U-OWC and conventional OWC and confirmed that the amplitude of pressure variation inside the chamber increases while the natural period of water surface displacement inside the chamber extends in the U-OWC. Then, it was confirmed that the performance of U-OWC is outstanding under small- and large-wave conditions. Boccotti (2007b) performed a theoretical analysis of the OWC installed on the caisson breakwater (Fig. 10). Boccotti et al. (2007) conducted an experiment on U-OWC for a 1:10 scaled model on the east coast of the Strait of Messina and compared the results with the theoretical results of Boccotti (2007b).

Strati et al. (2016) introduced the first real-sea-area U-OWC prototype (2.7 MW) installed at the Civitavecchia Port in Rome, Italy, and studied the optimization of the extraction performance of the U-OWC equipped with the Wells turbine by controlling the turbine under various incident wave conditions (Fig. 11). Arena et al. (2013a) compared the wave conditions of Civitavecchia (Rome) and Pantelleria (Sicily), which are two Italian coasts suitable for installing a U-OWC prototype. In 2012, Arena et al. (2013b) explained the construction process and design of the U-OWC breakwater installed at the Civitavecchia Port.

Vyzikas et al. (2017a) compared the energy extraction efficiency by applying geometric revisions to the conventional vertical OWC and U-OWC proposed by Boccotti (2003) to propose an OWC having various sloped attachments (Fig. 12). Thus, it was confirmed that the performance of U-OWC was better than the conventional vertical OWC. Malara and Arena (2013) proposed a modeling process for a wave field that interacts with the U-OWC under random incident wave conditions based on a linear wave theory. The drawbacks of existing theoretical modeling were resolved, and a hydrodynamic memory effect, which was disregarded in the previous modeling process was included. Malara et al. (2017) attempted to verify the reliability of a mathematical model based on the unsteady Bernoulli equation to estimate the response of the U-OWC in the time domain. For such reason, an experiment was conducted at the Reggio Calabria coast in

### Table 2 Comparison among previous studies with regard to the CFD analysis

<table>
<thead>
<tr>
<th>Reference</th>
<th>CFD code</th>
<th>OWC Type</th>
</tr>
</thead>
<tbody>
<tr>
<td>Kamath et al. (2015)</td>
<td>REEF3D</td>
<td>Vertical chamber</td>
</tr>
<tr>
<td>Iturrioz et al. (2015)</td>
<td>OpenFOAM</td>
<td>Vertical chamber</td>
</tr>
<tr>
<td>Teixeira et al. (2013)</td>
<td>Fluent</td>
<td>Vertical chamber</td>
</tr>
<tr>
<td>López et al. (2014)</td>
<td>Star-CCM+</td>
<td>Vertical chamber</td>
</tr>
<tr>
<td>Rezanejad et al. (2019)</td>
<td>OpenFOAM</td>
<td>Stepped sea bottom</td>
</tr>
<tr>
<td>Mohapatra and Sahoo (2020)</td>
<td>Fluent</td>
<td>Stepped sea bottom</td>
</tr>
<tr>
<td>Shelby et al. (2019)</td>
<td>Star-CCM+</td>
<td>Multi-chamber</td>
</tr>
<tr>
<td>Mahnamfar and Altunkaynak (2017)</td>
<td>FLOW 3D</td>
<td>Sloped chamber</td>
</tr>
<tr>
<td>Park et al. (2018a)</td>
<td>Star-CCM+</td>
<td>Sloped chamber</td>
</tr>
<tr>
<td>Gaspar et al. (2020)</td>
<td>Fluent</td>
<td>Sloped chamber</td>
</tr>
</tbody>
</table>

![Fig. 10 Caisson breakwater embodying a U-OWC (Boccotti, 2007b)](image1.png)

![Fig. 11 U-OWC plant and key geometrical characteristics (Strati et al., 2016)](image2.png)
Fig. 12 Four lid-on devices for experimental tests (Vyzikas et al., 2017a)

Italy, and the results were compared with numerical analysis results. Through this study, the Darcy-Weisbach-based chamber head loss model, which is traditionally used in a steady flow analysis, has a problem of over-prediction than the experiment. To overcome this limitation, the use of an instantaneous acceleration-based model including an abnormal head loss proportional to water column inertia was proposed. Ashlin et al. (2019) performed the response analysis of water surface displacement inside the U-OWC chamber using REEF3D, which is described as a 2D CFD numerical wave tank model. The RANS equations, k-omega turbulence model, and level-set free surface method were used. The calculation results matched those measured through wave tank experiments.

Ning et al. (2020) applied the U-OWC using a 2D fully nonlinear numerical model based on a high-order BEM and performed a geometric parameter analysis of structures, i.e., the height and width of a vertical duct in the time domain (Fig. 13). The calculation results agreed well with the published experimental results, in which the pneumatic pressure inside the chamber and hydrodynamic efficiency ($\eta$ in Fig. 13) increased as the submerged vertical duct height and wall thickness increased. However, the efficiency gradually diminished as the wall thickness increased. Belibassakis et al. (2020) examined the performance of an OWC considering the interaction between the sea floor terrain of the installation area and incident waves using the BEM. The resonance period of the OWC chamber was adjusted by installing an additional vertical wall in front of the OWC, and the effects of other parameters, e.g., chamber size and depth changes, were explained. Furthermore, the possibility of installing an OWC at a coastal port in Romania with sufficient wave energy was confirmed. Tsai et al. (2018) calculated the OWC model linked with fixed breakwater using Fluent and verified the results by comparing them with the experimental results. They installed a perforated wall at the front section of an OWC to create a U-type flow for improving the energy extraction efficiency of an OWC and further proposed a new model capable of reducing a wave force applied to the skirt-shaped structure of an OWC.

2.5 OWC Real-sea area Model

Various studies on the verification of numerical analysis and experimental results for a fixed OWC WEC model and the establishment, installation, and operation of real-sea area models have been reported. First, Falnes (1993) installed the prototype of an initial OWC at Toftestallen in Norway (Fig. 14).

In 1999, Falcão (2000) installed a 400-kW OWC WEC operated using the Wells turbine on the coast of Pico Island in Portugal (Fig. 15). Two ducts were installed and connected in a series with a turbine for better stability.

Fig. 14 Kvaerner OWC constructed on a cliff at Toftestallen (Falnes, 1993)

Fig. 15 Back view of the Pico OWC showing the exit from the turbine (Falcão, 2000)
Moreover, in 1999, Heath et al. (2000) installed a 500-kW land-installed marine power energy transmitter (LIMPET) OWC on Islay Island (Scotland) and reported the operation results (Fig. 16). LIMPET, which succeeds the 75-kW OWC prototype installed in the UK near the Islay Island, Scotland, in 1991 by Queen's University of Belfast (QUB), was constructed to solve various problems arising prior to commercial development. LIMPET includes a rectangular inclined OWC having the maximum output of 500 kW, which induces air flows generated through two contra-rotating Wells turbines connected to a 250-kW induction generator.

Zhang et al. (2009) investigated the wave energy technology development in China and reported the wave energy development outlook (Fig. 17). In particular, a 100-kW OWC demonstration plant operated on the Zhelang coast in Shanwei, Guangdong, which was the only large-scale project that was integrated with the power grid in China at that time.

Torre-Enciso et al. (2009) reported the processes from the establishment of the breakwater-linked OWC concept in the Mutriku Port in northern Spain to project completion (Fig. 18). The Mutriku wave energy plant comprises 16 chambers, in which each upper opening is connected to a turbo generator set with a rate capacity of 18.5 kW, thus generating a total output of 296 kW. This plant was the first multi-chamber facility, and the first commercial project in which a technical company sold an energy converter to investors for commercial operations.

Kihara et al. (2019) installed a prototype of a projecting-wall OWC (PW-OWC) at the Sakata Port in Japan. The characteristics of the
impulse turbine in constant airflow ($\dot{Q}$) were identified, and the turbine size was found to be independent of the constant airflow ($\dot{Q}$) and pressure difference ($\Delta P$).

Research and experiments are actively conducted on real-sea WECs in Korea. Choi et al. (2018) reported that power is generated through a test operation of a 500-kW water wave energy plant, which is a pilot plant (before the construction of a prototype) (Fig. 19). Moreover, Supervisory Control and Data Acquisition (SCADA) comprising marine monitoring, facility monitoring, and environmental monitoring systems is in operation. The marine monitoring system controls ships, and the facility monitoring system remotely controls the electrical and mechanical devices of a marine substation. The environmental monitoring system collects weather and marine data.

Lim et al. (2021) reviewed the method for estimating a wave load applied to the front part of an OWC linked with breakwater, which will be installed at Mok-ri Port in Chujado Island, Jeju, Korea (Fig. 20). A wave load applied to the OWC structure installed at the front part of the inclined breakwater was estimated based on the port and fishing port design standards; the CFD-based numerical results were compared with the results of the wave pressure calculations or 2D model experimental results to examine the stability of the structure. Meanwhile, a 30-kW OWC WEC linked with breakwater in Chujado Island is associated with an energy storage system to be used in the island area, and the pilot operation of the prototype will continue until 2027.

3. Conclusion

This study focused on the primary energy conversion of a fixed OWC WEC and explained current R&D achievements in five research categories: (1) potential flow-based numerical analysis, (2) wave tank experiments, (3) CFD analyses considering fluid viscous effects, (4) U-OWC studies that can amplify water surface displacement in the OWC chamber, and (5) studies on OWC prototypes that have been installed and operated in real sea environments. Hence, the research achievements proposed in this study will provide basic research informations for researchers planning to conduct detailed research on an OWC WEC. However, there are research achievements that have been omitted or missing owing to time and space constraints to investigate and describe all the related studies. In particular, the secondary energy conversion of a WEC will be further discussed in future studies. Therefore, readers are advised to keep this in mind while reviewing the content of this paper.

Conflict of Interest

Weoncheol Koo serves as an editor of the Journal of Ocean Engineering and Technology but has no role in the decision to publish this article. No potential conflict of interest relevant to this article was reported.

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References


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Corrigendum to: Change in Turning Ability According to the Side Fin Angle of a Ship Based on a Mathematical Model

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Corrigendum to: Journal of Ocean Engineering and Technology, 36(2), 91-100, April, 2022
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This corrects the article “Change in Turning Ability According to the Side Fin Angle of a Ship Based on a Mathematical Model” in Volume 36 on page 100.

There is an error in Funding section in the above article, and it is corrected as follows.

\textbf{Funding}

This research was funded and conducted under the Competency Development Program for Industry Specialists of the Korean Ministry of Trade, Industry and Energy (MOTIE), operated by Korean Institute for Advancement of Technology (KIAT). (No. P0012646, HRD program for Global Advanced Engineer Education Program for Future Ocean Structures) and Basic research project (No. 2020R1F1A1071610) supported by the National Research Foundation with funding from the Ministry of Communications and CO2 (DFOC) reduction based on the real operation of medium-sized ships conducted with the funding of the Ministry of Trade, the Industry and Energy’s “Medium Shipyard Innovation Growth Development Project” with the support of Technology Development (Project No.: 20007847), and research project of Inha University (Project No.: 62968).

\textbf{Author ORCIDs}

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<tbody>
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<td>Lee, WangGook</td>
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**Acknowledgments**

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KEY WORDS: Lumped mass line model, Explicit method, Steel lazy wave riser (Provide a maximum of 5 or 6 keywords.)

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Received 00 February 2100, revised 00 October 2100, accepted 00 October 2100
Corresponding author Firstname Lastname: +82-51-759-0656, e-mail@e-mail.com
It is a recommended paper from the proceedings of 2019 spring symposium of the Korea Marine Robot Technology (KMRITS).

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<th>Item</th>
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<td>Inner diameter (m)</td>
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<td>Axial stiffness (N)</td>
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<td>Inner flow density (kg·m(^3))</td>
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<tr>
<td>Seabed stiffness (N/m/m(^2))</td>
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</table>

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<th>Author name</th>
<th>ORCID</th>
</tr>
</thead>
<tbody>
<tr>
<td>So, Hee</td>
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</tbody>
</table>
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