The concept of anti-spin thruster control

Øyvind N. Smogeli*, Asgeir J. Sørensen, Knut J. Minsaas

Department of Marine Technology, Norwegian University of Science and Technology, NO-7491 Trondheim, Norway

Received 10 December 2004; accepted 21 June 2006
Available online 14 August 2006

Abstract

Experimental results for a ducted propeller with varying submergence and loading at low advance velocities are presented, and a simulation model for propellers subject to ventilation and in-and-out-of water effects is proposed. Motivated by this, the concept of anti-spin control for thrusters operating in extreme seas is introduced. Two operational regimes are defined: normal conditions and extreme conditions. Alternative thruster control schemes for normal conditions are possible: conventional shaft speed control, torque control, power control, or combinations of these. In extreme conditions the thruster may be subject to severe thrust losses, which can lead to propeller racing. When such losses are detected, an anti-spin control action which counteracts the propeller racing is proposed. The anti-spin control concept is shown to give increased control of the thruster performance in extreme seas, especially when using torque or power thruster control. The results indicate that a combination of torque or power control with an anti-spin control scheme will improve thrust production, reduce mechanical wear and tear, and reduce power system transients in both operational regimes.

Keywords: Propulsion control; Anti-spin regulation; Marine systems; Propellers; Thrusters; Dynamic positioning

1. Introduction

Vessels operating in extreme environmental conditions, like offshore service vessels, shuttle tankers, drilling vessels, pipe-laying vessels, and diving support vessels, rely on their propellers and thrusters for safety and operational performance. For vessels with electric propulsion systems, the thrusters are usually the main power consumers. To avoid black-outs, it is essential to have enough spinning power reserves to meet the power peaks. The vessel power management system, therefore, depends on robust performance and predictable power consumption from the thruster units to optimize power distribution and fuel consumption.

In extreme conditions, the thrusters are often subject to ventilation and in-and-out-of water effects. In addition to loss of thrust and undesirable power transients, industrial experience shows that the high thrust losses may lead to severe mechanical wear and tear of the propulsion unit (Koushan, 2004). The results are costly repairs and vessel down-time.

The station-keeping control system of a marine vessel may conceptually be divided into the high-level control system, including thrust allocation, and the low-level thruster control systems. The high-level control system, which could be a dynamic positioning (DP) system, a position mooring (PM) system, or a joystick control system, computes the desired forces in surge and sway, and moment in yaw. The thrust allocation system calculates the thruster setpoints according to some optimization criteria such that the high-level control commands are fulfilled, see e.g. Fossen (2002). The main purpose of the low-level thruster controllers is to control the thrusters to produce the thrust forces given by the setpoints from the thrust allocation system. This is essential for the total performance of the vessel.

In the design of vessel control systems, most effort has traditionally been put into the high-level control schemes. More recently, also the issue of local thruster dynamics and control has received increased attention, but this work has mostly been focused on underwater applications like remotely operated vehicles (ROVs) and autonomous

The concepts of torque and power control of electrically driven thrusters for surface vessels as presented in Sørensen, Ådènås, Fossen, and Strand (1997), Smogeli, Sørensen, and Fossen (2004a) and Smogeli, Ruth, and Sørensen (2005) will be summarized here, but a detailed analysis is considered outside the scope of this paper. Instead, these references will serve as evidence of the advantages of torque and power control over conventional shaft speed control in normal operating conditions. The proposed controller structure has been inspired by the anti-spin control system of a car, see e.g. Haskara, Özgün, and Winkelman (2000) or Johansen, Kalkkühl, and Petersen (2001). It is believed that anti-spin thruster control in particular will be important for tunnel thrusters and main propellers on ships, where ventilation and in-and-out-of water effects may occur during rough environmental conditions. For deeply submerged propellers, e.g. azimuthing thrusters on semi-submersibles, ventilation may not be of any concern. The current presentation of anti-spin thruster control is intended for electrically driven speed controlled fixed pitch propellers (FPP). However, the concepts are extendable also to electrically driven variable speed controllable pitch propellers (CPP). For mechanically direct driven propellers and fixed speed CPP, anti-spin control may be difficult to implement due to slower response of the diesel engine and pitch actuator when compared to an electric motor.

This paper is an extended version of Smogeli, Aarseth, Overå, Sørensen, and Minsaas (2003), in which the concept of anti-spin thruster control for station-keeping of marine vessels was first introduced. Since this publication the work on anti-spin control has continued, and new results have been published in Smogeli et al. (2004a,2005) and Smogeli, Hansen, Sørensen, and Johansen (2004b). This text is focused on the concept of anti-spin control; details of implementation and stability analysis are not considered.

The paper starts with formulating the mathematical model of a thruster subject to losses in Section 2. Experimental results for a thruster subject to ventilation losses are presented in Section 3. In Section 4 speed, torque, and power controllers are presented, and two anti-spin control schemes proposed. Simulation results for the various control schemes are presented in Section 5, before conclusions are given in Section 6.

2. Mathematical modelling

The actual propeller thrust $T_a$ and torque $Q_a$ can in general be formulated as functions of fixed thruster parameters $\theta_p$ (i.e. propeller diameter, position, number of propeller blades, propeller blade expanded-area ratio), variables $x_p$ (i.e. pitch ratio, advance speed, submergence), and the shaft speed $n$:

$$T_a = f_T(n, x_p, \theta_p),$$

$$Q_a = f_Q(n, x_p, \theta_p).$$

In the following, only FPP will be considered; the pitch ratio will in this case be a fixed parameter. The functions $f_T(\cdot)$ and $f_Q(\cdot)$ may include loss effects due to e.g. in-line and transverse velocity fluctuations, ventilation, in-and-out-of water effects, the so-called Coanda effect, and thruster–thruster interaction. Research results on thrust losses may be found in e.g. Shiba (1953), Fleischer (1973), Minsaas, Faltinsen, and Persson (1983), Minsaas, Thon, and Kauczynski (1986, 1987), and Lehn (1992).

2.1. Motor and shaft dynamics

In this work, a one-state model for the propeller rotational dynamics is used. It is defined by the moment of inertia for shafts, gears, and propeller $I_s$, the linear friction coefficient $K_o$, the angular velocity $\omega$, the motor torque $Q_m$, and the propeller load torque $Q_a$ according to

$$I_s \ddot{\omega} = Q_m - Q_a - K_o \omega. \quad (3)$$

More sophisticated models, where also flow dynamics are included by modelling a control volume of water around the thruster as a mass-damper system, were developed and analyzed by Yoerger et al. (1991), Healey et al. (1995), Whitcomb and Yoerger (1999a), Bachmayer et al. (2000), and Blanke et al. (2000). The models were shown to explain some of the transient behavior of the thruster. Also, it was shown that the performance of a shaft speed controller could be improved by accounting for such effects. This has not been further treated in the current work, since the flow dynamics model was developed for deeply submerged thrusters. During ventilation, which is the main focus here, the flow dynamics model would not be valid.

The torque control loop is inherent in the design of most applied control schemes for variable speed drive systems. The torque is controlled by means of motor currents and motor fluxes with high accuracy and bandwidth. Theoretically, the rise time of the torque in PWM (pulsed with modulated) drives is limited by the motor inductance (in load commutated inverters, LCIs, also by the DC choke). However, in practice the controller limits the rate of change of torque in order to prevent damages on the mechanics. The closed loop of motor and torque loop controller may for practical reasons be assumed to be equivalent with a first-order model (Sørensen et al., 1997):

$$\dot{Q}_m = \frac{1}{T_m} (Q_c - Q_m), \quad (4)$$

where $Q_m$ is the motor torque, $T_m$ is the motor time constant in the range of 20–200 ms, and $Q_c$ is the commanded torque.
from the propeller controller. In Fossen and Blanke (2000), it was shown how the motor dynamics for current controlled, voltage controlled, and torque controlled DC motors could be modelled by a unified DC-motor model with control input $Q$, and state $\omega$. It was here argued that the electric time constant for such motors is small compared to the mechanical time constant, and that the motor dynamics, therefore, is dominated by the rotational dynamics of the shaft (3). The simplified model in (4) should hence be appropriate for the applications studied here.

### 2.2. Propeller characteristics

The relationships between the actual propeller thrust $T_a$, torque $Q_a$, shaft speed $n = \omega/2\pi$, diameter $D$, and density of water $\rho$ are commonly given by (Carlton, 1994):

$$T_a = sgn(n)K_T\rho D^4n^2,$$  \hspace{1cm} (5)

$$Q_a = sgn(n)K_\Omega\rho D^5n^2.$$  \hspace{1cm} (6)

$K_T$ and $K_\Omega$ are strictly positive thrust and torque coefficients, where the effects of thrust and torque losses have been accounted for. The inclusion of loss effects in $K_T$ and $K_\Omega$ is not conventional, but is convenient for consistency in the description of the propeller characteristics. The actual propeller power consumption $P_a$ may be written as:

$$P_a = 2\pi nQ_a = sgn(n)2\pi K_\Omega\rho D^5n^3.$$  \hspace{1cm} (7)

The characteristics of a deeply submerged propeller is commonly given by thrust and torque coefficient curves as functions of the advance number $J_a$:

$$J_a = \frac{V_a}{nD},$$  \hspace{1cm} (8)

where $V_a$ is the propeller advance velocity. This relationship is referred to as an open-water propeller characteristics, and is valid for the first quadrant of operation ($n \geq 0, V_a \geq 0$). Fig. 1 shows an example for a Wageningen B-series propeller (Oosterveld & Ossenm, 1975). The corresponding open-water efficiency $\eta_0$ is defined as the ratio of produced to consumed power for the propeller:

$$\eta_0 = \frac{\frac{V_aT_a}{2\pi nQ_a}}{\frac{V_aK_T}{2\pi K_\Omega}} = \frac{J_aK_T}{2\pi K_\Omega}.$$  \hspace{1cm} (9)

The nominal thrust $T_n$, torque $Q_n$, and power $P_n$ for a deeply submerged propeller with zero advance velocity and no thrust losses are conveniently expressed by the nominal thrust and torque coefficients $K_{T_0}$ and $K_{\Omega_0}$:

$$T_n = sgn(n)K_{T_0}\rho D^4n^2,$$  \hspace{1cm} (10)

$$Q_n = sgn(n)K_{\Omega_0}\rho D^5n^2,$$  \hspace{1cm} (11)

$$P_n = sgn(n)2\pi K_{\Omega_0}\rho D^5n^3.$$  \hspace{1cm} (12)

Most propellers are asymmetric, resulting in different propeller characteristics when the propeller is reversed. Modelling of all four quadrants of operation is treated in e.g. Carlton (1994) and vanLammeren, vanManen, and Oosterveld (1969).

#### 2.3. Thrust losses

Thrust losses may be expressed by the thrust and torque reduction coefficients $\beta_T$ and $\beta_Q$, which express the ratio of the actual to the nominal thrust and torque (Minsaas et al., 1987):

$$\beta_T = \frac{T_a}{T_n} = \frac{sgn(n)K_T\rho D^4n^2}{K_{T_0}},$$  \hspace{1cm} (13)

$$\beta_Q = \frac{Q_a}{Q_n} = \frac{sgn(n)K_\Omega\rho D^5n^2}{K_{\Omega_0}}.$$  \hspace{1cm} (14)

Modelling of thrust losses, which are constituted of complex physical phenomena, is most commonly solved by empirical methods in conjunction with analytical models. The following loss effects may be considered:

- Fluctuations in the in-line water inflow to the propeller will cause fluctuations in thrust and torque for open and ducted propellers.
- Water inflow perpendicular to the propeller axis will introduce a force in the direction of the inflow due to deflection of the propeller race, termed transverse losses.
- For heavily loaded propellers ventilation (air suction) caused by loss of suction side sub-pressure on the propeller blades may occur, especially when the submergence of the propeller becomes small due to the wave-frequency motion of the vessel. If the relative propeller motion results in water exits, additional losses due to in-and-out-of-water effects following a hysteresis pattern due to the Wagner effect will occur.
- Both thrust reduction and change of thrust direction may occur due to thruster-hull interaction caused by frictional losses and pressure effects when the thruster race sweeps along the hull. The last is referred to as the Coanda effect.
- Thruster–thruster interaction caused by influence from the propeller race from one thruster on neighboring
thrusts may lead to significant thrust reduction, if not appropriate precautions are taken in the thrust allocation algorithm.

The in-line velocity fluctuations, ventilation losses, and in-and-out-of-water effects affect the loading of the propeller directly, and may, therefore, be partly compensated for by the low-level thruster controllers. These loss effects are incorporated in the simulation model, and will be presented in the following. Transverse losses, thruster–thruster-interaction, and the Coanda effect mainly affect the propeller wake, and have not been considered in this work. These loss effects must be targeted from the vessel design or thrust allocation routine.

2.3.1. In-line velocity fluctuations

Waves, currents, and vessel motion produce a time-varying velocity field around the propeller. This may be composed in an in-line component and a transverse component. The in-line, or axial, component gives rise to changes in the advance velocity \( V_a \), and hence the advance number \( J_a \) (8). As the thruster operating point is moving on the \( K_T \) and \( K_Q \) curves, fluctuations in thrust, torque, power, and shaft speed are induced. The advance velocity of a propeller behind a hull may be expressed by (Carlton, 1994):

\[
V_a = U(1 - (w_h + w_c + w_w)),
\]

where \( U \) is the ship surge velocity, and \( w_h \), \( w_c \), and \( w_w \) are the wake factors due to the hull and the in-line components of the current and wave induced velocities, respectively. With \( K_{TJ}(J_a) \) and \( K_{QJ}(J_a) \) the thrust and torque coefficients for a deeply submerged propeller at advance number \( J_a \), the thrust and torque loss factors \( \beta_{T,ax} \) and \( \beta_{Q,ax} \) due to axial flow are written as:

\[
\beta_{T,ax} = \frac{K_{TJ}(J_a)}{K_{T0}},
\]

\[
\beta_{Q,ax} = \frac{K_{QJ}(J_a)}{K_{Q0}}.
\]

These loss factors may be both larger than and smaller than unity, since the variations in advance speed may lead to both higher and lower propeller load than the nominal value for \( V_a = 0 \). Fig. 2 shows example thrust curves for varying advance speeds \( V_a \), and clearly illustrates that the resulting thrust loss is significant. Note that this loss effect is not applicable to a tunnel thruster, since the thrust then is assumed to be independent of the in-line velocity.

2.3.2. Ventilation and in-and-out-of-water effects

Ventilation, cavitation, loss of effective disc area, and the Wagner effect are all closely related physical phenomena, see e.g. Shiba (1953), Fleischer (1973), Minsaaas et al. (1987), Lehn (1992), and references therein. A simplified thrust loss model for ventilation and the loss of effective disc area has been implemented based on the experimental results presented in Section 3. The loss factor \( \beta_{T,vent} \) is given as a function of the relative immersion \( h/R \) and the relative shaft speed \( n/n_{max} \), see Fig. 3. Here, \( h \) is the propeller shaft submergence, \( R \) is the propeller radius, and \( n_{max} \) is the maximum shaft speed of the propeller. The loss model is parameterized by two relative shaft speeds, two submergence ratios and two loss factors, marked as \( n_1, n_2, h_1, h_2, b_1, \) and \( b_2 \) in the figure. Additionally, the main coordinates defining the loss surface are drawn as circles—the loss factor surface is found by linear interpolation between these. In the regime with \( h/R > h_2 \) the propeller is deeply submerged, and no losses are experienced (\( \beta_{T,vent} = 1 \)). For \( h/R < -1 \) the propeller exits the water completely, and hence no thrust is produced (\( \beta_{T,vent} = 0 \)). The remaining regime is \(-1 < h/R < h_2 \). For \( n/n_{max} < n_1 \) the propeller is lightly loaded, and only losses due to loss of effective disc area are experienced. This is seen as the gradually decreasing \( \beta_{T,vent} \) for \(-1 < h/R < h_1 \). For \( n/n_{max} > n_2 \), the propeller is heavily loaded, and decreasing submergence will lead to an abrupt loss of thrust due to ventilation.
The transition to-and-from the ventilated regime happens for \( n_1 < n/n_{\text{max}} < n_2 \) and \( h_1 < h/R < h_2 \). For \( h/R < h_1 \), additional thrust losses are experienced due to loss of effective disc area. The thrust loss for a fully submerged and fully ventilated propeller is defined by the two loss factors \( b_1 \) and \( b_2 \). \( b_1 \) is the loss factor where full ventilation starts \( (h/R = h_1 \) and \( n/n_{\text{max}} = n_2 \)), and \( b_2 \) is the loss factor at maximum shaft speed \( (h/R = h_1 \) and \( n/n_{\text{max}} = 1) \).

The hysteresis introduced by the Wagner effect may be modelled by a positive rate limit \( \dot{\beta}_{\text{wagner}} \) on \( \beta_{T,\text{vent}} \), such that the load is allowed to drop more quickly than it is allowed to build up, i.e. \(-\infty < \ddot{\beta}_{T,\text{vent}} \leq \dot{\beta}_{\text{wagner}} \). Such an approach gives good correspondence with the experimental time series.

The loss model is a coarse approximation of the real world, but captures the main characteristics of the phenomenon. It is also easy to implement, and is considered adequate for control system design and testing. Note that the loss model will be propeller specific, and that the six parameters defining the loss surface, therefore, will vary. In the model shown in Fig. 3, the parameters are chosen as \( n_1 = 0.3, n_2 = 0.35, h_1 = 1.1, h_2 = 1.3, b_1 = 0.3, \) and \( b_2 = 0.1 \).

The torque loss factor for ventilation is always larger than the thrust loss factor (since otherwise increasing loss would give increasing propeller efficiency), and may be related to the thrust reduction factor by:

\[
\beta_{Q,\text{vent}} = (\beta_{T,\text{vent}})^m,
\]

where \( m = 0.65 \) for a ducted propeller (Karlsen, Martinsen, & Minsaas, 1986).

### 3. Ventilation experiments

To investigate the effect of ventilation and loss of effective disc area for a propeller at low advance velocities, two sets of model tests were performed:

1. Stationary tests of a ducted propeller in a cavitation tunnel.
2. Dynamic tests of the same ducted propeller subject to waves and varying submergence in open water.

#### 3.1. Cavitation tunnel test results

Systematic tests with a ducted propeller were carried out in the cavitation tunnel at NTNU. The pressure in the tunnel was kept at atmospheric level, such that the propeller performance for normal free surface conditions could be tested. The advance number was kept at approximately 0.2, with varying thruster shaft speed and submergence. Time series of the thrust and torque were recorded, and the mean values for each test run were calculated. Fig. 4 shows the thrust coefficient \( K_T \) as a function of relative submergence \( h/R \) and propeller shaft speed \( n \). The torque coefficient has a similar form. The nondimensional total thrust is presented in Fig. 5. These results agree with previous research on the subject (Shiba, 1953; Fleischer, 1973), but contain additional information for low \( V_a \) – at low \( V_a \), with \( J_a \) close to zero, the advance number is no longer a sufficient parameter for describing the losses. For large submergence, the thruster exhibits the expected behavior, yielding the normal thrust characteristics with \( K_T \) constant and thrust proportional to \( n^2 \). For low shaft speeds, and hence low propeller loading, the thrust is constant for \( h/R = 1.5 \) to \( h/R = 1 \), and then decays almost linearly from \( h/R = 1 \) to \( h/R = 0 \). This can be seen in Fig. 5, and corresponds to loss of effective disc area, as described in Fleischer (1973). For higher propeller loading, the thrust and torque drop rapidly with decreasing submergence, indicating onset of ventilation. It is evident that for a heavily loaded
propeller, proximity to the surface may lead to an abrupt loss of thrust. As can be seen in Fig. 5, the results indicate that a reduction of shaft speed in such a case may increase the thrust, and that an increase in shaft speed will not increase the thrust. This was one of the key observations from the experiments, motivating the design of an anti-spin thruster controller. The findings are utilized in the ventilation loss experiments, motivating the design of an anti-spin thruster controller. The purpose of the local thruster controller is to relate the desired thrust $T_r$, given by the thrust allocation, to the commanded motor torque $Q_c$, as shown in Sørensen et al. (1997). The commanded motor torque should be run through a torque limiting function to limit the commanded torque according to the maximum motor torque $Q_{\text{max}}$ and power $P_{\text{max}}$

$$Q_c = \max\{Q_{c}, Q_{\text{max}}, P_{\text{max}}/(2\pi n)\},$$

where $Q_{c}$ is the commanded torque from the thruster controller. Fig. 7 shows the relationship between the desired thrust $T_r$ and the actually produced propeller thrust $T_a$, including thruster control with torque limiting, motor dynamics, shaft dynamics, and propeller hydrodynamics represented by $f_T(.)$ and $f_Q(.)$.

The thruster control schemes are based on the thrust/torque/speed/power relationships given in (5), (6), and (7). Since the propeller diameter and density of water are known and constant, the only remaining parameters are the thrust and torque coefficients $K_T$ and $K_Q$. The coefficients used in the controllers are termed $K_{TC}$ and $K_{QC}$, and denoted control coefficients.

In thruster control for station-keeping operations, estimates of the nominal thrust and torque coefficients $K_{T0}$ and $K_{Q0}$ for zero advance velocity are usually chosen as control coefficients, because the actual advance velocity is unknown to the controller. Doppler logs, GPS, or similar may be used to give estimates of the advance velocity, but these measurements are usually not of sufficient accuracy for inclusion in the local thruster control laws during station-keeping operations. Moreover, industrial practice dictates that other additional instrumentation only is considered worthwhile if it is economically sound, and the reliability and robustness of the controllers are not

3.2. Open water test results

The results from the cavitation tunnel are quasi-static values. An actual thruster on a vessel will be operating in more dynamic conditions, where transient effects are important. To investigate dynamic effects, open water tests in the Marine Cybernetics Laboratory (MCLab) at NTNU were carried out with the same propeller. The results here confirmed that it is advantageous to reduce the shaft speed when the thruster is subject to ventilation. Fig. 6 shows a time series of the propeller thrust and shaft speed for $h/R = 1.5$ in moderate waves. The thrust increases for increasing $n$, but suddenly drops sharply when the propeller loading becomes too high. The time series from these tests showed that ventilation caused the thrust to drop sharply by as much as 85% in a time span of only 1.5 s, illustrating the fast dynamics that the thruster control system has to cope with.

4. Thruster control

The proposed control scheme is divided into two control regimes, depending on the operational conditions:

(1) Thruster control in normal conditions, when experiencing moderate thrust losses.

(2) Anti-spin thruster control in extreme conditions, when experiencing large and abrupt thrust losses due to ventilation and in-and-out-of water effects.
negatively affected. In a station-keeping operation, where
the thruster will be subject to an oscillating velocity field
due to waves, disturbances from other thrusters and the
hull, as well as ventilation and in-and-out-of water effects,
using noisy and inaccurate estimates of
V_a in the control laws could result in reduced performance.

For thruster control in transit, usually only the main
propellers of the vessel are used. If the propeller
characteristics are known, better controller performance
may then be achieved by estimating the propeller advance
velocity V_a using the known vessel surge speed U and an
estimated hull wake factor w_k (15), or a low-pass filtered
measurement from a Doppler log or GPS. Updated values
of K_{TC} and K_{QC}, could then be estimated from the
propeller characteristics. Similar reasoning may be applied
to an underwater vehicle, where the propeller normally is
deeply submerged and not subject to ventilation and water
exits. Such an output feedback shaft speed controller with
advance speed compensation for AUVs was presented in
Fossen and Blanke (2000).

In this paper, low-speed station-keeping operations of
surface vessels are of main concern, such that the control
coefficients are taken as K_{T0} and K_{Q0}.

As already stated, most propellers are asymmetric. To
achieve good performance for both positive and negative
thrust references, it may, therefore, be necessary to use two
sets of control parameters. For low-speed operations, this
means that K_{T0} and K_{Q0} should be used for positive T_r,
and their reverse thrust counterparts used for negative T_r.

Switching between the control coefficients could be done at
T_r = 0. For the sake of simplicity, only positive thrust
references will be considered in the following.

4.1. Control objectives

The ultimate goal of any thruster controller is to make
the actual thrust track the desired thrust, i.e. T_a → T_r.
Since no feedback from the actual thrust is available,
different ways of trying to achieve this for a fixed pitch
propeller are:

- keep the shaft speed constant: shaft speed control;
- keep the motor torque constant: torque control;
- keep the motor power constant: power control.

When the thruster is subject to a time-varying wake field
(varying V_a) due to current, waves, and vessel motion, the
loading of the propeller is also time-varying, see Section 2.
Depending on the chosen control law, this will lead to
changes in shaft speed, torque, thrust, and power. In
extreme conditions, other goals may be more important
than tracking the desired thrust, e.g. reducing mechanical
wear and tear and limiting power transients. Other
performance criteria than thrust production should, there-
fore, also be taken into account when evaluating various
control schemes. In this work, the following performance
criteria are considered:

1. Thrust production in presence of disturbances, i.e.
   ability to counteract thrust losses.
2. Mechanical wear and tear caused by transients and
   oscillations in motor and propeller torque.
3. Predictable power consumption, which is of major
   concern to the power management system.

The simulation model does not capture the violent blade
frequency (number of blades times shaft speed in rps)
loading that occurs during ventilation, as described in
Olofsson (1996). These loads may lead to structural
damage of the propeller, and result in wear and tear of
the mechanical components of the propulsion system.
Although not well documented in literature, mechanical
wear and tear of thrusters is a well-known industrial
problem that has received increased attention in recent
years, especially for azimuthing thrusters and tunnel
thrusters. It is believed that the widespread mechanical
failures in gears and bearings of such thrusters are mainly
due to the combination of low-frequency ventilation loads
and blade frequency load oscillations (Koushan, 2004,
2006). Hence, it is considered advantageous to limit
propeller racing during ventilation, since this reduces the
dynamic loading significantly.

4.2. Thruster control for normal conditions

Thruster control schemes that may be considered for
normal conditions are shaft speed control, torque control,
power control, combined power/torque control, and
variations of these. For surface vessels with fixed pitch

---

Fig. 7. Block diagram showing the relationship between desired thrust T_r and produced propeller thrust T_a, including thruster control with torque
limiting, motor dynamics, shaft dynamics, and propeller hydrodynamics.
propellers, shaft speed control is normally used. Torque and power control for diesel engines with mechanically direct-driven propellers was proposed and implemented by Blanke and Busk Nielsen (1990), whereas torque and power control for electrically driven propellers on surface vessels was introduced by Sørensen et al. (1997). A combined torque and power controller was presented in Smogeli et al. (2004a). For underwater vehicles, both torque and various shaft speed control schemes have been proposed, see e.g. Yoerger et al. (1991), Whitcomb and Yoerger (1999b), Fossen and Blanke (2000), and the references therein. Experimental verification of the performance of torque and power control when compared to shaft speed control was presented in Smogeli et al. (2005), demonstrating that torque and power control outperforms the conventional shaft speed control in normal operational conditions—both in terms of more constant thrust production when subject to disturbed inflow (variations in $V_a$), reduced mechanical wear and tear when subject to an oscillating inflow (e.g. due to waves and vessel motion), as well as more predictable (constant) power consumption. This is also shown in the simulations presented in Section 5.

4.2.1. Shaft speed control

The shaft speed controller utilizes feedback from the thruster shaft speed, and sets the commanded motor torque $Q_{cm}$ by, for example, a PID algorithm operating on the shaft speed error $e = n_r - n$, where the desired shaft speed $n_r$ is calculated from the desired thrust $T_r$ by inverting (5):

$$n_r = \text{sgn}(T_r) \sqrt{|T_r|}. \quad (21a)$$

$$Q_{cm} = K_p e + K_i \int_0^1 et \, dt + K_d \dot{e}. \quad (21b)$$

The object of the torque controller is to control the set-point of the motor torque instead of the shaft speed. A mapping from the desired thrust $T_r$ to the desired torque $Q_r$ can be found from (5) and (6). The commanded motor torque $Q_{cm}$ is set equal to the desired torque:

$$Q_{cm} = Q_r = \frac{K_{oc}}{K_{tc}} DT_r. \quad (22)$$

4.2.3. Power control

Power control is based on controlling the power consumption of the thruster instead of the shaft speed. The desired power $P_r$ is found by inserting the desired torque from (22) and the desired shaft speed from (21a) in (7) such that:

$$P_r = \frac{Q_r 2 \pi n_r}{|T_r|^{3/2}} = \frac{2 \pi K_{oc}}{\sqrt{\rho D K_{tc}^3}}. \quad (23)$$

The signed desired power $P_{rs}$ is defined as:

$$P_{rs} = \text{sgn}(T_r) P_r = \text{sgn}(T_r)|T_r|^{3/2} = \frac{2 \pi K_{oc}}{\sqrt{\rho D K_{tc}^3}}. \quad (24)$$

The commanded motor torque $Q_{cp}$ is calculated from $P_{rs}$ using feedback from the measured shaft speed $n \neq 0$ according to:

$$Q_{cp} = \frac{P_{rs}}{2 \pi |n|} = \frac{K_{oc}}{\sqrt{\rho D K_{tc}^3}} \text{sgn}(T_r)|T_r|^{3/2} / |n|. \quad (25)$$

4.2.4. Combined power/torque control

The singularity for $n = 0$ in the power control scheme (25) means that power control should not be used close to the singular point, e.g. when commanding low thrust or changing the thrust direction. For low thrust commands, torque control shows better performance in terms of constant thrust production, since the mapping from thrust to torque is more directly related to the propeller loading than the mapping from thrust to power. For high thrust commands, it is essential to avoid large power transients to avoid danger of power blackout and harmonic distortions on the power plant network. Power control is hence a natural choice for high thrust commands. The combined power/torque controller is designed to utilize the best properties of both the power and the torque controller, and is:

$$Q_{cc} = z_n(n)Q_{cp} - (1 - z_n(n))Q_{cp} + Q_{cq}, \quad (26)$$

where $Q_{cc}$ is the commanded motor torque, and $Q_{cq}$ and $Q_{cp}$ are given by (22) and (25). $z_n(n)$ is a weight function of the type:

$$z(z) = e^{-kpz^r} \quad \text{for} \quad z \in \mathbb{R}, \quad (27)$$

where $k$, $p$, and $r$ are positive tuning gains. The shape of $z_n(n)$ defines the dominant regimes of the two control schemes, and can be used to tune the controller according to user specifications. The combined controller was in Smogeli et al. (2004a) shown to have smooth behavior with respect to $n$ as long as $r > 1$ in the function $z_n(n)$.

4.3. Anti-spin thruster control

As shown above, a thruster subject to ventilation and in-and-out-of water effects will experience large load transients. The conventional shaft speed controller is partly able to handle these losses, since the object of the controller is to keep the shaft speed constant. However, problems could arise because a well-tuned PID controller for normal conditions may behave badly when subject to large thrust losses. If torque or power control is used, large thrust losses will lead to even more serious problems. Since the object of torque and power control is to keep the motor torque or power constant, the loss of propeller load torque will lead
to severe motor racing, which again leads to vibrations and mechanical wear and tear because of the unsteady loading of the propeller, and transients when the propeller re-enters the water. From the experimental results presented in Section 3, it can be seen that reducing the propeller shaft speed during ventilation may increase the thrust. At the same time the mechanical wear and tear due to blade-frequency loading is decreased, and the re-entry into the water will be more gentle. This motivates the introduction of anti-spin thruster control. The general ideas of the anti-spin control concept are to:

- Reduce the wear and tear of the propulsion unit.
- Limit the transients on the power system.
- Optimize the thrust production and efficiency in transient operation regimes.

A possible approach to anti-spin control is a hybrid control scheme where the condition of the thruster is monitored, and an anti-spin control action is invoked when a ventilation incident is detected. Fig. 8 shows the structure of such an anti-spin control system, which will be further elaborated in the following.

### 4.3.1. Similarities to car anti-spin/ABS

Since both the structures of the physical systems and the control objectives are similar, the development of the thruster anti-spin control system has been motivated by similar hybrid control strategies in car anti-spin and ABS systems, see for example Haskara et al. (2000) or Johansen et al. (2001). Neglecting the motor dynamics and the linear friction term $K_f \omega$, the dynamic model of the propeller is shortly summarized as, see (1), (2), and (3):

\[ I_{r} \ddot{\omega} = -Q_{m} + Q_{m} \]  \hfill (28a)

\[ T_{a} = f_t(n, x_p, \theta_p) \]  \hfill (28b)

\[ Q_{a} = f_q(n, x_p, \theta_p) \]  \hfill (28c)

In car anti-spin/ABS, a commonly used model is the “quarter car” (Canudas de Wit & Tsiotras, 1999):

\[ I_{w} \ddot{\omega} = -rF_{x} + Q_{w} \]  \hfill (29a)

\[ \frac{F_{x}}{F_{z}} = \mu(\lambda), \quad \frac{m\ddot{v}}{F_{z}} = F_{x} \]  \hfill (29b, 29c)

where $I_{w}$ is the wheel rotational inertia, $\omega_{w}$ is the wheel angular velocity, $r$ is the wheel radius, $F_{x}$ is the tire friction force, $Q_{w}$ is the breaking or accelerating input torque to the wheel from brakes and motor, $F_{z}$ is the vertical force between tire and road, $\mu$ is the tire friction coefficient, $\lambda$ is the wheel slip, $m$ is the mass of a quarter car, and $v$ is the car speed. The actuator dynamics have been neglected. The wheel slip $\lambda$ is defined as:

\[ \lambda = \begin{cases} 
1 - \frac{r \omega_{m}}{v} & \text{if } v > r \omega_{m}, \ v \neq 0 \ (\text{braking}), \\
1 - \frac{v}{r \omega_{m}} & \text{if } v < r \omega_{m}, \ v \neq 0 \ (\text{accelerating}). 
\end{cases} \]  \hfill (30)

When $\lambda = 1$ there is no sliding (pure rolling), and when $\lambda = 0$ there is full sliding. The shape of the friction coefficient $\mu(\lambda)$ depends on many parameters, which can be summed up in the tire characteristics, the road conditions, and the vehicle operational conditions. The aim of the anti-spin/ABS control systems is commonly to keep the wheel slip at a specified set-point or to maintain a specified wheel acceleration (Johansen et al., 2001).

When comparing the two models (28) and (29), many similarities may be noticed. The rotational dynamics of the propeller and the wheel are identical, with inertial terms $I_{r} \ddot{\omega}$ and $I_{w} \ddot{\omega}$, motor input terms $Q_{m}$ and $Q_{w}$, and load torque terms $Q_{a} = f_q(n, x_p, \theta_p)$ and $rF_{x} = rF_{z}\mu(\lambda)$, respectively. In both cases, the aim of the anti-spin control is to optimize the load torque through control of the motor input. For car anti-spin, a too high input torque will lead to loss of friction through spin (acceleration) or wheel lock (braking), and hence reduced acceleration or stopping force. For thruster anti-spin, a too high load on a propeller operating close to the free surface will lead to loss of torque through ventilation, and hence loss of thrust. In addition, the propeller load torque and tire friction force depend on many other parameters, most of which are unknown to the controller.

However, some differences should also be noticed. The tire friction force can be directly related to the vehicle...
dynamics, and the wheel slip can be estimated from the vehicle speed and wheel angular velocity. This leaves only the friction coefficient $\mu(\lambda)$ unknown. However, for most conditions, the shape of the friction coefficient will be known, making it possible to develop a general anti-spin scheme. The propeller load torque $Q_a$ is composed of several complex physical phenomena, and must be considered as an exogenous disturbance from a control point of view. Also, the relationship between the propeller thrust and the vessel dynamics is more complex than the car dynamics, especially for a vessel equipped with multiple thrusters.

4.3.2. Detection

The detection of high thrust losses may be done in several ways. A simplistic approach is to set detection criteria by rate of change of shaft speed or motor torque— if the shaft speed increases quickly or the torque drops quickly, this indicates a high loss situation. A similar approach for car anti-spin is shown in Haskara et al. (2000). A more sophisticated detection algorithm was presented in Smogeli et al. (2004b), based on the propeller load torque observer from Smogeli et al. (2004a). The observer estimates the propeller load torque from measurements of propeller shaft speed and motor torque, based on a model of the propeller shaft dynamics:

$$\dot{\omega} = \frac{1}{I_s}(-\hat{Q}_a - K_o\omega + Q_m) + k_1(\omega - \dot{\omega}), \quad (31a)$$

$$\dot{\hat{Q}}_a = k_2(\omega - \dot{\omega}), \quad (31b)$$

where $k_1$ and $k_2$ are observer gains. If $k_1$ and $k_2$ are chosen according to:

$$k_1 > -K_o/I_s, \quad k_2 < 0, \quad (32)$$

the equilibrium point of the observer error dynamics is globally exponentially stable (Smogeli et al., 2004a). The load torque estimate $\hat{Q}_a$ can be used to calculate an estimate of the torque loss factor, $\hat{\beta}_Q$:

$$\hat{\beta}_Q = \frac{\hat{Q}_a}{Q_n}, \quad (33)$$

where $Q_n$ is given by (11). By defining limits for the onset and termination of ventilation, $\beta_{v,\text{on}}$ and $\beta_{v,\text{off}}$, a detection signal may be generated by monitoring $\hat{\beta}_Q$. The loss detection sets the detection flag $\zeta$ high or low, i.e. $\zeta \in \{0, 1\}$. A ventilation incident will then give the following evolution of $\zeta$, with time instants $t_1 < t_2 < t_3$:

$$t_1 : \hat{\beta}_Q \geq \beta_{v,\text{on}} \Rightarrow \zeta = 0 \quad \text{(no ventilation)}, \quad t_2 : \hat{\beta}_Q < \beta_{v,\text{on}} \Rightarrow \zeta = 1 \quad \text{(ventilation)}, \quad t_3 : \hat{\beta}_Q \geq \beta_{v,\text{off}} \Rightarrow \zeta = 0 \quad \text{(no ventilation)}.$$

For implementation, a detection algorithm combining several ventilation criteria will probably be the most robust.

A possible problem with the proposed anti-spin approach is that the anti-spin control action may increase the thrust to such an extent that the detection algorithm believes that the ventilation incident is over, and thereby removes the anti-spin action. As the propeller loading is increased, the ventilation may then reappear. This may lead to chattering as the anti-spin control action is switched on and off several times during one ventilation incident. Logic to avoid such incidents should, therefore, be included in the detection algorithm. One solution to this is e.g. to set a limit to how often $\zeta$ is allowed to change value. This may also remove false detections due to measurement noise and transients. However, no general solution to this problem has yet been found.

In Section 4.3.4, a simple anti-spin controller that does not require a dedicated detection scheme is presented. Instead, it uses a dynamic shaft speed limit, which is equivalent to detecting the ventilation based on the shaft speed. This removes any concerns about detection chattering, but introduces other challenges; this is further discussed below.

4.3.3. Anti-spin control actions

During operation in normal conditions, one of the controllers proposed in Section 4.2 is used. When extreme conditions are encountered, the anti-spin control action is triggered by the ventilation detection algorithm. Different approaches to the anti-spin action are possible, with the two most general being:

1. Switch to a completely new control strategy.
2. Modify the existing control strategy.

If choosing an approach with switching, two options exist: (a) switch anti-spin on and off every time ventilation is detected and terminated, and (b) switch to an anti-spin control mode when ventilation is first detected, and stay in this mode until no ventilation incidents have been detected for a given time horizon. The first alternative may introduce problems with ensuring performance and stability during switching. Bumpless transfer and switching problems are discussed in, for example, Aström and Wittenmark (1997), and will not be further treated here. The latter alternative will not give any problems with switching, and was the approach proposed in Smogeli et al. (2003). In this case, the existing control strategy must be compatible with the anti-spin control action. Two natural modifications of the normal control strategies when ventilation is detected are: (a) reduce the motor input $Q_v$ directly in order to reduce the motor torque and lower the shaft speed, and (b) lower the thrust reference $T_r$ in order to reduce the motor input indirectly. When the ventilation incident is terminated, the motor input and/or thrust reference should be increased to their original values. To avoid transients, the change of motor input and thrust reference must be done smoothly. The torque and thrust modification factors are termed $\gamma_Q$ and $\gamma_T$, respectively.
and used such that the anti-spin motor input $Q_{cas}$ and anti-spin thrust reference $T_{ras}$ are defined as:

$$Q_{cas} = \gamma_Q Q_c, \quad T_{ras} = \gamma_T T_r. \quad (34a)$$

The experimental results presented in Section 3 indicate that there for this type of propeller exists an specific shaft speed $n_{cas}$ that may optimize the thrust production during ventilation. From Fig. 5, this optimal shaft speed would be approximately 9 rps, and from Fig. 3 it would correspond to $n/n_{aux} \approx 0.25$. This optimal shaft speed will be a thruster specific parameter, and can be used to find $\gamma_T$.

An approach with both motor input and thrust reference scaling as anti-spin control actions was presented in Smogeli et al. (2004b), based on the combined power/torque controller from Smogeli et al. (2004a). In a similar manner, detection and anti-spin control action could be added to a normal shaft speed controller to yield a different anti-spin controller. However, since the object of this work is to design a controller that has the best possible performance both in normal and extreme conditions, it is natural to choose a controller based on torque and/or power control, which is assumed to have superior performance in normal conditions.

In the following, two simple anti-spin control schemes based on the combined power/torque controller (26) will be proposed.

4.3.4. Anti-spin controller #1

In the simplest of the two anti-spin control formulations proposed in this work, a dynamic shaft speed bound is added to the combined controller. This limits the propeller racing, since the shaft speed is kept within the desired bounds. Using the desired shaft speed $n_r$ from (21a), the dynamic speed bound $n_b$ is defined as:

$$n_b = (1 + k_b)n_r, \quad (35)$$

where $k_b$ is a constant, typically in the range $k_b \in [0.2, 0.3]$, which means that the shaft speed is allowed to deviate by 20–30% from its nominal value. The shaft speed can then vary freely as long as $n < n_b$, such that the combined controller is undisturbed in normal conditions. In a high loss situation, when the combined controller normally would lead to severe propeller racing, the shaft speed is bounded by $n \approx n_b$.

The shaft speed bound is implemented by an additive shaft speed PI controller, which is only activated when $n > n_b$, i.e.:

$$Q_{cas} = Q_{cc} + Q_b, \quad (36)$$

where $Q_{cc}$ is defined in (26) and the additive PI term $Q_b$ is defined as:

$$Q_b = \min \left( 0, K_p e_b + K_i \int_0^t e_b \, dt \right). \quad (37)$$

As in (21b) $K_p$ and $K_i$ are proportional and integral term tuning gains, which should be tuned to give rapid response, and $e_b$ is the bound error defined as:

$$e_b = n_b - n. \quad (38)$$

To avoid integral windup, the integral term may only assume negative values (upper saturation at zero). With respect to the anti-spin control actions defined in (34a), only $\gamma_Q$ is used, with $\gamma_Q$ in this case being defined by:

$$\gamma_Q = \frac{Q_{cas}}{Q_c} = \frac{Q_{cc} + Q_b}{Q_{cc}}. \quad (39)$$

Note that the dynamic speed bound just as well may be used with the pure torque controller (22) or pure power controller (25). This formulation does not utilize a dedicated ventilation detection scheme as proposed in Section 4.3.2, but “detects” ventilation simply by an excessive shaft speed. The number of control parameters is, therefore, kept low such that it is relatively easy to tune.

4.3.5. Anti-spin controller #2

In this more sophisticated anti-spin controller, a detection scheme as proposed in Section 4.3.2 triggers a twofold anti-spin control action: (1) activate an additive shaft speed PI controller; and (2) lower the thrust reference, if desired. In this case, no shaft speed bound is needed, since the additive shaft speed controller aims to regulate the shaft speed to its reference $n_r$ from (21a). The primary anti-spin control action is formulated as follows:

$$Q_{cas} = \begin{cases} Q_{cc} & \text{for } \zeta = 0, \\ Q_{cc} + Q_{PI} & \text{for } \zeta = 1, \end{cases} \quad (40)$$

where the additive PI term is defined similarly as in (21b):

$$Q_{PI} = K_p e + K_i \int_0^t e \, dt. \quad (41)$$

As before, $K_p$ and $K_i$ are the nonnegative proportional and integral tuning gains, and $e = n - n_r$. The integral term is continuously reset for $\zeta = 0$ to avoid integral windup. In addition, the total commanded torque $Q_{cas}$ is saturated from below at zero, such that negative torque cannot be commanded. As a secondary anti-spin action, the thrust reference $T_r$ may be lowered when $\zeta = 1$, assuring that the shaft speed is reduced to $n_{aux}$. This is done by passing the thrust reference through a first-order low-pass filter with time constant $\tau_n$ and a rate limiting function such that $\dot{n}_{aux} < \dot{n} < \dot{n}_{rise}$, where $\dot{n}_{fall}$ and $\dot{n}_{rise}$ are shaft speed rate limits. With respect to (34), both $\gamma_Q$ and $\gamma_T$ are used.

In this formulation, more control parameters are needed because of the ventilation detection scheme. However, when correctly tuned, the performance of this scheme is expected to be superior to anti-spin controller #1. This controller attempts to utilize the best properties of both the combined controller and the shaft speed controller: in normal conditions the combined controller is left to work undisturbed, and when ventilation is detected, the shaft
speed controller assures that the desired shaft speed is maintained.

4.3.6. Robustness issues and tuning

The main purpose of this paper is to motivate and introduce the concept of anti-spin thruster control. The two proposed anti-spin controllers and ventilation detection scheme are only meant as examples of how the problem may be solved, and a thorough robustness analysis is considered outside the scope of this paper. However, the physical properties of the thruster itself can be assumed to be constant, leaving the propeller loading as the uncertain element in the analysis. The anti-spin controllers are designed to cope with exactly such conditions. Assuming that the control coefficients \( K_{TC} \) and \( K_{OC} \) are known, the only tuning parameters of the combined power/torque controller are the weighting function parameters, which determines the shaft speed range for torque and power control. This does not affect the stability of the controller as such, but during ventilation power control will be preferable over torque control. This will be shown below, and indicates that it is beneficial to choose the weighting function parameters such that power control dominates in the regime where ventilation may occur. Tuning of the detection scheme must be done separately from the controller, and is discussed in Section 4.3.2. The remaining parameters to tune are the PI parameters that are used during ventilation, the shaft speed bound \( k_b \) in the case of anti-spin controller #1, and the optimal shaft speed \( n_{as} \) in the case of anti-spin controller #2. Tuning of the PI parameters may be done in a similar manner as for a conventional speed controller; the influence of these parameters on the shaft speed controller performance is studied below.

When the propeller is subject to increasing advance velocities, the combined controller will increase the shaft speed in order to compensate for the reduced loading. This increase should not be detected as a ventilation incident. A weakness of anti-spin controller #1 is, therefore, that in order to allow the combined controller to work undisturbed, \( k_b \) should be chosen relatively high. This means that the propeller is allowed to spin more out during ventilation. Choosing a value for \( n_{as} \) in anti-spin controller #2 depends on the knowledge of the system. If the shaft speed that optimizes the efficiency during ventilation is known, \( n_{as} \) should be set to this value. Otherwise, some ad hoc value must be chosen, or the thrust reference scaling can be omitted.

Further investigation of these issues and the development of other anti-spin algorithms will be important topics for further research.

5. Simulation results

Simulations were performed with the models presented in Section 2. The propeller was a Wageningen B-series propeller (Oosterveld & Ossanen, 1975) with characteristics given in Fig. 1. The model parameters are given in Table 1. The ventilation loss model from Section 2 was used with \( n_{\text{max}} = 4 \) rps. The Wagner effect rate limit \( \beta_{\text{Wagner}} \) was set to 1.

This configuration could typically represent a pod or azimuth thruster installed in the aft ship of a supply vessel. Two cases were simulated:

1. Normal control in moderate seas.
2. Anti-spin control in extreme seas.

The control coefficients were chosen equal to the nominal coefficients, i.e. \( K_{TC} = K_{T0} \) and \( K_{OC} = K_{Q0} \). The controller feedback signals from shaft speed and motor torque were contaminated with white noise of power \( 1E-7 \) and \( 10 \), respectively, and the simulation run at a sampling rate of 100 Hz. Other controller parameters and details on the simulation scenarios are given subsequently.

5.1. Normal control in moderate seas

In this simulation scenario, the propeller shaft was fixed at a submergence of \( h = 7 \) m with respect to the free surface. The propeller was subjected to harmonic waves with amplitude 2 m and period 10 s, with an assumption of undisturbed waves. This resulted in inflow \( V_{in} \) oscillations with amplitude 0.95 m/s. The propeller thrust and torque were calculated from the 1-quadrant thruster characteristics of the propeller, with no ventilation losses or in-and-out-of-water effects present. Four controllers were tested:

1. Shaft speed PI controller #1: Tightly tuned with \( k_p = 5E5 \) and \( T_i = 0.4 \).
2. Shaft speed PI controller #2: Loosely tuned with \( k_p = 1E5 \) and \( T_i = 0.6 \).
3. Torque control.
4. Power control.

Fig. 9 shows a comparison of the shaft speed \( n \), thrust \( T_a \), propeller torque \( Q_a \), motor torque \( Q_m \), and power \( P_a \) for the four controllers. If first considering only shaft speed controller #1, torque, and power control, the three control laws can be seen to obtain their objectives: speed control gives the least fluctuations in shaft speed, torque control gives the least fluctuations in propeller and motor torque, and power control gives the least fluctuations in propeller power. The torque control also gives the least fluctuations in propeller thrust. The combined power/torque controller performance can be inferred from the performance of the

<table>
<thead>
<tr>
<th>Table 1</th>
</tr>
</thead>
<tbody>
<tr>
<td>Simulation model parameters</td>
</tr>
<tr>
<td>( D ) (m)</td>
</tr>
<tr>
<td>4</td>
</tr>
</tbody>
</table>
power and torque controllers, depending on the chosen weighting function \( w(n) \). This confirms that controllers based on torque and power control will give more constant thrust production, less mechanical wear and tear, and more constant power consumption than the shaft speed controller in normal operating conditions. When looking at shaft speed controller #2, it clearly does not track the desired shaft speed well, and in that sense performs badly with respect to its goal. However, when compared to shaft speed controller #1 it has slightly less fluctuations in thrust, torque, and power. It hence has better performance with respect to the performance criteria specified in this work, although still outperformed by the torque and power controllers. Similar results may be obtained for any propeller and load disturbance, since the superior properties of the torque and power controllers are inherent in their design.

5.2. Anti-spin control in extreme seas

The objective of this simulation scenario was to expose the propeller to a severe ventilation incident. The propeller was, therefore, subject to a sinusoidal heave (vertical) motion with mean submergence 7 m, amplitude 7 m, and period 15 s. No waves were simulated, such that the loss effects only were due to ventilation and in-and-out-of water effects as given by the ventilation loss model.

First, several shaft speed controllers were tested to investigate the sensitivity to tuning of the PID parameters. Fig. 10 shows the resulting shaft speed for four PI controllers, with \( K_p \) and \( T_i \) given in the legend of the figure (all with \( K_d = 0 \)). Clearly, the tuning is of major importance. If the controller is too tightly tuned, the result may be oscillations due to integrator discharge. If the controller is too loosely tuned, the integrator builds down too slowly, resulting in shaft speed racing.

In the following, six controllers will be compared:

1. Shaft speed PI controller #1: Tightly tuned with \( K_p = 5E5 \) and \( T_i = 0.4 \).
2. Shaft speed PI controller #2: Loosely tuned with \( K_p = 1E5 \) and \( T_i = 0.6 \).
3. Torque control.
4. Power control.
5. Anti-spin controller #1, see Section 4.3.4.
6. Anti-spin controller #2, see Section 4.3.5.

During ventilation, anti-spin controller #2 without the reference scaling behaves almost identically to shaft speed controller #1. These curves are, therefore, not shown. The combined controller weighting function parameters in the anti-spin controllers were \( k = 1 \), \( p = 1 \), and \( r = 2 \). This results in pure power control for most of the operating regime. The additive shaft speed controller parameters were chosen as \( K_p = 5E5 \) and \( T_i = 0.5 \) for both anti-spin controllers. For anti-spin controller #1, the shaft speed bound was chosen as \( k_b = 0.2 \). For anti-spin controller #2, the ventilation detection scheme was based on \( \tilde{\beta}_Q \) from

![Fig. 9. Controller comparison in moderate seas: shaft speed \( n \), thrust \( T_a \), propeller torque \( Q_a \), motor torque \( Q_m \), and power \( P_a \).](image)

![Fig. 10. Comparison of the shaft speed \( n \) for four PI controllers during a ventilation incident.](image)
(33) with $\beta_{\text{con}} = 0.7$ and $\beta_{\text{off}} = 0.9$, and the load torque observer gains were $k_1 = 10$ and $k_2 = -1E6$. The optimal shaft speed during ventilation, filter time constant, and rate limits for the secondary anti-spin action were chosen as $n_{\text{at}} = 1.1 \, \text{rps}$, $\tau_n = 1 \, \text{s}$, $\beta_{\text{fall}} = -1 \, \text{rps/s}$, and $\beta_{\text{rise}} = 1 \, \text{rps/s}$.

The simulation results for one ventilation incident are shown in Figs. 11–17, where the ventilation starts at approximately 15.5 s, and terminates at approximately 19.5 s. Fig. 11 shows the relative submergence $h/R$ and details from anti-spin controller #2: scaled numerical derivatives of the motor torque $Q_m$ and the shaft speed $n$, the estimated loss factor $\hat{\beta}_Q$ and detection flag $\zeta$, and the thrust and torque modification factors $\gamma_T$ and $\gamma_Q$. The derivatives of $Q_m$ and $n$ are scaled with their maximum values during the simulation for plotting purposes. The loss detection shows good agreement with the actual loss incident. The optimal shaft speed during ventilation corresponds to $\gamma_T \approx 0.5$ for this particular case, and the torque modification $\gamma_Q$ is as low as zero for a while after the ventilation is detected.

Fig. 12 shows the shaft speed $n$, demonstrating how the torque and power controllers lead to severe propeller racing during ventilation. It also demonstrates that a loosely tuned PI controller (shaft speed controller #2) will race out at the beginning of the ventilation incident, and have problems with regaining the reference shaft speed when the ventilation incident is over. Shaft speed controller #1 keeps the shaft speed quite constant, and the
two anti-spin controllers can be seen to work as intended: anti-spin controller #1 stops the propeller racing at the designated level, whereas anti-spin controller #2 lowers the shaft speed down to $n_{av}$ during ventilation. The resulting thrust production $T_a$, propeller load torque $Q_a$, and power consumption $P_a$ are found in Figs. 13–15, respectively. According to the simulation model, the torque controller achieves the highest thrust production during ventilation. This is, however, at the expense of severe propeller racing, as well as excessive power consumption. Such performance is clearly unacceptable, emphasizing that torque control should not be used in its pure form if the propeller may be subject to ventilation. The power controller also achieves higher thrust production, but again at the expense of propeller racing. However, the power consumption is kept almost constant all through the ventilation incident. The major problem with the power controller is, therefore, mechanical wear and tear due to vibration, which is not accounted for in the simulation model. The thrust production of the two shaft speed controllers and anti-spin controller #1 are of comparable magnitude. The thrust production of anti-spin controller #2 is, when the shaft speed is lowered to its optimal value, slightly larger than for the two shaft speed controllers and anti-spin controller #1, and comparable with the power controller. This shows that it may be possible to increase the thrust production during a ventilation incident if proper actions are taken. The power consumption of all these four controllers is acceptable. The motor torque $Q_m$ can be found in Fig. 16, showing how the controllers work.

Fig. 17 shows the resulting thrust during ventilation as a function of relative submergence $h/R$ and shaft speed $n$, including traces of a ventilating propeller with three different controllers: shaft speed controller #1, power control, and anti-spin controller #2. The start of the traces are marked with a circle, where the propeller is deeply submerged with a shaft speed of $n \approx 1.5$ rps. The ventilation incident starts when $h/R$ is reduced such that it falls over the edge for $h/R \approx 1.3$. The loss of load torque leads to different behavior for the three controllers, as already seen above. The shaft speed controller keeps the shaft speed nearly constant after a transient period, the power controller leads to propeller racing, and the anti-spin controller lowers the shaft speed. The optimal shaft speed can be seen as a ridge at $n \approx 1.2$. When the ventilation incident terminates, the shaft speed is controlled back to its original value by all the controllers.

6. Conclusions

A model for a ventilating propeller has been motivated and developed based on experimental results. The concepts of shaft speed, torque, and power control have been summarized, and the need for an anti-spin control law when using torque or power control has been shown. Two different anti-spin controllers have been proposed. These
were based on a combined power/torque controller with additional anti-spin control actions that aimed at controlling the shaft speed during ventilation.

Simulations of a thruster in waves demonstrated that better thrust production, less mechanical wear and tear, and more constant power consumption could be achieved by using torque or power control when compared to shaft speed control. Simulations in extreme conditions, with the propeller subject to ventilation and in-and-out-of-water effects, showed that the performance of the torque and power controllers was unacceptable due to excessive power consumption for the torque controller, and propeller racing for both controllers. The anti-spin controllers were shown to limit the propeller racing during ventilation. The most advanced anti-spin controller was also capable of lowering the shaft speed to a designated value when ventilation was detected. It was shown that this could increase the thrust production during ventilation, while avoiding power peaks and vibrations that occur during propeller racing.

Acknowledgment

This work has been carried out at the Centre for Ships and Ocean Structures (CeSOS) at NTNU in cooperation with the research project on Energy-Efficient All-Electric Ship (EEAES). The Research Council of Norway is acknowledged as the main sponsor of CeSOS and EEAES.

References


control. In *Proceedings of the fourth IFAC conference on manoeuving and control of marine craft (MCMC’97)*. Brijuni, Croatia.


