An End-to-End Simulator for the All-Electric Ship MVDC Integrated Power System

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Abstract
In this paper, a large scale Medium Voltage DC all-electric ship integrated power system is modeled from the prime mover (gas turbine) to the propulsion load. This system has a three-phase 21MW synchronous machine as a main generator and a three-phase 19MW induction motor as a main propulsion drive. The influence of propeller emergence on both electrical and mechanical components of the system is investigated.

1. INTRODUCTION
In the new All-Electric Ship (AES) Integrated Power System (IPS), there is an increasing demand for ship system automation, electrical weaponry, electric propulsion, and ship service distribution. About 70% to 90% of power from the generator units in the fully integrated power system is consumed in the propulsion systems [1]. The power distribution must yield the most efficient power usage to maintain continuity of service in the case of large power demand or critical situation [2].

This complex problem is compounded by the sea states which are unpredictable and uncontrollable. Therefore, it is challenging to investigate interactions between machines, power electronics interface, and uncertain sea conditions. In [3], the interactions between the various components of a large scale AC integrated power system are presented. However, there have been no published papers that account for dynamic interactions of propeller emergence with a large scale Medium Voltage DC (MVDC) shipboard integrated power system. In this paper, a detailed model of the MVDC IPS, and the propeller coupled to random wave dynamics and ship motion are developed.

This paper describes modeling and simulation of a large scale AES MVDC IPS. The model is based on the MVDC (750V) Testbed (MVDC'T) that has been constructed at Purdue University [4], where the main generation system consists of a 59kW wound rotor synchronous machine and the main load is 37kW propulsion system. The IPS modeled in this paper is the ‘scaled-up’ Purdue system, together with the models of both a gas turbine and a full propeller model. In the large scale MVDC IPS the main generator unit is a three-phase 21MW synchronous machine and the propulsion drive is a three-phase 19MW induction motor. The distribution MVDC bus is 5kV. In addition, a gas turbine is modeled as a prime mover for the system. The Matlab/Simulink platform is used for modeling and simulation purposes.

2. TOPOLOGY AND SYSTEM COMPONENTS
To investigate the influence of the propeller loading on the propulsion system and power generation in the electric ship, modeling of both power system and hydrodynamics including the propeller must be incorporated. The overall diagram of an MVDC integrated power system is given in Fig.1. The MVDC system is similar to the AC system described in [5] for a ship like the DDG51 destroyer. In summary, the main generation unit is a three-phase 21MW synchronous machine with a gas turbine as a prime mover, and a three-phase 19MW induction motor as a propulsion drive.

2.1. Generation System
The Generation System includes: a gas turbine with speed governor, a three-phase 21MW synchronous machine, a three-phase rectifier with an output low-pass filter, and a DC voltage controller.

Gas Turbine
The gas turbine is a prime mover for a three-phase synchronous machine. The turbine response has an important in-
fluence on the dynamic performance of an electrical system. For example, in the case of a fault in the system, depending on the action of turbine valve, the unit may stay in the system or it may lose synchronism and be disconnected. Therefore, maintaining the system frequency is one of the concerns in isolated power systems such as AES IPS.

The gas turbine model used here is based on a model proposed by Rowen in [6]. The temperature control is neglected. The simplified block diagram for a single shaft gas turbine together with its control and fuel system is presented in Fig.2.

Figure 2. Simplified single-shaft gas turbine model

The gas turbine control system includes speed control, upper and lower fuel limits and valve position. $V_{CE}$ is per unit fuel demand, $K_a$ is the fuel consumption at no load, $W_F$ is fuel flow per unit, and $f_2$ is turbine function whose inputs are fuel flow and turbine speed to produce a value of turbine torque, $f_2 = a_{f2} + b_{f2}W_F - c_{f2}\omega_r$. The per unit value for $V_{CE}$ corresponds to the per unit value for the turbine power output. For example, if mechanical power is 1 pu, the steady state value for $V_{CE}$ is 1 pu [7]. Therefore, the gain $K_3 = 1/b_{f2}$ and $a_{f2} = K_a b_{f2}$. The speed governor is PI controller with its proportional and integral constants, $K_p$ and $K_i$, respectively. The value of $V_{CE}$ is scaled by $K_3$ and offset by a value given by $K_a$. Fuel flow control results in turbine torque $T_m$. The gas turbine parameters are given in Table 1.

### Synchronous Machine

The generator is a three-phase wound rotor synchronous machine with brushless excitation system. The rotor windings consist of the field windings ($f_d$) and damper windings ($k_q$ and $k_d$). The mathematical equations in the $qd$ reference system are given in [8]. The synchronous machine parameters are given in Table 2.

### Table 1. Gas turbine parameters

<table>
<thead>
<tr>
<th>$K_p$</th>
<th>$K_i$</th>
<th>$K_a$</th>
</tr>
</thead>
<tbody>
<tr>
<td>8</td>
<td>3</td>
<td>0.23</td>
</tr>
<tr>
<td>$a_{f2}$</td>
<td>$b_{f2}$</td>
<td>$c_{f2}$</td>
</tr>
<tr>
<td>0.201</td>
<td>1.3</td>
<td>0.5</td>
</tr>
</tbody>
</table>

### Table 2. Synchronous machine parameters

<table>
<thead>
<tr>
<th>$R_s$</th>
<th>$R_{fd}$</th>
<th>$R_{kd}$</th>
</tr>
</thead>
<tbody>
<tr>
<td>1.27mΩ</td>
<td>401mΩ</td>
<td>4.74mΩ</td>
</tr>
<tr>
<td>$R_{kd}$</td>
<td>$L_d$</td>
<td>$L_{mq}$</td>
</tr>
<tr>
<td>5.26mΩ</td>
<td>391µH</td>
<td>2.51mH</td>
</tr>
<tr>
<td>$L_{md}$</td>
<td>$L_{fd}$</td>
<td>$L_{ld}$</td>
</tr>
<tr>
<td>2.79mH</td>
<td>227µH</td>
<td>69.8µH</td>
</tr>
<tr>
<td>$L_{lkq}$</td>
<td>$P$</td>
<td>$f_h$</td>
</tr>
<tr>
<td>157µH</td>
<td>2</td>
<td>60Hz</td>
</tr>
</tbody>
</table>

### Three-phase Rectifier

The AC output components of the synchronous machine are connected to a passive diode three-phase rectifier to obtain DC voltage bus. There is no control for the rectifier and the diodes will conduct only when they are positively biased. However, the detailed model of the rectifier given by the Matlab/SimPower Systems Toolbox is used.

### Low-pass Filter

The rectifier output ($r_{Ldc1}, L_{dc1}, C_{dc1}, r_{Ldc2}, L_{dc2}, r_{Pdc}, C_{dc2}$ and $r_{ESR}$) and inverter input ($r_{L}, L, R_P, C_m$ and $r_{ESR}$) low-pass LC filters (Fig.1) are included to reduce the high frequency harmonics from propagating into the distribution DC bus [4]. The parameters of the low-pass filters are: $r_{Ldc1} = r_{Ldc2} = r_{Pdc} = 0.075\Omega$, $L_{dc1} = L_{dc2} = 20mH$, $C_{dc1} = C_{dc2} = 100\mu F$, $L = 50mH$, $R_P = 100\Omega$, $C_m = 10\mu F$, and $r_{ESR} = 1\Omega$. For the detailed model of the diode rectifier, see [9].
\[ L_{dc1} = L_{dc2} = 0.5 \text{mH}, \ C_{dc1} = 2.3 \text{mF}, \ C_{dc2} = C_{in} = 1.4 \text{mF}, \]
\[ r_p C_{dc} = 8k\Omega, \ r_L = 0.5\Omega, \ L = 0.226 \text{mH}, \ R_D = 40k\Omega \text{ and } \]
\[ r_{ESR} = 0.1824\Omega. \]

**DC Voltage Control**

The DC bus voltage control block is given in Fig.3. \( V_{dc}^\ast \) is commanded (reference) voltage and \( V_{dc} \) is the measured DC bus voltage. The PI controller is equipped with an anti wind-up structure. The closed loop feedback path becomes open as soon as the limits of the dynamic saturation are reached. The integral part of the PI controller is an unstable element if open loop and hence it must be stabilized by an anti-wind-up structure when a saturation occurs. The output of the DC bus control is the commanded excitation voltage \( E_{fd} \) of the synchronous machine.

**2.2. Propulsion System**

The Propulsion System is driven by a three-phase 19 MW induction motor with slip frequency current control. A three-phase inverter connects the induction motor with the DC bus. The three-phase inverter is controlled with the hysteresis current control method.

**Induction Motor**

The three-phase 12-pole 19MW induction motor is the propulsion drive. The voltage equations of the induction motor stator and rotor windings are given in [8]. The parameters for the three-phase induction motor are converted from the 15-phase induction motor used in the large scale AC IPS [3]. It is assumed that both induction motors are operating at the same phase voltage. The number of stator turns must be equal [9]. The winding factors are different because the three-phase winding is in five slot locations to every one slot location for the 15-phase winding. This implies a winding factor \( k_\phi \) that incorporates the distribution phases. The phase spacing is \( \gamma = 180^\circ / 15 = 12^\circ \). The winding factor is given as:

\[ k_\phi = \frac{\sin 5\frac{\gamma}{2}}{5 \sin \frac{\gamma}{2}} \approx 0.957 \]  \hspace{1cm} (1)

This implies that the number of turns in the three-phase winding is:

\[ N_\phi(3) = \frac{N_\phi(15)}{k_\phi} \]  \hspace{1cm} (2)

Then the ratio of magnetizing inductances is:

\[ \frac{L_m(3)}{L_m(15)} = \frac{1}{5 k_\phi^2} \]  \hspace{1cm} (3)

The same scaling holds for the rotor parameters. If the total slot area is not changed, the stator resistance \( R_s \) will be proportional to the number of turns divided by five, since there is a total of five times as much area for the slot resistance. The number of turns is increased by the reciprocal of the winding factor. For stator leakage inductance \( L_{ls} \) for the three-phase machine the number of slots is five times that for the 15-phase machine.

The three-phase induction motor parameters are summarized in Table 3.

**Table 3.** Induction motor parameters

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>( R_s )</td>
<td>27,\text{m}\Omega</td>
</tr>
<tr>
<td>( R_r )</td>
<td>8,\text{m}\Omega</td>
</tr>
<tr>
<td>( f_b )</td>
<td>15,\text{Hz}</td>
</tr>
<tr>
<td>( L_{ds} )</td>
<td>1.5,\text{mH}</td>
</tr>
<tr>
<td>( L_{ds} )</td>
<td>1.7,\text{mH}</td>
</tr>
<tr>
<td>( L_M )</td>
<td>31.7,\text{mH}</td>
</tr>
</tbody>
</table>

**Three-phase IGBT/diode Inverter**

The three-phase IGBT/diode inverter is given by Matlab/SimPower Systems Toolbox. The gate input signal for controlled inverter consists of six firing signals for each IGBT, based on the inverter hysteresis current control output.

**Inverter Hysteresis Current Control**

The current control method applied here is the hysteresis control method. With this control method the switching of the inverter does not depend only on an external control signal but also on the momentary currents in the circuit.

The switching action is performed each time the current error reaches the limits of the hysteresis bandwidth defined in advance. The hysteresis bandwidth is defined in advance.

The inverter hysteresis current control block diagram for the phase current ‘a’ is given in Fig.4. The current error is fed into the hysteresis comparator and its output defines the switching signals for the corresponding inverter’s upper and lower phase switches (\( S_{a1}, S_{a2} \)). The controllers for the phases ‘b’ and ‘c’ are the same.

**Figure 4.** Inverter phase ‘a’ hysteresis current control

The average switching frequency depends on the width of the hysteresis bandwidth. When the hysteresis bandwidth is
small the switching frequency is high (because the current changes fast from the upper limit to the lower limit and vice versa), which means that the dominant current harmonics are of a higher order. The switching period increases (i.e., switching frequency decreases) with increasing hysteresis bandwidth.

**Induction Motor Controller**

The torque mode controller based on slip frequency current control is considered. The overall block diagram for the control of the induction motor torque is given in Fig.5. In this mode, the torque reference value is commanded directly. It consists of the Torque Trim Controller (TTC) and the Maximum Torque Per Amper controller (MTPA). The TTC ensures tracking between the commanded torque (\(T_{\text{ref}}\)) and the actual value of the torque (\(T_e\)). It does not allow fast changes in the commanded torque. The MTPA controller is based on Adaptive Maximum Torque Per Amper controller (AMTPA) [10] which requires an estimate of rotor resistance to determine the commanded currents (\(I_{abc}^*\)) and slip frequency (\(\omega_s^*\)). Our model assumes constant rotor parameters and no magnetic saturation. Therefore, the rotor resistance estimator is omitted from the AMTPA control technique.

The maximum torque that can be achieved with the set slip-frequency is given by:

\[
T_{e,\text{thresh}} = \frac{3}{2} P \frac{\omega_{s,\text{set}}^2 \lambda_{r,\text{max}}^2}{r} \tag{5}
\]

This controller also determines the reference rms value of the fundamental component of the stator current as:

\[
I^* = \sqrt{\frac{2 |T_e^*| |r^2 + (\omega_0 L_{tr})^2|}{3 P |\omega_0| L_{d}^2 r}} \tag{6}
\]

The selection of the slip frequency set point \(\omega_{s,\text{set}}\) that maximizes the torque is given by:

\[
\omega_{s,\text{set}} = \frac{r}{L_{tr}} \tag{7}
\]

**Propeller Model**

The propeller model used in this study is based on a five-blade, fixed-pitch, highly skewed propeller with a maximum skew angle of 32 degrees [11]. Several effects, such as fluctuation of in-line water inflow, ventilation, and in-and-out-of-water effects, can cause a reduction in the propeller thrust and torque from its nominal operating condition. Both the in-line water inflow variation and the in-and-out-of-water effects directly affect the propeller loading. The propeller emergence can abruptly reduce the propeller thrust due to a loss in the propeller effective disc area. In particular, when the ship maneuvers in rough sea conditions, the propeller emergence can occur intermittently, resulting in ship speed reduction, sudden increase in shaft angular speed, and rapid decrease in the motor’s current. Propeller thrust and torque loss can be represented by the thrust loss factor, \(\beta_T\), and the torque loss factor, \(\beta_Q\), multiplied by the open-water propeller thrust, \(T_p\), and torque \(Q_p\), respectively. According to [12], the thrust loss factor due to the propeller emergence is assumed to be proportional to the effective disc area as follows:

\[
\beta_T = \text{real} \left(1 - \frac{\arccos(\frac{h}{r})}{\pi} + \frac{h}{\pi} \sqrt{1 - \left(\frac{h}{r}\right)^2}\right) \tag{8}
\]

where \(h/r\) is the relative submergence, with the propeller shaft submergence \(h\) and the propeller radius \(r\). Thus, \(\beta_T = 1\) when the propeller is fully submerged. Also, \(\beta_Q\), corresponding to a reduction of the effective propeller disc area, is related to \(\beta_T\) as follows:

\[
\beta_Q = (\beta_T)^m, 0 < m < 1 \tag{9}
\]

where for an open-water propeller, \(m\) is typically within the range of 0.8 and 0.85, resulting in a larger \(\beta_Q\) than \(\beta_T\). The propeller efficiency is less than unity if the loss due to propeller emergence increases.
To model the wave effects, we assume that nonlinear and viscous effects are small compared to wave inertia for a ship motion in the sea. Moreover, we assume deep water random waves, modeled through the one-parameter Pierson and Moskowitz spectrum [13], derived on the basis of North Atlantic data and described by the significant wave height, $H^{1/3}$, and modal frequency, $\omega_n$.

To model ship hydrodynamics, we must consider the interaction between the propeller and ship hull because the wake created by the hull modifies the propeller advance velocity ($V_a$) [14] with $V_a = (1 - \omega)U$, where $U$ is the ship speed. The wake fraction ($0 < \omega < 0.4$) indicates the velocity reduction due to the wake. Moreover, the presence of the propeller thrust must be decreased by a factor $(1 - t)$, where $t$ is the so-called “thrust deduction factor” to account for the difference between the self-propelled and the towed-model resistance [14].

The specifications of the full-scale USS DDG-51 Arleigh Burke-Class [15], driven by the electrical propulsor, are given in Table 4. This full-scale ship is employed in the ship hydrodynamics calculations for calm water and added resistances, and for propeller performance.

### Table 4. Specification of the DDG-51 Arleigh Burke-class

<table>
<thead>
<tr>
<th>Parameters</th>
<th>full-scale DDG-51</th>
</tr>
</thead>
<tbody>
<tr>
<td>Length [m]</td>
<td>153.9</td>
</tr>
<tr>
<td>Draft [m]</td>
<td>9.45</td>
</tr>
<tr>
<td>Beam [m]</td>
<td>20.12</td>
</tr>
<tr>
<td>Displacement [tons]</td>
<td>8,300</td>
</tr>
<tr>
<td>Speed [knot]</td>
<td>31</td>
</tr>
<tr>
<td>Propeller Diameter [m]</td>
<td>5.2</td>
</tr>
<tr>
<td>Propulsion Power [hp]</td>
<td>100,000</td>
</tr>
</tbody>
</table>

In this study, we consider only the surge motion of the electric ship, driven by a propeller connected to an IPS, in head sea. The ship’s motions in other directions i.e. sway, heave, roll, pitch, and yaw are assumed to be small. Thus, the equation of ship motion with the induction motor directly driven the propeller can be described as:

\[
(M + M_a) \frac{dU(t)}{dt} = 2(1 - t)T_p(n, U) - R(U) - \bar{R}_AR(U,t) - R_{2nd-AR}(U,t) \tag{10}
\]

\[
J_s \frac{d\omega_s(t)}{dt} = T_e - \frac{Q_p(n, U)}{\eta_b \eta_r \eta_t} \tag{11}
\]

where $n$ denotes revolutions per minute of the propeller, $n = \omega \times 30/\pi$, and $M$ and $M_a$ are the ship mass and added mass in the surge direction, respectively. The rotational inertia, $J_s$, includes the induction motor rotor and propeller inertia. $T_p$ and $Q_p$ are the propeller open-water thrust and torque, respectively, i.e. $T_p = K_T(J_p)n^2D^4$ and $Q_p = K_Q(J_p)n^2D^5$, where $J = \frac{V}{n^2}$ is the advance velocity. Also, $K_T$ and $K_Q$ are thrust and torque coefficients of the propeller, while $\eta_b$, $\eta_r$, and $\eta_t$ denote the bearing, propulsive and shaft efficiencies. The drag force, $R$, corresponds to the calm-water resistance. It can be found from the non-dimensional drag coefficient, $C_D(R_e, F_r)$, as $R = \frac{1}{2}C_D\rho S U^2$ where $S$ is the wetted surface area. The drag coefficient $C_D$ [13] is composed of a frictional resistance coefficient $C_f(R_e)$ and a residual resistance coefficient $C_R(F_r)$, where $R_e$ and $F_r$ are Reynolds and Froude numbers, respectively. The added resistance, $R_{AR}$, is associated with the involuntary speed reduction due to the waves. Assuming that the added resistance is a second-order nonlinear system, obeying the properties of quadratic systems, the slowly-varying added resistance can be separated into two major components: mean or steady second-order force $\bar{R}_{AR}$ and slowly-varying second-order force $R_{2nd-AR}$ given by:

\[
R_{AR} = \bar{R}_{AR} + R_{2nd-AR} \tag{12}
\]

\[
R_{2nd-AR} = \frac{1}{2} \sum_{k=1}^{N} A^2 \{ H(\omega_k, -\omega_k) + \frac{1}{2} \sum_{l=1}^{N} R_e \{ A_k A_l H(\omega_k, \omega_l) e^{i(\Delta\omega + \Delta\phi)} \} \}
\]

where $\Delta\omega = \omega_k - \omega_l$ and $\Delta\phi = \phi_k - \phi_l$; $\omega_k, l$ are wave amplitudes and independent random phases varying between $(0,2\pi)$, corresponding to the wave frequency $\omega_k, l$, respectively. Note that $A_k$ can be calculated from the one-side sea spectrum $S(\omega)$ by discretizing $S(\omega)$ into $N$ equal intervals. Then, $A_k^2 = 2\int_{\omega_k}^{\omega_k+1} S(\omega)d\omega$ for $k = 1, \ldots, N$. $H(\omega_k, \omega_l)$ represents the second-order transfer function of hydrodynamics force. The response amplitude of the steady force normalized by the wave amplitude squared is denoted by $R(\omega)$; therefore, $R(\omega)$ is assumed to be equal to $\frac{1}{2}H(\omega_k, -\omega_k)$. According to [16], the added mean resistance is obtained from $R(\omega)$, and it is computed from the MIT5D Code [17]. For the slowly-varying added resistance, $H(\omega_k, \omega_l)$ can be approximated as $2R(\omega_k, \omega_l)$. From Newman’s approximation [14], $R_{2nd-AR}$ is associated with the diagonal terms of $H(\omega_k, \omega_l)$ if the difference of two frequencies, $\omega_k$ and $\omega_l$, is very small, e.g. $|\omega_k - \omega_l| < 0.2$.

### 3. VERIFICATION AND VALIDATION

In order to validate the modeling approach for the large scale MVDC IPS (5kV), a smaller system (750V) based on Purdue Testbed model [4] was modeled first (Fig.7).

Two cases were studied: 1. an additional load is added in parallel with the induction motor, and 2. a step change occurs in the torque command. The Purdue measurements and simulation results, for the first test case (added shunt resistance),
for the DC bus voltage and DC rectifier current are given in Fig. 8.

The simulation results for the case with additional shunt resistive load, for the smaller system (750V), which is modeled for validation purposes, are given in Fig.9, for the DC bus voltage and the rectifier DC current.

The Purdue measurements and simulation results for the second case (step change in the torque command) are given in Fig.10.

The simulation results for the case when torque command in changed, for the smaller system (750V), are given in Fig.11, for the DC bus voltage and the rectifier DC current.

For both studied cases, it can been seen that the simulation results for the case when torque command is changed - Purdue measurements and simulation results [18]

The propeller emergence leads to an increase in the induction motor rotor speed (Fig.13), the propeller rotational speed (Fig.14), and a minor decrease in the ship speed (Fig.14). The mean added resistance of about 0.33MN at 20 knots, and the slowly varying added resistance of the scaled electric ship are computed using Newman’s approximation and the added resistance response amplitude operator [19]. Thus, the propeller driven by IPS experiences the slow-varying force from the added resistance.

In this section, the simulation results of the effects of the propulsion system on the large scale IPS components are given. The effective disc area of the propeller is reduced when the propeller emerges out of water. Specifically, the propeller emerges at 20, 30, 45, 57, 67 and 85 seconds (Fig.12). The propulsion system is turned on with commanded torque of 1.48MNm, which results in the ship moving forward at about 20 knots. The mean added resistance of about 0.33MN at 20 knots, and the slowly varying added resistance of the scaled electric ship are computed using Newman’s approximation and the added resistance response amplitude operator [19]. Thus, the propeller driven by IPS experiences the slow-varying force from the added resistance.

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The induction torque (Fig.15), the generated active power, the electric ship excited by Pierson and Moskowitz spectrum with $H^{1/3} = 20 ft$ was simulated. When they are connected together, the synchronous machine and induction motor exhibit a strong interaction due to start up dynamics and they have to be turned on sequentially. Initially the synchronous generator operates near its steady-state conditions and at 5 seconds the propulsion system is turned on with commanded torque of 1.48MNm, which results in the ship moving forward at about 20 knots. The mean added resistance of about 0.33MN at 20 knots, and the slowly varying added resistance of the scaled electric ship are computed using Newman’s approximation and the added resistance response amplitude operator [19]. Thus, the propeller driven by IPS experiences the slow-varying force from the added resistance.

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Figure 12. Propeller thrust loss factor and propeller thrust

Figure 13. Synchronous machine and induction motor rotor speed

Figure 14. Propeller rotational speed and ship speed

Figure 15. Induction motor electromagnetic torque and propeller torque

Figure 16. Gas turbine fuel demand and fuel flow

Figure 17. Calm water resistance, mean added resistance and second order added resistance

Fig.17 illustrates the slowly-varying calm water resistance, mean added resistance and second order added resistance and their changes during propeller emergence. Notice that the ship speed (Fig.14) fluctuates at a rate similar to that of all resistances (Fig.17).
5. CONCLUSION

The large scale shipboard IPS, where the main voltage bus is the 5kV MVDC bus, was modeled and simulated. The influence of hydrodynamic force on the IPS, particularly in the surge motion, was investigated. The results show that the added resistance from the random sea state has a small effect on the ship forward speed as well as the induction motor variables. The propeller emergence has no significant influence on the generated active power or the DC bus voltage. However, it has an influence in the gas turbine fuel flow and demand. Nevertheless, there are strong dynamic interactions between the synchronous machine and the induction motor during the induction motor start-up transient.

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Biography

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