Investigating the Steady and Unsteady Maneuvering Dynamics of an Azimuthing Podded Propulsor

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ABSTRACT

This paper provides highlights of results of a recent investigation completed at the Massachusetts Institute of Technology pertaining to the characterization of the steady and unsteady maneuvering force dynamics associated with an azimuthing podded propulsor. Highlighted results include quasi-steady and unsteady forces, as well as several methods of flow visualization.

KEY WORDS: Azimuthing propulsion; podded propulsion; maneuvering dynamics.

NOMENCLATURE

- P: Propeller pitch
- Q, Qρ: Propeller/propulsor torque (total hydrodynamic, quasi-steady)
- T, Tρ: Propeller/propulsor thrust force (total, quasi-steady)
- Va: Advance velocity, carriage velocity
- Vρ, Vθ: Axial velocity, tangential velocity
- Vρ, Vθ: Axial induced velocity
- V: Effective inflow velocity at propulsor
- Vi: Induced velocity

INTRODUCTION

While azimuthing propulsion in the form of low-power electric, hydraulic, or right-angle gear-driven steerable thrusters has been around for nearly half a century, it has been only in the last decade that electric motor technology has advanced to the point where implementation of high-power azimuthing electric-drive propulsors has become practical for primary propulsion (and steering). The advantages of azimuthing electric-drive primary propulsion (often called podded or modular propulsion) are numerous, including design flexibility leading to improved arrangement efficiency, power management, maneuverability, and even reduced hull resistance.

While the implementation of podded propulsors into the commercial ship market has been swift, the complete understanding of their hydrodynamics through research, particularly in the area of maneuvering performance, has been limited. Van Terwisga et al. (2001) provide a general overview of the history of mechanical and electrical steerable propulsion units, and address general hydrodynamic issues associated with their design and use. Toxopeus and Loeff (2002) discuss recent applications of podded propulsion from a maneuvering perspective, comparing maneuverability between specific ship designs with conventional propulsion and podded propulsion, and highlighting the general benefits and points of attention. Some additional comparative maneuvering testing has been conducted under the auspices of the OPTIPOD and FASTPOD research programs funded by the European Union, with limited results recently presented at the First International Conference on Technological Advances in Poded Propulsion (T-POD),
held in the UK in April 2004 (Ayaz et al. 2004; Kobylinksi 2004).

Although some limited free-running and captive model maneuverability testing on ships with pods has been conducted, there has been little basic research done specifically in the area of prediction and simulation of dynamic maneuvering forces associated with podded propulsors. Research of this type conducted in the past has focused mainly on the design and optimization of low-power thrusters and dynamic positioning systems for deep water applications at slow speeds or in currents (Minsaas and Lehn 1978; Norrby and Ridley 1980; Nienhuis 1992; Wichers et al. 1998). A few new investigations of this type for podded propulsors have just recently been presented at the T-POD conference (Heinke 2004; Grygorowics and Szantyr 2004; Stettler et al. 2004).

This paper summarizes results of recent work completed at M.I.T. (Stettler 2004), which documents the investigation of the steady and unsteady dynamic maneuvering forces associated with an azimuthing podded propulsor, and provides theoretical insight toward understanding their maneuvering attributes and performance. Because of the wide range of potential applications of azimuthing podded propulsion, dynamic force effects applicable to maneuverability of both large and small vehicles have been investigated. These include quasi-steady force effects applicable to any maneuvering vehicle or ship, as well as unsteady or transient force effects which might have more significant application to the maneuverability of smaller vehicles, particularly for precision control applications. The ultimate aim of this paper is to present a basic technical understanding and quantify the dynamic effects associated with azimuthing propulsion, particularly relating to vehicle maneuvering dynamics.

**MANEUVERING DYNAMICS**

For typical marine propulsion and maneuvering simulation and control applications (i.e. conventional ships with shafted propellers or ROV/AUV marine thrusters), equations for the maneuvering dynamics of the vehicle or ship are typically coupled to the dynamics of the main engine or propulsion motor through quasi-steady mappings relating ambient or advance velocity and propeller rotation rate to propeller thrust and torque. However, some recent studies have also considered coupling of propeller thrust and torque to the unsteady fluid velocities in the vicinity of the propeller blades for considering very fast thrust and torque dynamics for precision control applications (Healey et al. 1994; Whitcomb and Yoerger 1999; Bachmeyer et al. 2000; Blank et al. 2000). In any case, the additional complexities of propeller (propulsor) azimuth lead to a necessity to expand the dynamic maneuvering equations to include additional vectored propulsion and steering force components, and expand the propeller/propulsor mappings to include normal force and steering moment, in addition to the vectored (altered) thrust and torque. It is mainly in the character of the vectored thrust force, torque, normal force, and steering moment that maneuvering with azimuthing propulsors differs so significantly from maneuvering with conventional shafted propellers and Rudders, or marine thrusters.

**Intuition**

A fundamental intuition of vectored marine propulsion can be obtained by considering the forces that result from a propeller that is operating in an oblique inflow (Fig. 1). First, the hydrodynamic thrust force (T) and torque (Q) vectors along the axis of the propeller shaft are affected by a reduction in effective axial inflow velocity, as the propeller is azimuthed relative to the inflow. Intuitively, this reduction in effective axial inflow velocity (i.e. with the cosine of the propulsor azimuth angle \( \delta \)) reduces the effective advance coefficient in terms of the inflow velocity to the propeller, and thus increases thrust and torque in accordance with “typical” propeller thrust-torque-speed characterization (i.e. “open-water” characterization) (Fig. 2). Second, a normal force (or side force) \( N \) is created due to unequal angles of incidence on the blade sections as they rotate through the oblique inflow (Crane et al. 1988), and for larger inflow angles at higher advance velocities, the transverse roll-up of the wake along its top and bottom edges, essentially forming two dominant vortex bundles (see for example Leishman 2000 and Leishman 2002). Regardless of the direction of rotation of the propeller, the net normal force is in the same direction, away from the inflow (Crane et al. 1988).

![Fig. 1 - Propeller in oblique inflow. Hydrodynamic torque Q, thrust force T, normal force N, and steering moment M, resulting from oblique inflow. F_s and F_y are equivalent surge force and sway force in the vehicle, ship or towing tank coordinate frame.](image)

Thus one main effect of azimuthing propulsion, in terms of maneuvering forces, is to produce propeller forces which vary in a nonlinear manner with azimuth (inflow) angle, propeller speed, and advance velocity (or non-dimensional advance coefficient \( J=V_s/nD \)). This would suggest a nonlinear 3-dimensional mapping of quasi-steady propeller thrust, torque, and normal force with azimuth angle and advance coefficient. It should be noted that, for an azimuthing podded propulsor, this also implies a coincidental mapping of the hydrodynamic moment on the propulsor about its steering shaft (i.e. the steering moment \( M \)). It is also noted that in addition to the vectored propeller forces, the propulsor pod itself also induces
force contributions resulting from its own effective hydrodynamic characteristic drag and lift forces (or axial force along its axis and normal force perpendicular to its axis), as well as a hydrodynamic moment.

Fig. 2 - “Typical” propeller thrust-torque-speed characterization. Non-dimensional thrust force and torque coefficients (K_T and K_Q) vs. advance coefficient (non-dimensional advance velocity J=V_a/nD). Thrust and torque are maximum at zero advance coefficient and reduce with increasing advance coefficient.

In addition to the nonlinear mappings of quasi-steady propeller thrust, torque, normal force and steering moment, there are also unsteady hydrodynamic force components associated with accelerating and decelerating flows. In terms of maneuvering forces, the term unsteady is used here to denote those force components which arise due to time rate-of-change of the main state variables associated with the azimuthing propulsor (n, V_a, δ). For maneuvering simulation and control of large ships or vehicles, these unsteady hydrodynamic force components are typically neglected, as the time constants are usually at least an order of magnitude faster than the dominant time constants for the maneuvering ship or vehicle. For smaller vehicles, this is not necessarily the case, and consideration of unsteady forces may be necessary, particularly for maneuvering control applications such as precision underwater work vehicles. Therefore, this work considered both the quasi-steady force components as well as the unsteady or transient force components.

Maneuvering Models

In order to tie together and motivate the test program and provide a basis for quantification of the effects of the azimuthing propulsor on vehicle maneuvering, a combined dynamic maneuvering model for a vehicle with azimuthing propulsion is briefly discussed. A combined maneuvering model can be developed based upon fundamental principles of dynamics, and parameters determined through a suitable test program for the various force components and interactions. The dynamics of an azimuthing propulsor can be incorporated into an overall vehicle dynamic maneuvering model by coupling the equations for the vehicle dynamics to those of the propulsor, through quasi-steady or dynamic equations relating velocity and propeller rotation rate to propeller thrust, torque, normal force and steering moment. This relationship, for a surface vehicle with a single azimuthing propulsor driven by a DC motor, can be simply illustrated (Fig. 3). In order to carry out system identification and combined system simulation or control of the maneuvering dynamics of the vehicle, the various dynamic and kinematic relations in the model must be put into suitable forms, whose parameters can be determined (identified) through a reasonable experimental test program.

Fig. 3 - Combined coupled dynamic maneuvering model for a surface vehicle with a DC motor-driven azimuthing propulsor.

In order to motivate the direction of testing of the azimuthing propulsor and highlight the significance of the results in terms of maneuvering forces, simulation or control applications, it is necessary to define a “form” for the equations of motion, or simulation model, which is capable of capturing the relevant dynamics for the maneuvering of the vehicle (and whose parameters can be successfully identified through a suitable experimental test program).

The form of the vehicle dynamic and kinematic relations can be written based upon “standard” nonlinear equations for maneuvering of a ship or vehicle, modified to account for the vectored forces associated with the azimuthing propulsor, plus appropriate interaction terms. Fig. 4 defines relevant earth-fixed (inertial) and vehicle-fixed coordinate frames, as well as forces, velocities and displacements for a surface vehicle maneuvering in the horizontal plane. Combined maneuvering equations of motion can be written based upon any of a number of ship maneuvering models, for example the “Abkowitz” maneuvering models (Abkowitz 1969; Abkowitz 1980; Crane et al. 1988), with modifications to account for the vectored propulsor forces. With the dynamic state variables delineated in Fig. 4 relating to vehicle velocities (u,v,r) and displacements (x_o,Y_o,ψ) as specified by the selected vehicle maneuvering model, a simple yet comprehensive nonlinear maneuvering model for a vehicle maneuvering in the horizontal plane can be developed and written as three coupled equations of the form
where the first equation is for surge, the second for sway, and the third for yaw. The maneuvering coefficients (or derivatives) are defined in a standard way from Taylor series expansions of the hydrodynamic forces around prescribed operating points \((u=u_0, v=0, r=0)\). Here, a 3rd order (nonlinear) expansion has been used (i.e. the “Abkowitz” form), although 2nd order nonlinear expansions have also commonly been used and found to be acceptable for many maneuvering simulation studies (Goodman et al. 1976; Barr 1993; ITTC 2002). Note that many of the derivative terms which result from the Taylor series expansion are neglected in the model due to considerations of symmetry or homogeneity. Also note that the hydrodynamic coefficients as written here are for the “bare hull”, with the terms specifically associated with the azimuthing propulsor accounting for vectored forces, in addition to interaction effects.

\[
\begin{align*}
\dot{u} - rv - xu &= X_0 + X_u \dot{u} + X_{uu} \Delta u + X_{uv} \Delta v + X_{uvr} \Delta r + F_x + (\text{interaction terms}) \\
\dot{v} + ru + xu &= Y_0 + Y_u \Delta u + Y_{uu} \Delta u^2 + Y_{uv} \Delta v + Y_{uvr} \Delta r + F_y + (\text{interaction terms}) \\
\dot{r} + xu &= Z_0 + Z_u \Delta u + Z_{uu} \Delta u^2 + Z_{uv} \Delta v + Z_{uvr} \Delta r + F_z + (\text{interaction terms})
\end{align*}
\]

\( (\Delta u = u - u_0) \)

To complete the maneuvering model, along with the equations of motion, two sets of kinematic relations are required. Referring again to Fig. 4, one set of equations transforms from earth-fixed to vehicle-fixed coordinate frames (Abkowitz 1969; Fossen 1994) and can be written in the form

\[
\begin{align*}
\dot{x} &= u \cos(\psi) - v \sin(\psi) \\
\dot{y} &= u \sin(\psi) + v \cos(\psi) \\
\psi &= r
\end{align*}
\]

where \(u, v, \) and \(r\) are vehicle surge, sway and yaw velocities (respectively) in the vehicle-fixed frame, and \(x, y, \) and \(\psi\) are \(x\) and \(y\) position and yaw angle (respectively) in the earth-fixed frame. Another set of kinematic equations transforms the forces from the propulsor-fixed coordinate frame to the vehicle-fixed coordinate frame, and can be written as

\[
\begin{align*}
F_x &= T(n, V_e, \alpha) \cos \delta - N(n, V_e, \alpha) \sin \delta \\
F_y &= T(n, V_e, \alpha) \sin \delta + N(n, V_e, \alpha) \cos \delta \\
M &= M
\end{align*}
\]

where the effective inflow velocity at the azimuthing propulsor, \(V_e\) (in lieu of advance velocity \(V_a\)) can be written

\[
V_e = \sqrt{u^2 + (v + x_p \tan(\beta - \delta))^2}
\]

where \(x_p\) is the distance from the center of the vehicle reference frame to the center of the propulsor. The effective angle of inflow to the propulsor, \(\alpha\) (in lieu of propulsor azimuth angle \(\delta\)) can be written

\[
\alpha = \delta - \beta = \delta - \tan^{-1}\left(\frac{\sqrt{v^2 + x_p^2}}{u}\right)
\]

where \(\beta\) is the drift angle of the vehicle in the vehicle-fixed frame.

Referring again to Fig. 3, the form of a simple equation relating motor dynamics to propeller torque (referred to as the motor torque equation) can be derived based upon fundamental principles of the torque dynamics of an electric motor. For a direct current permanent magnet motor, this can be written in a “standard” nonlinear form (see for example Electro-craft Corp. (1977))

\[
k_i i_m + B_m(n) + Q
\]

where \(k_i i_m\) (torque constant times motor current) is the motor (input) torque, \(I_m\) is the mechanical “dry” rotary moment of inertia of the motor, shaft and propeller, and \(B_m(n)\) is a general mechanical “dry” stick-slip friction associated with the motor, shaft and seal, and propeller. The latter can be written in a general quadratic form

\[
B_m(n) = k_{r0} \text{sign}(n) + k_{r1} n + k_{r2} n^2
\]

where the first term is a Coulomb friction, and the latter are dynamic friction terms.

![Fig. 4 - Maneuvering coordinate frames. Earth-fixed (inertial) and vehicle-fixed coordinate frames, forces, velocities and displacements for a surface vehicle with an azimuthing podded propulsor maneuvering in the horizontal plane.](image-url)
The propeller (external) hydrodynamic torque $Q$ in general includes both drag (quasi-steady) components and inertial (unsteady) components. One simple way to write this would be by analogy to the mechanical torque as

$$Q = I_a \ddot{\alpha} + Q_p,$$

where the latter term, $Q_p$, is the quasi-steady hydrodynamic torque which is mapped to the quasi-steady propeller states (propeller rate $n$, advance velocity $V_a$, and azimuth angle $\delta$). The former term is an unsteady inertial term in analogy to the mechanical inertia, with $I_a$ being a hydrodynamic (added) rotational inertia. However, since the hydrodynamic (added) inertia is related in general to fluid velocities (vice propeller rate), this term is not easily identifiable in terms of propeller rate $n$, and can depend upon the additional unsteady propulsor state variables ($\dot{\alpha}, \dot{V}, \dot{\delta}, \dot{\phi}$). In fact, the hydrodynamic added inertia coefficient as written here has no physical modeling basis, other than to equate it to some empirical mass of fluid that is accelerated with the propeller, which might be based upon a simplified momentum model.

**Dynamic Inflow Model**

A physically-based yet simple model to represent the first order unsteady dynamics of hydrodynamic torque $Q$, as well as hydrodynamic thrust force $T$, normal force $N$, and steering moment $M$ (i.e. the dynamics of $Q(t)$, $T(t)$, $N(t)$, and $M(t)$), is via a “dynamic inflow” or “wake” time constant $\tau$. A theory of dynamic inflow applied to a marine propeller is discussed in detail by Stettler (2004), and is only summarized here.

“Dynamic inflow” refers to the dynamics associated with the development of the unsteady 3-dimensional shed vortical wake (i.e. the helical wake or slipstream), and its effect on the induced velocity field in the vicinity of the propeller blades (i.e. in accordance with the Biot-Savart Law). This can also be thought of as representing the inertia of the 3-dimensional helical wake, and therefore it has a characteristic time scale. Snel and Schepers (1992; 1993) have provided a thorough and interesting perspective on dynamic inflow pertaining to helicopter and wind-turbine dynamics. They use the term dynamic inflow to indicate the response of the inflow velocities at the rotor disk (helicopters or wind turbines) to changes in the load conditions on the rotor. Fig. 5 provides a simple example, in which the blade pitch angle undergoes a rapid change from an initial value $\beta_1$ to a new value $\beta_2$. Blade-element-momentum theory (or steady vortex wake theory) gives two different equilibrium values of the induced velocities at the disk pertaining to the two pitch angles (i.e. $V_{i1}$ and $V_{i2}$). In reality, there is time needed for the flow to accelerate and the 3-dimensional wake to change. If the pitch change is sufficiently fast, the inflow velocity will essentially still be at the initial value, and only gradually change to the new value. The consequences of this are also shown in Fig. 5 (see also Fig. 6). Instead of instantly changing the blade angle of attack from $\alpha_1$ to $\alpha_2$, as suggested by quasi-steady equilibrium theory, there is an important “overshoot” in the angle of attack (indicated by the heavy line). The actual “overshoot” is dependent on the time scale involved in the adjustment of the inflow (i.e. the development of the changes to the overall helical wake). Consequently to the “overshoot” of the angle of attack, the blade loads (particularly the lift force) also exhibit an “overshoot” (also compared to their equilibrium values). For an ideal step change in blade pitch angle as shown, the induced velocity builds up more or less exponentially, with a given time constant. Conversely, the angle of attack and blade forces, following the initial overshoot transients, decay to their steady-state values, roughly with the same time constant. This time constant is referred to here as the “dynamic inflow time constant,” or “wake time constant”.

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**Fig. 5 – Illustration of the influence of wake inertia (dynamic inflow) on induced velocity, blade angle of attack, and blade lift force after a step change in blade pitch angle. Basic graphic adapted from Snel and Schepers (1993).**

**Fig. 6 – Simplified blade-section velocities and forces (i.e. “blade element theory”) for a propeller in oblique inflow, neglecting slip stream rotation.**
Thus, by blade element theory, as a step change in blade pitch angle \( \beta \) causes the induced velocity at the disk to build-up with first order dynamic inflow time constant \( \tau \), the local forces at the propeller blade (i.e. lift and drag, or thrust force and torque force) see a sudden overshoot, and then decay approximately with the same dynamic inflow time constant \( \tau \). Further, Fig. 6 indicates that from a rapid (say a step) increase in propeller rate \( n \) comes a rapid increase in blade angle of attack \( \alpha \). This sudden increase in blade angle of attack directly increases lift and drag (or thrust and torque) forces through the blade lift characteristics. The converse effects are seen with a sudden decrease in propeller rate \( n \). Thus, the net effect is that the thrust force, torque, normal force and steering moment can all be expected to “lead” a change in propeller rate with the dynamic inflow time constant \( \tau \).

This same dynamic inflow time constant \( \tau \) is also theoretically applicable for sudden changes in the other propeller states (advance velocity \( V_a \) and propulsor azimuth angle \( \delta \)). Looking again at Fig. 6, it should be clear that sudden changes in either advance velocity \( V_a \) or propulsor azimuth angle \( \delta \) directly affect the angle of attack \( \alpha \) and thus should theoretically have the same time constant \( \tau \) for change in induced velocity at the disk or change in the blade (and propeller) forces. For example, a sudden increase in propulsor azimuth angle \( \delta \) (say from zero to some moderate or large angle), reduces the effective inflow velocity \( V_a \cos \delta \), and thus increases the blade angle of attack and blade forces. Thus, the blade forces should lead a change in azimuth angle approximately with the first order dynamic inflow time constant \( \tau \). This will be shown subsequently in the experimental results.

In summary, the basis for the use of a dynamic inflow time constant lies in the physical inertia of the vortex wake, and its corresponding effect on induced velocities at the propeller disk, and ultimately on the blade forces. Using this concept of a dynamic inflow time constant \( \tau \), the time-dependent propeller torque, thrust force, normal force and steering moment can be represented using the first order differential equations

\[
\begin{align*}
Q(t) &\approx Q_p(n, V_a, \delta) + \tau \frac{dQ_p(n, V_a, \delta)}{dt} \\
T(t) &\approx T_p(n, V_a, \delta) + \tau \frac{dT_p(n, V_a, \delta)}{dt} \\
N(t) &\approx N_p(n, V_a, \delta) + \tau \frac{dN_p(n, V_a, \delta)}{dt} \\
M(t) &\approx M_p(n, V_a, \delta) + \tau \frac{dM_p(n, V_a, \delta)}{dt}
\end{align*}
\]

(9)

where \( Q_p(n, V_a, \delta) \), \( T_p(n, V_a, \delta) \), \( N_p(n, V_a, \delta) \) and \( M_p(n, V_a, \delta) \) are the quasi-steady propulsor force mappings (i.e. from quasi-steady experiments), and the time constant \( \tau \) is the dynamic inflow time constant. The argument for applying the same dynamic inflow time constant \( \tau \) for all the propeller forces follows from development of the dynamic inflow theory just discussed (see also Stettler 2004). Eq. 9 could be written in dimensionless form, so that the dynamic inflow time constant \( \tau \) would be in terms of the propeller revolutions (or radians). Note that these are linearized relations, in that they are linearized about the current propulsor state \( (n, V_a, \delta) \), and therefore are applied at each time \( t \) in the simulation or control application.

It is acknowledged that this representation of unsteady propeller forces is different than some recent studies which have also considered unsteady thrust and torque dynamics for marine thrusters (Healey et al. 1994; Whitcomb and Yoerger 1999; Bachmayer et al. 2000; Blanke et al. 2000). In those studies, the unsteady propeller thrust (and torque in one case) was considered based upon an empirical added mass (inertia) which was modeled using a simplified unsteady momentum approach, but required measurement (or estimation) of the fluid velocities in the vicinity of the propeller disk. Although this approach using a dynamic inflow time constant and differential equations could also be approached from an empirical perspective, it is also based upon a fundamental model of the vortex wake and its interaction with the unsteady propeller states through induced velocity at the propeller disk.

**Maneuvering Dynamics Summary**

To recapitulate: referring again to Figs. 1-3, the motor torque dynamic eq. 6 is coupled to the full nonlinear vehicle dynamic maneuvering eq. 1 through the propeller hydrodynamic forces and torques \( (Q(t), T(t), N(t), \text{and } M(t)) \). This coupling can be written in terms of a dynamic inflow time constant \( \tau \) and the quasi-steady mappings using the first order dynamic representation eq. 9.

The primary propulsor forces and torques investigated and discussed in this paper are thrust force, normal force, propeller torque, and steering moment, all in the propulsor or propeller coordinate frame. These are based upon the “traditionally” reported propeller force characterizations. However, the characterizations of surge force \( F_s \) and sway force \( F_w \), as shown in Figs. 1 and 4, are also reported and discussed in order to provide a different perspective on maneuvering forces. All of the quasi-steady forces and torque mappings are parameterized to azimuth angle \( \delta \), as well as propeller rate \( n \), and ambient or advance velocity \( V_a \) (or nondimensional advance coefficient \( J=V_a/nD \)).

Finally, note also that, although one simple dynamic equation (eq. 6) has been written here for the DC motor torque, more complex equations of the same basic form could be written for electric motors (or other prime-movers) with more complex dynamics, so long as the motor or engine dynamics remain coupled to the vehicle dynamics through the hydrodynamic forces and torques.

**EXPERIMENTAL SETUP**

In order to investigate the combined maneuvering dynamics of a vehicle propelled by a dynamically-azimuthed podded propulsor, a 12 foot autonomous surface vehicle propelled with a single dynamically-azimuthed podded propulsor was designed and constructed (Fig. 7). The autonomous surface vehicle is of
a modular design, so that the various components could be separated and tested as subsystems.

The test vehicle was designed and built on a modified 12’2” (3.7 meter) fiberglass kayak hull. The vehicle is controlled by an onboard computer, either autonomously or remotely-controlled using a shore-based PC. The onboard Pentium-based PC104 computer system (which includes data acquisition and control I/O and Ethernet cards) provides serial and analog data acquisition and control capability, with interface to the shore-based “host” laptop PC through a wireless Ethernet bridge. Data acquisition and control functions utilize the xPC Target® software package (Mathworks, Inc.). The system also includes PWM brush servo amplifiers, motor current sensor, and 24 volt power. The propulsor itself is a modified trolling motor (Motorguide® model ET54 with 3-bladed Machete II aluminum propeller), which has been modified using a Hall-effect latching magnetic sensor (with magnets placed inside the propeller hub), enabling closed-loop speed control. The overall propulsor is 13 inches (30 cm) in length and 3.625 inches (9 cm) in diameter, with the open-bladed fixed-pitch propeller being 9.875 inches (25 cm) in diameter. The propulsor is dynamically-azimuthed by a servo gearmotor, with an angle sensor enabling closed-loop dynamic azimuth control. Both motors are permanent magnet DC and are controlled using the PWM brush servo amplifiers in the torque (current) control mode. Further details of the system components, and the precise geometry of the propulsor and propeller, are provided by Stettler (2004).

Quasi-steady and unsteady/transient force testing of the azimuthing propulsor was conducted using two configurations. First, the modular components of the dynamically-azimuthed propulsor system were installed into a test fixture including a dynamometer and attached to the MIT towing tank carriage (Fig. 8). Second, the entire autonomous surface test vehicle, with the dynamically-azimuthed propulsor system, was installed and tested on the Planar Motion Mechanism (PMM) at the U.S. Naval Academy (Fig. 7). In both configurations, quasi-steady force measurements were made over the entire desired range of quasi-steady operating conditions. In the former, a series of unsteady/transient force tests and measurements were completed, and a series of wake visualizations utilizing a new fluorescent paint method were also completed. These tests and visualizations, and a brief presentation of results will be discussed subsequently. In addition to the integrated propulsor testing, several tests were conducted specifically to identify the necessary propulsion motor, friction, and inertia parameters discussed in the previous section.

Testing in the MIT towing tank was conducted utilizing a test fixture incorporating an AMTI® model MC6 six axis load cell and amplifier (Fig. 8). Both propulsor and servo gearmotor were founded within a structure attached below the load cell. The propulsor was dynamically-azimuthed in the horizontal plane with Rulon shaft bearings though a hydrodynamic strut. Because of the large moment arm, the load cell output was calibrated daily in place. Data acquisition and control utilized the modular components of the autonomous surface vehicle.

Testing of the quasi-steady maneuvering forces of the propulsor was also conducted on the PMM in the large towing tank at the U.S. Naval Academy using the entire autonomous surface test vehicle (Fig. 7). The vehicle was attached to the carriage via four 50 pound variable-reluctance modular block force gauges manufactured by Hydronautics, Inc. The force gauges were calibrated on a special calibration stand with their corresponding amplifiers. Data acquisition and control utilized the modular components of the autonomous surface vehicle. Propulsor forces were determined from the measurements by deducting the contribution of the bare hull. Note that because of the very large separation between the hull and the propeller (by design) the hull wake and boundary layer are well outside the propeller slipstream and therefore the interaction effects are considered minimal, and are mainly captured by this method using the bare hull deduction.

Additional wake visualizations and documentation were conducted with the dynamically-azimuthed propulsor system

Fig. 7 – Autonomous surface test vehicle with dynamically-azimuthed propulsor on Planar Motion Mechanism (PMM) at the U.S. Naval Academy.

Fig. 8 - Dynamically-azimuthed propulsor in test fixture in MIT towing tank.
installed in the MIT recirculating water tunnel, utilizing an existing test fixture incorporating three coplanar axial strain-gauge load cells with amplifiers (Fig. 9). The servo gearmotor was attached to a floating collar on the fixture, and the propulsor was dynamically-azimuthed in the horizontal plane with Rulon shaft bearings through the floating collar. Data acquisition and control utilized the modular components of the autonomous surface vehicle. The main purpose of the testing in the water tunnel was to conduct dynamic wake flow visualization using particle image velocimetry (PIV) and cavitation visualization using high-speed video. The PIV utilized a New Wave Research Gemini® dual head laser PIV system, with LaVision DaVis® software for control of the laser and camera systems. The wake was illuminated using a horizontal laser sheet, with CCD camera acquiring images of the wake from below; thus mid-plane horizontal wake cut 2D velocities were measured. In the PIV system internal triggering mode, lasers were fired at approximately 7.5 Hz. In the external triggering mode, lasers were synchronized to the propeller rotation. Internal triggering was utilized to obtain instantaneous results for dynamic wake (unsteady) conditions, and external triggering was utilized to obtain phase-averaged results for quasi-steady conditions.

**Identification of Torque Equation Parameters**

Since it was impractical to install a propeller torque load cell in the small propulsor, the propeller hydrodynamic torque was calculated from accurate motor current measurement, based upon the motor torque equation (eq. 6), by first experimentally identifying the motor torque constant, mechanical “dry” friction coefficients, and mechanical “dry” rotational inertia. The torque constant $k_t$ for the propulsion motor was determined by driving the propeller with another motor, measuring rotation speed, and measuring the voltage generated across the motor terminals (the back EMF voltage). The slope of the linear curve fit of shaft speed vs. measured voltage provides the voltage (or speed) constant $k_{EMF}$ (Fig. 10). By SI unit equivalence, $k_t$ (in N-m/A) is equivalent to $K_{EMF}$ (in V-s/rad). Referring to eqs. 6 and 7, the coefficients of the mechanical “dry” friction $B_m(n)$ and mechanical “dry” rotational inertia $I_m$ were determined by performing a series of step current input tests in air, where propeller hydrodynamic torque is negligible (note: the shaft seals were maintained wet to maintain lubrication). The plot of steady-state propeller speed vs. input torque ($k_{i_m}$) was fit with a quadratic to determine the coefficients of $B_m(n)$ (Fig. 11). $I_m$ was determined by averaging the results of 10 current input step tests over a range of current input step magnitudes. These experiments were checked for repeatability and consistency over the time span of the testing program.

**PROPULSOR FORCE EXPERIMENTS**

A number of different types of tests were conducted specifically to identify the fundamental parameters associated with the azimuthing propulsor’s contributions to the maneuvering equations (i.e. vectored force mappings, time constants, inertias, etc.). Tests included both quasi-steady (i.e. constant in propeller speed, carriage velocity, and azimuth angle), as well as a number of dynamic tests (such as step current input, ramp current input, sinusoidal current input, step azimuth command, and sinusoidal azimuth). An overview of these tests and results is presented.
Fig. 12 - Vectored propulsor forces for towing tank and PMM testing.

Fig. 15 shows thrust and torque for the entire range of azimuth angles ($\delta$ to ±180°). Despite the apparent “scatter” for larger angles ($|\delta|>90°$), there are also clearly regular nonlinear features associated with the changing wake character. Note that thrust is taken from the force load cell, while torque is calculated from the motor current (i.e. there is no sensor correlation), yet the nonlinear character of the two plots is very similar, including locations of maxima and minima. Thus, the basic features of both thrust and torque plots over most of the parameter space, are real quasi-steady force effects (i.e. not load cell measurement irregularity or data scatter).

The apparent “scatter” in Fig. 15 at larger angles ($|\delta|>90°$) for smaller advance velocities is due to the highly unsteady nature of the reversing wake (the so-called “crash-back” condition), and the resulting difficulty in calculating the 0th Fourier harmonic from the necessarily truncated unsteady, quasi-periodic force data for each test run (Stettler 2004; Jiang et al. 1997). It can also be seen from Fig. 15 that for larger angles, as velocity is increased, the Fourier averaging results in a much smoother plot of the quasi-steady force components. This is because, at higher advance velocities, the unsteady wake becomes even more regularly quasi-periodic, and therefore the Fourier averaging technique is more successful, since there is a more dominant period about which to truncate the data. This increase in periodicity with increase in carriage speed was visually observed during the testing. As the carriage speed was increased, stronger vortex shedding from the reversing (“crash-back”) unsteady wake was observed.

Figs. 16 and 17 provide a different presentation of the quasi-steady results in the form of contour plots for each of the tested advance coefficients, providing a more detailed view of the data. Note the small asymmetry in the force data (particularly noticeable in terms of locations and magnitudes of maxima and inflection points between 0 and ±90°). These force asymmetries are due to the influence of the pod housing and strut in front of the propeller, and the effect of the direction of propeller rotation (here a left-handed propeller). Specifically, the strut or steering shaft, being in front of the propeller, causes a blockage of the flow into the propeller across the top of the pod, and results in an induced swirl around the pod housing and
into the propeller, in addition to the swirl induced by the normal propeller rotation. This additional induced swirl causes an asymmetric swirl inflow into the propeller (i.e. for a positive propulsor rotation angle $\delta$, it increases the normal counter-clockwise inflow swirl to the propeller, and for a negative propulsor angle $\delta$, it decreases the normal counter-clockwise inflow swirl to the propeller). The net result is decreased angle of attack at the blades for positive propulsor rotation (decreased lift and lift-induced drag) and increased angle of attack at the blades for negative propulsor rotation (increased lift and lift-induced drag). This type of force asymmetry for “pusher” type pods and thrusters can be seen in the data presented by Norrby and Ridley (1980), and more recently in Heinke (2004) and Grygorowicz and Szantyr (2004), but the causal effects were not discussed.

While the presentation of force data in terms of thrust and normal force (i.e. in the coordinate system of the propulsor) is more “traditional”, it is perhaps more instructive in terms of maneuvering performance to present the forces in the coordinate system of the towing tank. This is similar to the forces in the coordinate system of a vehicle or ship in which the propulsor would be installed, with the modification that the actual inflow velocity and angle of attack seen by the propulsor in the vehicle would need to be corrected for the effects of the vehicle’s velocities, yaw rate, propulsor location, and wake fraction (eqs. 2-5). Nevertheless, the forces in the coordinate system of the towing tank have been termed surge force ($F_x$) and sway force ($F_y$), to be consistent with the vehicle force terminology. Fig. 18 shows surge force and sway force in the form of surface data mesh plots over the entire range of azimuth angles ($\delta$ to $\pm 90^\circ$). Again, the smooth but nonlinear nature is evident. The very intuitive variation of surge force (with $\cos \delta$) and sway force (with $\sin \delta$) in the limit at $J=0$ is obvious. As an aside, it is also
interesting to note the sudden drop in surge force $F_x$ between $\pm 150^\circ$ and $\pm 180^\circ$ (an increase in the negative $F_x$) at $J \approx 0.3$; this is the infamous “vortex ring state” (Leishman 2000), well known to the helicopter community for causing sudden drops in rotor thrust, with particularly dire consequences in terms of loss of flight control for the VTOL aircraft. Fig. 19 presents $F_x$ and $F_y$ in the form of contour plots for each of the tested advance coefficients. It is very interesting to note the trends of both $F_x$ and $F_y$ in the moderate range of azimuth angles (say up to $\pm 45^\circ$), and in the range of a typical vehicle design advance coefficient for this propeller ($J \approx 0.36$ for the MIT vehicle). In this range, $F_x$ is nearly linear (and almost constant), and $F_y$ is nearly linear. The implications of this in terms of vehicle maneuvering and control are significant, in that there is a strong possibility for linearization, even decoupling, in the surge-sway-yaw vehicle maneuvering control problem, even for large angle propulsor deflections!

Fig. 15 - Surface plots of quasi-steady thrust and torque ($\delta$ to $\pm 180^\circ$). Data points shown, surface plots are bicubic interpolation of data points.

Fig. 16 - Contour plots of quasi-steady thrust force, torque, and normal force ($\delta$ to $\pm 180^\circ$). Data points shown, contour plots are cubic interpolation of data points.
Fig. 17 - Contour plots of quasi-steady thrust force and normal force (\(\delta\) to ±90°).

Fig. 18 - Surface plots of quasi-steady surge force (\(F_x\)), sway force (\(F_y\)) (\(\delta\) to ±180°). Data points shown, surface plots are bicubic interpolation of data points.
Unsteady (Transient) Propulsor Forces

The development of unsteady propeller forces (thrust, torque, normal force, and steering moment) due to time-rate-of-change in the propeller operating states \((n, V_a, \delta)\) was briefly introduced in the previous section. The idea of modeling these unsteady propeller forces via first order differential equations using a dynamic inflow time constant \(\tau\) was also presented as a reasonable physically-motivated representation of the first order dynamics of the propeller forces. In this section, the investigation of an empirical dynamic inflow time constant representation of the first order propulsor force dynamics is discussed.

In previous studies which have experimentally investigated unsteady propeller force dynamics for marine thrusters (Yoerger et al. 1990; Healey et al. 1994; Whitcomb and Yoerger 1999; Bachmayer et al. 2000) (all at zero advance velocity), parameters based upon simple momentum models used to model the steady and unsteady thrust dynamics were identified through application of a number of different simple input response tests. A similar approach has been taken for this study; however the parameterization of a first order dynamic inflow time constant \(\tau\) has been pursued, vice empirical added inertias (although the two approaches are essentially equivalent in terms of the system dynamics). Because the propulsor dynamics of interest are those related to the maneuvering forces, the propulsor testing focused on the characterization of the first order time constant through a series of tests which target characterization of first order system dynamics. To this end, several of the developed dynamic test methods were based upon “established” dynamic test procedures for mechanical first order dynamic systems (such as step and saturated ramp input response tests). Several others are variations of harmonic or sinusoidal test procedures, but utilized in a slightly different manner. A good overview of mechanical system dynamics and test procedures is provided by Doebelin (1998), and their application to propulsor force dynamics is discussed by Stettler (2004).

One type of dynamic test utilized was the current input step test. In these tests, a step increase in the current to the propulsor motor was applied, followed by a step decrease after a suitable amount of time at steady-state (Fig. 20). The linear time constant for the response of the propeller rate \(n\) provides a characteristic linear time constant for the overall dynamic system. The hydrodynamic portion of the linear characteristic response is calculated by linear superposition using the previously identified mechanical rotary inertia coefficient, using a modification to eq. 6. This hydrodynamic portion of the overall time constant is the dynamic inflow time constant \(\tau\). Fig. 21 shows typical results for a step current increase and a step current decrease. All of the variables have been appropriately normalized, and the total system time constant is obtained using a “log-slope” method. An entire series of current input step tests were performed over the range of azimuth angles \(\delta\) to \(\pm 90^\circ\) and for 3 representative velocities (Fig. 22). Here the dynamic inflow time constant \(\tau\) has been non-dimensionalized to propeller revolutions (Stettler 2004).

One of the most interesting observations regarding the time constant results is that there appears to be an “asymmetry” in the response to a rapid current increase and rapid current decrease. The step current increase apparently results in a faster hydrodynamic time constant than the corresponding step current decrease! Additionally, a slight increase in dynamic inflow time constant is observed with increase in forward velocity.
The asymmetry in force results seen with the current input step testing is more easily observed using a current input that is a saturated ramp. The details of this test and results are provided by Stettler (2004). The net result is that an approximation of the dynamic inflow time constant $\tau$ can be measured directly by the lag in propeller rate behind the thrust force, for the normalized ramp response, as shown in Fig. 23. Visually more obvious than the current input step tests, the ramp tests indicate that the time constant associated with a rapid increase in propeller rate is apparently faster than that of a corresponding rapid decrease in propeller rate.

Interestingly, in published results provided by Whitcomb and Yoerger (1999) and Bachmeyer et al. (2000), plots of step current increase and step current decrease did not appear to show this obvious asymmetry. This might at first seem curious. However, it is noted that all of those tests were conducted using ROV marine thrusters designed for low speed ROV applications, and the thrusters were therefore equipped with symmetric constant pitch propellers with zero rake and skew (for zero vehicle speed bi-directional thrust equivalence), and were also fully ducted. This latter difference, as it turns out, may have a significant impact on this force asymmetry.

One plausible explanation for the observed force asymmetry may be described in terms of the unsteady 3-dimensional vorticity that is shed into the wake with a rapid increase in propeller rate. For a large step current increase to the propulsion motor, and the resulting rapid increase in propeller rate, a large amount of vorticity is shed into the wake. In conjunction with this, a rapid acceleration of the fluid behind the propeller takes place in the generation or strengthening of the slipstream. This rapid slipstream acceleration is jet-like, in that it accelerates quickly relative to the fluid outside the slipstream. Owing to viscosity, the shear layer shed from the blades rolls up into a 3-dimensional vortex ring, which quickly plumes outward, then convects downstream as the slipstream...

Fig. 20 – Propulsor current input step test.

Fig. 22 – Dynamic inflow time constant calculated from current input step tests. Non-dimensionalized by average propeller rate.

Fig. 21 – Example current input step response. Normalized change in motor current (torque) step input, normalized change in thrust and propeller rate for step current increase (top) and step current decrease (bottom). Approximate dynamic inflow time constant using “log-slope” method.
convects. In consideration to the Biot-Savart Law, this vortex ring induces additional axial velocity at the propeller disk, in effect decreasing the angle of attack at the blades (see Fig. 6). Based on the results of flow visualizations (to be discussed subsequently), this vortex ring could be quite large, and therefore could induce substantial axial velocity at the propeller disk. The physics of the generation of the vortex ring, and its impact on the dynamic inflow time constant is discussed in detail by Stettler (2004). It is also interesting to note that the marine thrusters used in the experiments conducted by Whitcomb and Yoerger (1999) and Bachmeyer et al. (2000) were fully ducted. This is significant in that then the large vortex ring would not have formed until the acceleration of the slipstream progressed beyond the end of duct. Then, by the Biot-Savart Law, its ability to induce velocities at the propeller blades would be effectively removed (not to mention the fact that the ducting around the propeller would effectively shield it from seeing the effect of the ring vortex).

It should also be noted that the small negative dynamic inflow time constant \( \tau \) shown for some of the test results in Fig. 22 (for the step increase in current) does not imply that the dynamic system has a negative time constant (i.e. infinite bandwidth). It may be recalled from eq. 9 that the dynamic inflow time constant \( \tau \) could also be thought of as representing the time constant of lead in propeller forces for a sudden change in a propeller state (here the propeller rate \( n \)). It therefore only represents the hydrodynamic portion of the first order system dynamics, the mechanical inertia of the system ensures a positive overall system time constant (as can be clearly seen from the composite step response record shown in Fig. 20). Further, the apparent negative time constant for some of the results with the step current increase is consistent with the overall asymmetry in results, including the formation of a vortex ring (Stettler 2004).

Finally, it is also clear from Fig. 22 that the variation of dynamic inflow time constant with propulsor azimuth angle \( \delta \) and advance or carriage velocity \( V_a \) is generally negligible (at least for the rapid step decrease). For the rapid step increase, where a vortex ring forms, the effect of the vortex ring on the induced velocities at the disk is decreased with ambient/advance velocity. This is consistent with vortex ring formation (Stettler 2004).

In addition to investigation of unsteady propeller rate dynamics using motor current input tests, several dynamic test types were also conducted to investigate propulsor azimuth rate dynamics. As an example, sinusoidal azimuth tests were completed in order to approximate the dynamic inflow time constant, in terms of a rapid change in propulsor azimuth angle, or a propulsor azimuth maneuver. A zero offset sinusoidal azimuth was commanded by the control system, for different frequencies (controlled using the feedback control system), with a resulting quasi-sinusoidal response in measured sway force \( F_y \). Example results are shown in Fig. 24 (both are appropriately normalized to their respective steady-state amplitudes). The lead of the sway force compared to the azimuth angle, measured at their respective normalized zero crossings, provides a measurement of the dynamic inflow time constant \( \tau \) (Stettler 2004). The figure shows the approximation of the dynamic inflow time constant in this manner for frequencies of 0.25 and 0.5 Hz (with maximum angular rates of 25°/sec and 49°/sec, respectively) for a velocity of 3.05 ft/s and 700 RPM (11.667 rev/s). Both frequencies yielded the same dynamic inflow time constant: \( \tau \approx 0.06 \text{ sec} \approx 0.7 \text{ rev} \). It is noted that both frequencies provide a consistent dynamic inflow time constant.
**Fluorescent Paint Wake Visualization**

A new fluorescent paint visualization technique was developed for the visualization and documentation of the quasi-steady and unsteady propulsor wake. In addition, it was desired to compare the evolution of unsteady propulsor forces during a maneuver with the evolution of the helical wake during the maneuver. The visualization was carried out using a fluorescent paint applied to a single propeller blade, with the wake fluoresced under ultraviolet black light. The fluoresced wake was recorded using digital video for subsequent video and photo-analysis.

The visualization was carried out in the MIT towing tank using the same test fixture used for the quasi-steady and dynamic test series discussed previously. The wake was illuminated using several banks of ultraviolet black light attached to the carriage assembly, overhead of the propulsor. A digital video camera was also attached to the carriage assembly overhead of the propulsor, looking directly down at the wake. Thus, video and still image sets were obtained from above, through the free surface, as the propulsor was towed on the test fixture. The free surface did, unfortunately, introduce some minor image distortion, but was believed to allow reasonable results for basic helical wake visualization. In addition to the overhead visualizations, two visualizations were conducted with the digital video camera mounted underwater, inside a clear acrylic window fixture, looking forward at the propulsor and the wake.

A clear acrylic-based (water-soluble) paint leveling gel was used as a base for the fluorescent mixture applied to the single propeller blade. To the base was added Fluorescein Sodium powder, mixed into a thin paste, and brushed onto both sides of the single blade. The applied paste was allowed to dry for 3-4 hours.

For each visualization run, the propeller was installed onto the propulsor by hand, and the camera set to record. Within 30 seconds of propeller installation, the carriage motion was initiated and the programmed propulsor sequence (i.e. sequence trajectory of propeller speed and azimuth angle controlled by the control system) was executed. All runs were controlled to execute in entirety within 90 seconds of installation of the propeller.

The visualization was carried out for two sets of advance coefficients (combinations of carriage velocity and propeller rate) and five azimuth angles (0°, ±30°, and ±60°). Additionally, each change in azimuth angle was executed as a fast ramp (i.e. a step command change to the azimuth gearmotor) during each run, so that the transient wake effects could also be visualized and correlated with force measurements.

It should be noted that, because of the rapid diffusion of the fluorescent paint, there was a practical limitation in terms of propeller rotation rate that could be used. Here, propeller rates of 240 and 300 RPM were used (this was determined through a
By varying propeller rate, carriage velocity and propulsor azimuth angle, a small matrix of quasi-steady operating test conditions was completed. After each set of visualizations, a set of still image sequences was created from the digital video, with frequency timed to the rotation rate of the propeller (six images per propeller revolution in all cases). Ten images for each of the test conditions were then measured using a dimensioning software. For each image, an approximate helical wake pitch (distance) was measured on both sides of the wake, as well as an approximate wake angle (i.e. for the “near” and “intermediate” wake). The theoretical blade helix surface can be thought of as a continuous distribution of helices between tip and root. Note that the blade pitch (distance) is theoretically identical for both tip helix and root helices, but in reality depends upon the local radial blade pitch angle which depends upon the blade design. There is however, a roll-up of the wake in the tip region (the tip vortices) which distorts the blade helix surface which is visualized in these experiments, making measurement of the “true” helix pitch more difficult. Sample images for several of the test matrix conditions are shown in Fig. 25. Because of the roll-up of the wake in the tip region, pitch measurements were made at the mid-blade region of the wake. Measurements for each of the ten images for each condition were averaged, and tabulated, as provided in Table 1. The results are provided in non-dimensional form (i.e. pitch/diameter ratio (P/D) and wake angle). The wake angle (referred to as the wake “skew” angle by the helicopter community) is the angle of the near wake in the horizontal plane from the propeller shaft axis, as shown in Fig. 25.

The results listed in Table 1 indicate that the P/D ratio does not appear to change with even the large change in azimuth angle, only with advance velocity or propeller rate. To understand why this might be so, consider the definition of pitch angle \( \phi \) at a blade radius \( r \) (Van Manen and Van Oossanen 1988)

\[
\tan \phi(r) = \frac{P}{2\pi r} \tag{11}
\]

where \( P \) is the pitch (distance), the distance between turns of the wake helix. The pitch angle at blade radius \( r \) can also be written as

\[
\tan \phi(r) = \frac{(V_A + V_{Ai})}{2\pi r n} \tag{12}
\]

where \( V_A \) is the axial component of inflow velocity at the propeller disk and \( V_{Ai} \) is the axial induced velocity at the propeller disk (together they make up the total axial velocity at the disk). Note that this representation includes account for propeller slip (i.e. it is not assumed that the propeller is lightly loaded). Equating eqs. 11 and 12 and using the dependence of axial inflow velocity on propulsor azimuth angle (see Fig. 1)

\[
P = \frac{(V_A + V_{Ai})}{n} \Rightarrow \frac{P}{D} = \frac{(V_A + V_{Ai})}{nD} = J\cos\delta + \frac{V_{Ai}}{nD} \tag{13}
\]

Thus, the pitch/diameter ratio of the helical wake for an azimuthing propulsor is dependent on the advance ratio \( J \) and azimuth angle \( \delta \), but is also dependent upon the axial induced velocity \( V_{Ai} \), which depends on propeller blade loading. Although the contribution of the first term on the right hand side of eq. 13 is reduced when the propulsor is azimuthed, the second term is increased when the propulsor is azimuthed, since the loading of the propeller increases. Thus, there is an apparent cancellation-effect with propulsor azimuth in terms of pitch/diameter ratio. Note also that eq. 13 could theoretically be used to calculate an approximate mean axial induced velocity, based upon the P/D ratio measured from the visualization.

Measurement of blade pitch on both sides of the propeller wake, as shown in Fig. 25, provides an illustration of the wake distortion which occurs when the propeller is subjected to oblique inflow. The upstream side of the wake is stretched, while the downstream side of the wake is compressed. The net effect is a slight difference in wake pitch as measured on the upstream and downstream sides of the wake. The blade pitch/diameter ratios calculated and used in Table 1 are taken from the mean of the upstream and downstream wake pitch measurements from each image. Table 1 also shows that the P/D ratio is greater for the condition with the moderate propeller loading (J=0.2) and less for the condition with the light propeller loading (J=0.4). This is as expected. The theoretical no-slip P/D (i.e. the “no load” condition, at zero net thrust) is approximately 0.43 (see Fig. 2, with accounting for the small parasitic drag associated with the propulsor pod). The P/D for the light loading condition is closer to the theoretical no-slip P/D ratio, as expected.

From observations made following the conduct of smoke wake studies of helicopter rotors in the early 1950s, R.B. Gray (1956; 1992) concluded that the wake from a blade consisted of a strong tip vortex and an inner vortex sheet of opposite sense. Gray observed that the outer part of the sheet moved faster than the inner part, and that the outer part of the sheet moved much faster than the tip vortex. Landgrebe conducted additional smoke tests which confirmed Gray’s result and also reported that the tip vortices do not necessarily occur at the ends of the corresponding sheet, but rather lag behind (Landgrebe 1971; Done and Balmford 2001). It was reported that the blade tip vortex moved downwards relatively slowly until it passed beneath the following blade, from which point it moved down more rapidly, although always lagging behind the corresponding blade wake. Although the blade geometry and loading distribution of a helicopter blade is clearly different than a “typical” marine propeller, it can be expected that the same basic relationship between blade and tip helices would remain. This relation was clearly observed from the paint visualization, and was the main initial source of difficulty in consistently
measuring blade wake pitch as previously noted. As the blade wake is shed and convects in the wake, the region of the wake in the tip region (i.e. the tip vortex) can be clearly seen to move much more slowly than the rest of the blade wake (see Stettler (2004) for additional visualizations).

Each visualization run also included 3 rapid transient changes in azimuth angle (for example from 0° to 60°, 60° to -60°, and -60° to 0°). The rapid changes amounted to rapid saturated ramp azimuth changes (from a step command change to the azimuth gearmotor). The purpose of these rapid dynamic azimuth changes was to investigate the correlation between the change in propulsor maneuvering forces (e.g. sway force F_a) and the progression of the helical wake during a very rapid propulsor azimuth maneuver. Fig. 26 provides an example of several wake images taken during a rapid azimuth maneuver (here from 0° to 60°) and the corresponding normalized change in sway force (F_a). Despite the large amount of “noise” in the force signal due to the resonant vibration of the test fixture, the fundamental result is rather clear. As can be clearly seen from the sequence, the maximum sway force has already been reached upon the completion of the azimuth maneuver (i.e. upon the propulsor reaching 60°). Another way of looking at this is to note that for the image at t=20.60 seconds, where the sway force has already reached its maximum, the blade wake has not progressed. In other words, the propulsor force leads the azimuth angle of the propulsor! This is consistent with the “dynamic inflow” model, and is also consistent with the results of sinusoidal azimuth tests discussed previously.

As mentioned previously, a plausible explanation for the asymmetry seen in the force tests for unsteady propeller rate may be described in terms of the unsteady 3-dimensional vorticity that is generated and shed into the wake with a rapid increase in propeller rate. The jet-like acceleration of the slipstream, in conjunction with viscosity, results in the formation of a 3-dimensional ring vortex, which quickly plumes outward, then convects downstream with the slipstream. In consideration to the Biot-Savart Law, this vortex ring induces additional velocity at the propeller disk, effectively decreasing the angle of attack at the blades. This conception is made partially based upon the visualizations of the unsteady wake with the rapid increase in propeller rate due to a step increase in motor current. Fig. 27 provides an example image from a sequence showing the formation of a vortex ring, which has been formed as the vorticity is rapidly shed from the blades following a rapid motor current increase. Owing to its size and location, it is easy to see that the toroidal vortex ring would have a substantial influence on the induced velocities at the propeller disk. Specifically, given the direction of the rotation of the vortex ring, it increases axial velocity at the propeller disk, with the effect of decreasing the angle of attack at the blades, and therefore decreasing the blade forces (thrust, torque) (see Fig. 6). This provides a potent potential explanation for the asymmetry seen in the experimental determination of a dynamic inflow time constant (Fig. 22). Stettler (2004) provides additional fluorescent paint image sequences showing the growth and convection of the vortex ring under several propeller operating conditions, and provides a theoretical basis for its formation, growth, convection, and influence on velocities in the vicinity of the propeller disk.

Table 1. Fluorescent paint visualization. Summary of graphical measurement of helical wake parameters. First helix P/D taken at mid-blade. First helix P/D and wake angle are averages taken over 10 measured image sets for each operating condition.

<table>
<thead>
<tr>
<th>V_a (ft/s)</th>
<th>n (rev/s)</th>
<th>J = V_a / nD</th>
<th>δ (deg)</th>
<th>First helix P/D</th>
<th>Wake angle (deg)</th>
</tr>
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<tr>
<td>0.82</td>
<td>5.0</td>
<td>0.2</td>
<td>-60</td>
<td>0.56</td>
<td>15</td>
</tr>
<tr>
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<td>5.0</td>
<td>0.2</td>
<td>-30</td>
<td>0.56</td>
<td>11</td>
</tr>
<tr>
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<td>0.2</td>
<td>0</td>
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</tr>
<tr>
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<td>5.0</td>
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<td>30</td>
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</tr>
<tr>
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</tr>
<tr>
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</table>

Particle Image Velocimetry Wake Visualization

In addition to the wake visualization using the fluorescent paint method, a particle image velocimetry (PIV) technique was adapted for use in the MIT recirculating water tunnel to visualize the wake and measure and document the velocities within the wake for a number of quasi-steady and transient conditions. It is anticipated that characterization of the wake in this manner provides a means for better understanding the complex flows associated with an azimuthing propulsor, as well as providing an additional tool for validation of unsteady codes and flow modeling tools. While details of the visualization are provided by Stettler (2004), only an overview is provided here.

The general approach was very similar to that taken by DiFelice et al. (2001) for a stationary propeller in steady axial flow, and therefore generic discussion of PIV and application to investigation of propeller wake velocities will not be made here; the reader is referred to DiFelice et al. and other informative papers by Gui et al. (2001) and Westerweel (1997). The focus is on the presentation and interpretation of results of the flow visualization, and documentation of the velocities of several of the quasi-steady and dynamic wakes. Particular notice is made to the blade wake velocities, tip vortex spatial development and fluctuations, and overall velocity trends, and geometries associated with the quasi-steady and transient wakes.
Fig. 25 – Fluorescent paint visualization and graphical measurement of helical wake parameters. Top to bottom: 0°, 30°, 60°. RPM=300, \( V_a = 0.82 \) ft/s (\( J=0.2 \)).

Fig. 26 – Fluorescent paint visualization showing progression of helical wake vs. sway force for a fast ramp change in azimuth angle. Azimuth change from 0° to 60°, \( J=0.2 \).
For the quasi-steady azimuth conditions, the laser was synchronized to the rotation of the propeller, and a phase-averaging technique was utilized to calculate the quasi-steady velocities in the horizontal mid-plane of the wake. The coordinate system in all of the plots is centered on the main steering shaft of the propulsor (located 5 ⅜ inches (13.7 cm) forward of the aft face of the propeller hub). Phase-averaging in each case was made over 45 image sets, each synchronized to the same blade location.

One other important note should be made regarding the quasi-steady azimuth wake velocities from the PIV. It is clear from Fig. 28 that the magnitude of velocity (or induced velocity) is greater for the “upstream” or “outboard” side of the wake for the azimuthed case (for the horizontal wake cut). An explanation for this wake velocity asymmetry can be understood by considering that the vortex wake undergoes distortion under the oblique inflow. Experiments for helicopters in forward flight have confirmed vortex wake distortion effects, and progress has been made in modeling this distortion using free-vortex wake models (Leishman 2000; Bhagwat and Leishman 2000). The net result is that the “upstream” side of the vortex wake is stretched (and the “downstream” side is compressed), such that the “upstream” wake velocities are higher. For these relatively small azimuth angles, the effect is minimal, but noticeable. This distortion was also clearly demonstrated in the fluorescent paint visualization.

The previous section provided a fluorescent paint visualization of an unsteady azimuth rate in terms of a fast ramp change in azimuth angle. The result provided a visualization and rough correlation between the change in propulsor maneuvering forces (e.g. sway force $F_y$) and the progression of the helical wake during a very rapid propulsor azimuth maneuver, and provided confirmation of a lead in sway force vs. azimuth angle, consistent with a “dynamic inflow” model (Stettler 2004). The PIV system was also used to visualize a set of rapid propulsor azimuth maneuvers, in the form of a sinusoidal azimuth. Fig. 30 provides two of the resulting velocity maps, taken from image sets at a point very close to the zero degree cross-over (within 2°), for frequencies of 0.5 and 0.25 Hz. The velocity maps clearly show the geometric lag of the wake behind the azimuth angle. As expected, the higher frequency azimuth (faster azimuth rate) has a larger geometric lag. Fig. 24 provides the corresponding azimuth and sway force ($F_y$) records, with the PIV image set being taken at the zero crossing.
Fig. 28 – PIV. Quasi-steady phase-averaged perturbation (induced) velocity field magnitude, normalized by free-stream velocity $(V-V_a)/V_a$. $\delta=0^\circ$ (top), $\delta=20^\circ$ (bottom). RPM=700, $V_a=3.05\text{ft/s (J=0.32)}$.

Fig. 29 – PIV. Quasi-steady phase-averaged vorticity distribution, non-dimensionalized by free-stream velocity and propeller diameter $\omega D/V_a$. $\delta=0^\circ$ (top), $\delta=20^\circ$ (bottom). RPM=700, $V_a=3.05\text{ft/s (J=0.32)}$. 

Paper No. 2005-D11 Stettler 21
**CONCLUSIONS AND RECOMMENDATIONS**

This paper has provided highlights of results of a recent investigation completed at the Massachusetts Institute of Technology pertaining to the characterization of the steady and unsteady maneuvering force dynamics associated with an azimuthing podded propulsor. The paper has emphasized the more important aspects of the maneuvering force dynamics, including characterization of the steady vectored propulsor forces (thrust, torque, normal force, steering moment), as well as parametric modeling and identification of unsteady force dynamics as the propulsor is azimuthed for steering, or as the propeller is rapidly accelerated or decelerated. Finally, the visualization of the steady and unsteady azimuthing propulsor wake using two flow visualization techniques has been presented.

The quasi-steady propulsor force data provide valuable experimental data for the future validation of computational studies on podded propulsors. The introduced fluorescent paint flow visualization technique provides a unique means for visualizing the complex 3-dimensional helical wake of an azimuthing propulsor over a wide range of steady and unsteady operating conditions. In conjunction with the force measurement results, the visualization results also provide specific sets of wake trajectory data, including helix geometries (pitch/diameter ratios, wake angles, and wake distortions), which can be used in computational studies for wake modeling and alignment at small, moderate and large azimuth angles. The fluorescent paint visualizations have also revealed the formation of vortex rings during rapid increase in propeller rate. This provides an important rationale for the observed asymmetries in unsteady force results (i.e. dynamic inflow time constants) during rapid changes in propeller rate.

The fluorescent paint and PIV flow visualizations have also demonstrated an apparent invariance of the helical wake pitch/diameter ratio with propulsor azimuth angle, for given propeller rates and advance velocities. This apparent invariance is considered due to the relative cancellation between the decrease in effective inflow and the increase in induced velocity (i.e. loading) as the propulsor is azimuthed relative to the flow.

There are two obvious areas that are recommended for further study. First, the experimental results of force measurements should be utilized in full-vehicle simulations, which should be compared to free-running model tests of the autonomous surface test vehicle. The full set of nonlinear maneuvering coefficients for the bare hull has been identified from PMM tests (Stettler 2004). The combined dynamic maneuvering model could be used as the numerical basis for simulations.

The other area recommended for further study is in the formation and interaction of the vortex ring during rapid increase in propeller rate. A basic analytical model for the “formation time” of a vortex ring associated with a rapid increase in propeller rate has been developed (Stettler 2004). However, the complexity of the flows through a propeller disk make this difficult to confirm analytically for a propeller, based on existing experimental flow visualizations, because of the unknown velocities across the propeller disk and feeding the vortex ring. Follow-on research using a high-frequency PIV system to measure detailed velocity fields around the propeller and vortex ring may shed significant light in this area.
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REFERENCES


