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Offshore Crane Control Based on Adaptive External Models

Saverio Messineo and Andrea Serrani

Abstract— **A novel controller for cranes employed in heavy-lift offshore marine operations is proposed. The control objective is to reduce the hydrodynamic slamming load acting on a payload at water-entry of moonpool operations while, at the same time, the values of the wire tension must be kept within acceptable bounds. The adopted solution relies upon the use of an adaptive observer and two external models; as a result, the closed loop system is adaptive with respect to both plant parameters and frequencies of the harmonic disturbances entering the system. Experimental results show improvements with respect to a previous internal model-based controller.**

I. INTRODUCTION

In heavy-lift offshore marine operations, an important issue is how to safely install a payload on the seabed. This task can be accomplished by using an actively controlled crane placed on a vessel, and by lowering the payload trough a well in the ship hull referred to as a "moonpool". As documented in [1], the most critical phase occurs at water entry; indeed, as the payload is hit by the waves, the impulsive slamming load to which it is subject can seriously damage the payload, especially at harsh sea conditions. Moreover, during the launch of the payload through the moonpool it is important that the instantaneous value of the wire tension remains within certain bounds. In particular, the minimum value of the wire tension must be prevented from becoming negative to avoid high snatch loads that may break the wire, and its maximum value must not exceed a safety limit. In addition, it is desirable to reduce the variations of the wire tension so as to decrease the wire's wear and tear.

The problem under consideration has been previously addressed in [2] and [3] using standard tools from control of robotic systems. A different approach was pursued in [4], where a two-phase control strategy was proposed. In the first phase, which occurs when the payload is in the air and far enough from the moonpool, "heave compensation" is applied; the goal is to steer the wire tension to a constant value equal to the weight of the load. The second phase, referred to as "wave synchronization", starts when the payload is close to the moonpool. As shown in [5], the impulsive hydrodynamic slamming force that affects the payload at water-entry increases as the relative velocity between the waves and the payload increases; consequently, the control objective of this phase is to lower the payload through the water-entry zone keeping such relative velocity constant and equal to a prescribed value. For each of the two phases, [4] proposed a feedforward compensator to achieve the control objectives. Inspired by [4], in [6] a two-phases feedback compensator based on the internal model principle [7] was proposed. In each of the two phases, the control objective was cast as that of letting the output variable track a reference signal while rejecting the wave-induced disturbances.

Saverio Messineo is with CeSOS, NTNU, Otto Nielsens veg 10, NO-7491 Trondheim, Norway. Andrea Serrani is with the Department of Electrical and Computer Engineering, The Ohio State University, Columbus, OH, USA. Corresponding author: S. Messineo, Email: saverio@marin.ntnu.no

The aim of the present paper is to introduce a novel robust control strategy to relax the restrictive assumptions which the previous work [6] relied upon; in particular, the proposed control strategy is made adaptive with respect to both the plant parameters and the frequencies of the harmonics of the wave-induced disturbances. Moreover, only an upper bound on the number of harmonics is assumed to be known. One of the challenges involved in the present control design is that part of the state variables and part of the wave-induced harmonic disturbances are not measurable. As a consequence, a certainty-equivalence approach is proposed by designing an adaptive observer and two adaptive external models of the wave-induced disturbance (see [8]) in order to reconstruct the needed quantities. A remarkable feature of the proposed external models is their capability of yielding converging estimates even in the case of over-parametrization, that is, when the number of harmonics contained in the external model exceeds the number of harmonics of the waves in the basin. The certainty-equivalence approach proceeds by designing the controllers for the heave-compensation and the wave synchronization phases using the obtained estimates in place of the non measurable quantities. The two distinct controllers are designed in an adaptive fashion, as opposed to the work in [6]. The effectiveness of the proposed design is shown both theoretically and experimentally. The experiments are performed on a scale-model of a crane vessel with moonpool.

II. SCALE-MODEL AND MATHEMATICAL MODELING

In this section, first a brief description of the crane vessel scale-model is given; then, a mathematical model is derived. The mathematical model will be used for control design.

A. Experimental Setup

The scale-model consists of a servo motor, with an internal speed-control loop, and a spherical payload connected to the motor by a wire that goes over a pulley suspended by a spring. The scale-model is equipped with vertical accelerometers in both the payload and the vessel, and with a wire tension sensor. Attached to the vessel there is a wave meter that measures the water level in the moonpool. The motor position is measured by an encoder. A wave generator is used to produce waves.

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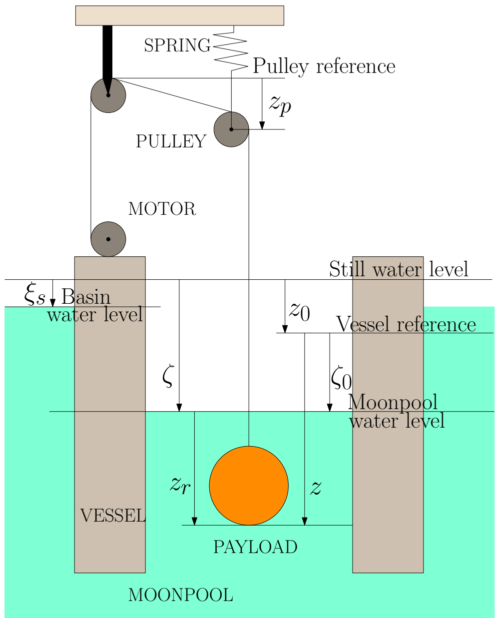

Fig. 1. Sketch of the scale-model and definition of coordinates

B. Dynamics of the Scale-Model Crane Vessel

In Fig. 1, a sketch of the experimental setup is shown along with a definition of references and of the coordinates z, z_0, ζ, ζ_0, z_p, z_r and ξ_s. Note that the "still water level" is an earth-fixed frame, whereas the "vessel reference" is a body-fixed frame. Let m denote the payload mass, g the gravity acceleration, F_t the wire tension, and f_z the hydrodynamic force on the payload in the moonpool; then, the motion of the payload with respect to the still water level reference frame is described by

$$m(\ddot{z} + \ddot{z}_0) = mg + f_z - F_t. \tag{1}$$

By defining $z_m \doteq z - z_p$, the equation of the motion of the pulley with respect to the pulley reference frame can be approximated as

$$k_p(z - z_m) = F_t - m_p\ddot{z}_0. \tag{2}$$

In [3], the same approximation was used except for the fact that the term accounting for $m_p\ddot{z}_0$ was neglected. Denote with $\dot{z}_d$ the reference speed of the servo motor which is also the control input; for the frequencies under consideration, it is convenient for control design to neglect the fast motor dynamics and employ the following expression

$$\frac{z_m}{\dot{z}_d} \cong \frac{1}{s}. \tag{3}$$

As a result, the control input for the system is given by $\dot{z}_d$, while the measurable outputs are $\ddot{z}_0$, $\ddot{z}_0 + \ddot{z}$, F_t, ζ_0 and z_m.

C. Hydrodynamic Forces in the Moonpool

When the payload is in the moonpool, f_z can be modeled as

$$\begin{aligned} f_z = & -\rho g \nabla(z_r, d) - \rho\nabla(z_r, d)\ddot{z}_r - Z_{\ddot{z}_r}(z_r)\ddot{z}_r \\ & - \frac{\partial Z_{\ddot{z}_r}}{\partial z_r}(z_r)\dot{z}_r^2 - \frac{1}{2}\rho C_D A_{pz}(z_r)\dot{z}_r|\dot{z}_r| - d_l(z_r)\dot{z}_r \end{aligned} \tag{4}$$

(see [3, p. 600]). In (4), $\rho = 1000$ kg m^{-3} is the density of water; z_r represents the payload position with respect to the "moonpool water level"; $\nabla(z_r, d)$ is equal to the volume of the submerged part of the payload, and d is the diameter of the payload. The remaining quantities on the right hand side of (4) are described in detail in section V-B.

D. Wave-induced Disturbances

Following [6], the heave motion z_0 of the crane vessel with respect to the still water level and the wave elevation ζ_0 inside the moonpool with respect to the vessel reference, both induced by the motion ξ_s of the waves in the basin, can be given the following representation

$$z_0 = \sum_1^p A_i \sin(\omega_i t + \varphi_i), \quad \zeta_0 = \sum_1^p B_i \sin(\omega_i t + \alpha_i). \tag{5}$$

In this work, the actual number of harmonics p and the values of the amplitudes, phases, and frequencies of z_0 and ζ_0 is assumed to be unknown. However, knowledge of an upper bound on p is required for controller design.

III. Adaptive External Models

In this section, it is shown how to recover converging estimates of the harmonic disturbances entering the system. The external model estimating $\dot{z}_0$ and $z_0^{(3)}$ by processing the measurement of $\ddot{z}_0$ is presented in details, as the reconstruction of $\dot{z}_0$ requires a peculiar hybrid implementation. The external model estimating $\dot{\zeta}_0$, $\ddot{\zeta}_0$ and $\zeta_0^{(3)}$ by exploiting measurements of ζ_0 is introduced at the end of Section III-A.

A. Continuous time external models

Consider the measured signal $\ddot{z}_0$ together with its integral $\dot{z}_0$ and its derivative $z_0^{(3)}$ (not available for feedback); choose $F_p \in \mathbb{R}^{2p\times 2p}$ and $G_p \in \mathbb{R}^{2p\times 1}$ so that (F_p, G_p) is a controllable pair and F_p is Hurwitz. From equation (5), it is well known (see [9] and [10]) that there exists a vector $\Psi_\sigma \in \mathbb{R}^{1\times 2p}$ so that $\ddot{z}_0$, $\dot{z}_0$ and $z_0^{(3)}$ can be given the following representation

$$\begin{aligned} \dot{w}_p &= (F_p + G_p\Psi_\sigma)w_p \\ \ddot{z}_0 &= \Psi_\sigma w_p \\ \dot{z}_0 &= \Psi_\sigma(F_p + G_p\Psi_\sigma)^{-1}w_p \\ z_0^{(3)} &= \Psi_\sigma(F_p + G_p\Psi_\sigma)w_p. \end{aligned} \tag{6}$$

Assumption 1 The unknown vector Ψ_σ ranges within a known compact set $S \subset \mathbb{R}^{1\times 2p}$.

To compute estimate $\hat{\dot{z}}_0$ and $\hat{z}_0^{(3)}$ of $\dot{z}_0$ and $z_0^{(3)}$, respectively, the following adaptive external model is proposed

$$\begin{aligned}
\dot{\xi} &= (F + G\hat{\Psi})\xi - Gk_a y \\
\dot{y} &= -k_a y - \Psi_\sigma w_p + \hat{\Psi}\xi \\
\dot{\hat{\Psi}}^T &= -\gamma_a y \xi \\
\hat{\ddot{z}}_0 &= \hat{\Psi}\xi
\end{aligned} \qquad (7)$$

which has the purpose of reconstructing the available signal $\ddot{z}_0$. While the observation of the state $\ddot{z}_0$ is obviously irrelevant per se, it provides a way of reconstructing its derivative and its first integral, as will be seen in a moment. In (7), k_a and γ_a are scalar gains, $F \in \mathbb{R}^{2q \times 2q}$, $G \in \mathbb{R}^{2q \times 1}$ so that (F, G) is a controllable pair, F is Hurwitz, $q \geq p$ is an upper bound on p, $\xi \in \mathbb{R}^{2q \times 1}$, and $y \in \mathbb{R}$.

The following theorem establishes that, in the process of reconstructing $\ddot{z}_0$, the external model is capable to recover all the information that is needed to compute the required estimates:

Theorem 1: There exist a matrix $\bar{M} \in \mathbb{R}^{2q \times 2p}$ and a number $k_a^* > 0$ such that, for any $k_a > k_a^*$ and any $\gamma_a > 0$ all the trajectories of the system (6), (7) are bounded and satisfy $\lim_{t \to \infty} y(t) = 0$, $\lim_{t \to \infty} \|\xi(t) - \bar{M} w_p(t)\| = 0$, $\lim_{t \to \infty} \|\hat{\ddot{z}}_0(t) - \ddot{z}_0(t)\| = 0$, $\lim_{t \to \infty} \hat{\Psi}(t) = \hat{\Psi}_\infty$, where $\hat{\Psi}_\infty$ is a constant value. Moreover the following identities hold:

$$\hat{\Psi}_\infty \bar{M} = \Psi_\sigma \qquad (8)$$

$$(F + G\hat{\Psi}_\infty)\bar{M} = \bar{M}(F_p + G_p \Psi_\sigma)\,. \qquad (9)$$

Proof: See [8]. ∎

The previous result is instrumental in order to prove that, under a particular assumption that will be removed in section III-B, an estimate $\hat{\dot{z}}_0$ of $\dot{z}_0$ can be extracted from the external model. This is shown in the following theorem:

Theorem 2: Assume that the solution $\hat{\Psi}(t)$ of (7) is such that $F + G\hat{\Psi}(t)$ is nonsingular for all $t \geq 0$. Then, the additional outputs of the external model (7) defined as

$$\begin{aligned}
\hat{\dot{z}}_0 &= \hat{\Psi}(F + G\hat{\Psi})^{-1}\xi \\
\hat{z}_0^{(3)} &= \hat{\Psi}(F + G\hat{\Psi})\xi
\end{aligned}$$

provide a converging estimate of $\dot{z}_0(t)$ and $z_0^{(3)}(t)$, respectively.

Proof: Due to lack of space, the proof of the theorem is omitted. ∎

Moreover, it can be easily shown that the following external model is capable of providing converging estimates of the remaining unknown state variables $\dot{\zeta}_0$, $\ddot{\zeta}_0$, and $\zeta_0^{(3)}$:

$$\begin{aligned}
\dot{\eta} &= (F + G\hat{\Theta})\eta - Gk_b r \\
\dot{r} &= -k_b r - \zeta_0 + \hat{\Theta}\eta \\
\dot{\hat{\Theta}}^T &= -\gamma_b r \eta \\
\hat{\zeta}_0 &= \hat{\Theta}\eta \\
\hat{\dot{\zeta}}_0 &= \hat{\Theta}(F + G\hat{\Theta})\eta \\
\hat{\ddot{\zeta}}_0 &= \hat{\Theta}(F + G\hat{\Theta})^2\eta \\
\hat{\zeta}_0^{(3)} &= \hat{\Theta}(F + G\hat{\Theta})^3\eta
\end{aligned} \qquad (10)$$

where F and G are the same matrices as in (7), k_b and γ_b are scalar design parameters, $\eta \in \mathbb{R}^{2q \times 1}$, $r \in \mathbb{R}$, ζ_0 is the available measurement. The signals ζ_0, $\dot{\zeta}_0$, $\ddot{\zeta}_0$ and $\zeta_0^{(3)}$ can be given an analogous representation as in (6), that is, they can be regarded as the output of an exosystem in canonical parametrization.

B. Discrete-time update law

In this section, it is shown how to recover a converging estimate of $\dot{z}_0$ relaxing the restrictive hypothesis about the invertibility of the matrix $F + G\hat{\Psi}(t)$ formulated in Theorem 2. Following [8], let $\{t_k\}_{k=0}^{\infty}$ denote a monotonically increasing sequence of equally spaced sampling times, with $t_0 = 0$ and $\lim_{k \to \infty} t_k = \infty$. The following hybrid external model is proposed in place of (7)

$$\begin{aligned}
\dot{\xi} &= (F + G\hat{\Psi})\xi - Gk_a y \\
\dot{y} &= -k_a y - \Psi_\sigma w_p + \hat{\Psi}\xi \\
\dot{\hat{\Psi}}^T &= -\gamma_a y \xi \\
\hat{\ddot{z}}_0 &= \hat{\Psi}\xi \\
\hat{\Gamma}(k+1) &= \mu(\hat{\Psi}(t_k)) \\
\hat{\dot{z}}_0 &= \hat{\Gamma}(k)\xi \\
\hat{z}_0^{(3)} &= \hat{\Psi}(F + G\hat{\Psi})\xi.
\end{aligned} \qquad (11)$$

In (11), $\mu(\cdot)$ denotes a discrete-time algorithm defined below, while the value of $\hat{\Gamma}(k)$ is constant during each interval $[t_k, t_{k+1})$ and commutes at $t = t_{k+1}$. As a result, the structure of the proposed external model is that of a continuous-time system, undergoing switching at each sampling time t_k. Denote with $V_g(k)$ the modal subspace of $F + G\hat{\Psi}(t_k)$ corresponding to the eigenvalues different from zero, and decompose $\mathbb{R}^{2q}$ in the direct sum $V_g(k) \oplus V_b(k)$, where $V_b(k)$ is the modal subspace of $F + G\hat{\Psi}(t_k)$ associated with the remaining zero eigenvalues. Then, the following algorithm defines the discrete-time update law $\mu(\cdot)$.

Algorithm

At each time $t = t_k$

- if the condition number of the matrix $F + G\hat{\Psi}(t_k)$ is smaller than a prescribed tolerance (i.e., if that matrix is invertible), set $\hat{\Gamma}(k+1) = \hat{\Psi}(t_k)(F + G\hat{\Psi}(t_k))^{-1}$.

Otherwise

- compute a basis for $\mathbb{R}^{2q}$ adapted to the subspaces $V_g(k)$ and $V_n(k)$ of $F + G\hat{\Psi}(t_k)$
- set $\hat{\Gamma}_g(k+1) = \hat{\Psi}_g(t_k)(F + G\hat{\Psi}(t_k))_g^{-1}$, where $\hat{\Psi}_g(t_k)$ and $(F + G\hat{\Psi}(t_k))_g$ are respectively the projection of $\hat{\Psi}(t_k)$ onto $V_g(k)$ and the restriction of $F + G\hat{\Psi}(t_k)$ to $V_g(k)$.
- set $\hat{\Gamma}(k+1) = (\hat{\Gamma}_g(k+1) \quad 0)$ and revert back to the original coordinates.

The algorithm is initialized at $\hat{\Gamma}(0) = \hat{\Psi}(0)(F + G\hat{\Psi}(0))^{-1}$, where $\hat{\Psi}(0)$ is chosen so that $(F + G\hat{\Psi}(0))$ is invertible.

IV. Adaptive observer

In order to apply a certainty-equivalence approach to controller design, a way to reconstruct the values of z and $\dot{z}$ must also be devised, as the latter are not measurable. For this purpose, we resort to an adaptive observer based on equation (2) and on the measurements of the quantities z_m, $\ddot{z}$, $\ddot{z}_0$, and F_t. Rearrange equation (2) as

$$z_m = z - \frac{1}{k_p} F_t + \frac{m_p}{k_p} \ddot{z}_0 \tag{12}$$

where F_t and $\ddot{z}_0$ are bounded signals, k_p and m_p are unknown constants and the variables $z(t)$, $\dot{z}(t)$, $\ddot{z}(t)$ and $z_m(t)$ are assumed to be defined for any $t \geq 0$. Define x_1 as the output of a stable, first order filter, whose input is z_m, that is

$$\dot{x}_1 = -\lambda x_1 + z_m \tag{13}$$

with $\lambda > 0$. Moreover, define $x_2 \doteq z$, $x_3 \doteq \dot{z}$, $x \doteq \begin{pmatrix} x_1 & x_2 & x_3 \end{pmatrix}^T$, $\theta_1 \doteq \frac{1}{k_p}$, $\theta_2 \doteq \frac{m_p}{k_p}$, $\theta \doteq \begin{pmatrix} \theta_1 & \theta_2 \end{pmatrix}^T$, $\phi(t) \doteq \begin{pmatrix} -F_t(t) & \ddot{z}_0(t) \end{pmatrix}^T$. Using equations (12) and (13), the dynamics of the system to be observed can be written as follows

$$\begin{aligned} \dot{x} &= A_b x + \bar{b}_3 \phi^T(t)\theta + \bar{b}_1 \bar{y} + \bar{b}_2 \ddot{z} \\ \bar{y} &= C_b x \end{aligned} \tag{14}$$

where (A_b, C_b), with $A_b \in \mathbb{R}^{3\times3}$ and $C_b \in \mathbb{R}^{1\times3}$, are in Brunowsky's canonical form, $\bar{b}_1 = \begin{pmatrix} -\lambda & 0 & 0 \end{pmatrix}^T$, $\bar{b}_2 = \begin{pmatrix} 0 & 0 & 1 \end{pmatrix}^T$, $\bar{b}_3 = \begin{pmatrix} 1 & 0 & 0 \end{pmatrix}^T$; the quantities $\bar{b}_1\bar{y}$ and $\bar{b}_2\ddot{z}$ are known and can be easily taken into account in the observer design. As the vector $\bar{b}_3$ is not Hurwitz, a filtered transformation of order two is necessary in order to design the adaptive observer for the system (14). To this purpose, following [11], a time-varying change of coordinates is performed as $\bar{p} = x - M(t)\theta$, yielding

$$\begin{aligned} \dot{\bar{p}} &= A_b\bar{p} + [A_b M(t) + \bar{b}_3\phi^T(t) - \dot{M}(t)]\theta + \bar{b}_1\bar{y} + \bar{b}_2\ddot{z} \\ \bar{y} &= C_b \bar{p}. \end{aligned} \tag{15}$$

Choose the vector $\bar{d} = \begin{pmatrix} 1 & \bar{d}_1 & \bar{d}_0 \end{pmatrix}$ such that the polynomial $s^2 + \bar{d}_1 s + \bar{d}_0$ has all the roots in $\mathbb{C}^-$; then, it is well known that by partitioning $M(t) \in \mathbb{R}^{3x2}$ as $M(t) = \begin{pmatrix} 0 \\ N(t) \end{pmatrix}$, with $N(t) \in \mathbb{R}^{2x2}$, it must exist $\beta(t)$ satisfying $\beta(t)^T = C_b A_b M(t) + C_b \bar{b}_3 \phi^T(t)$ so that system (15) can be rewritten as

$$\begin{aligned} &\dot{\bar{p}} = A_b\bar{p} + \bar{d}\beta(t)^T\theta + \bar{b}_1\bar{y} + \bar{b}_2\ddot{z} \\ &\bar{y} = C_b\bar{p}. \end{aligned} \tag{16}$$

The adaptive observer for system (14) is then given by

$$\begin{aligned} &\dot{N} = A_d N + B_d \bar{b}_3 \phi^T(t) \\ &\dot{\hat{\bar{p}}} = A_b\hat{\bar{p}} + \bar{d}\beta(t)^T\hat{\theta} + \bar{k}_0(\bar{y} - C_b\hat{\bar{p}}) + \bar{b}_1\bar{y} + \bar{b}_2\ddot{z} \\ &\dot{\hat{\theta}} = \bar{\gamma}\beta(t)(\bar{y} - C_b\hat{\bar{p}}) \\ &\hat{x} = \hat{\bar{p}} + \begin{pmatrix} 0 \\ N(t) \end{pmatrix}\hat{\theta} \end{aligned} \tag{17}$$

where $\bar{\gamma} > 0$ and

$$A_d = \begin{pmatrix} -\bar{d}_1 & 1 \\ -\bar{d}_0 & 0 \end{pmatrix}, \; B_d = \begin{pmatrix} -\bar{d}_1 & 1 & 0 \\ -\bar{d}_0 & 0 & 1 \end{pmatrix}.$$

Choose $\bar{k}_0 = \begin{pmatrix} \bar{d}_1 + \bar{\lambda} & \bar{d}_0 + \bar{\lambda}\bar{d}_1 & \bar{\lambda}\bar{d}_0 \end{pmatrix}^T$, with $\bar{\lambda} > 0$. Since it is assumed that $\ddot{z}_0(t)$ is at least sufficiently rich of order two, from the physics of the problem it makes sense to conjecture[1] that the signal $\phi(t)$ is capable to deliver enough information to ensure convergence of $\hat{\theta}(t)$ to θ. As a consequence, following [11] it is possible to conclude that the adaptive observer (17) yields an asymptotic estimate of the state x and, as a byproduct, of the vector θ. The estimates $\hat{x}$ and $\hat{\theta}$ will be both used for control design.

V. Control Design

Similarly to [4] and [6], the control strategy proposed in this paper consists of two distinct phases (heave compensation and wave synchronization) and a transition between them.

A. Heave compensation

Set the regulation error as the difference between the payload velocity with respect to the inertial frame and the desired velocity c, that is as $e_h \doteq \dot{z} + \dot{z}_0 - c$, define $b \doteq \frac{k_p}{m}$ and $\bar{g} \doteq \frac{m}{k_p} g$. From equations (1), (2) and (3), since $f_z = 0$ in the air, the error system can be written as

$$\begin{aligned} &\dot{e}_h = b\left[z_m - z(t) + \bar{g} - \theta_2\ddot{z}_0(t)\right] \\ &\dot{z}_m = u_h \end{aligned} \tag{18}$$

where u_h is the control input. It is assumed that the constants $\bar{g}$ and θ_2 are unknown and that $b \in [b_0, b_1]$ with $b_0 > 0$. From sections III and IV it is seen that an estimate of e_h can be computed as $\hat{e}_h = \hat{\dot{z}}(t) + \hat{\dot{z}}_0(t) - c$. From now on, in order to ease the notation, the dependence of a given quantity by the time will be omitted. Choose $k_1 > 0$ and define $\tilde{z}_m = z_m - (\hat{z} - \hat{\bar{g}} + \hat{\theta}_2\ddot{z}_0)$, where $\hat{\bar{g}}$ is still to be determined; as a result, the error system in the new coordinates reads as

$$\begin{aligned} &\dot{e}_h = b[\tilde{z}_m + \tilde{\bar{g}} - \tilde{z} - \tilde{\theta}_2\ddot{z}_0] \\ &\dot{\tilde{z}}_m = u_h - \dot{\hat{z}} + \dot{\hat{\bar{g}}} - \dot{\hat{\theta}}_2\ddot{z}_0 - \hat{\theta}_2 z_0^{(3)} \end{aligned} \tag{19}$$

where $\tilde{z} = z - \hat{z}$, $\tilde{\theta}_2 = \theta_2 - \hat{\theta}_2$ and $\tilde{\bar{g}} = \bar{g} - \hat{\bar{g}}$. Then the following result holds.

Theorem 3: Set $\gamma_h, k_2 > 0$; the following control law

$$\begin{aligned} &\dot{\hat{\bar{g}}} = \gamma_h \hat{e}_h \\ &u_h = -k_1\hat{e}_h - k_2\tilde{z}_m + \dot{\hat{z}} - \dot{\hat{\bar{g}}} + \dot{\hat{\theta}}_2\ddot{z}_0 + \hat{\theta}_2\hat{z}_0^{(3)} \end{aligned} \tag{20}$$

is such that the regulation error e_h converges to zero and all the trajectories are bounded.

Proof: The proof is omitted for lack of space. ■

B. Wave synchronization

Set the regulation error as $e_w \doteq \dot{z} - \dot{\zeta}_0 - c$. From equations (1), (2), (3), this yields the following error system

$$\begin{aligned} &\dot{\hat{e}}_w = b[\bar{g} - (m\theta_1 + \theta_2)\ddot{z}_0(t) - m\theta_1\ddot{\zeta}_0(t) + \tfrac{1}{k_p} f_z \\ &\qquad + z_m - z(t) - \tfrac{1}{b}\dot{\tilde{e}}_w] \\ &\dot{z}_m = u_w \end{aligned} \tag{21}$$

where, for convenience, system (21) is written in observer coordinates and the observation error $\tilde{e}_w \doteq e_w - \hat{e}_w$ is

[1] Simulations and experimental results seem to validate the conjecture.

regarded as a converging perturbation; u_w denotes the control input. The mass m of the payload can be *measured* by means of equation (1) whenever the payload is in the air (e.g. before starting the lowering operations), as a result, the value of m is available for control design. It is assumed that θ_1 is unknown and that $k_p \in [k_{p_0}, k_{p_1}]$ with $k_{p_0} > 0$. Notice that, at water entry, if the part of the wave synchronization task that takes place in the air has been pursued correctly, then the hydrodynamic force (4) is mainly due to the buoyancy, i.e. $f_z \cong -\rho g \nabla(z_r, d)$. In particular, since $\dot{z}_r = e_w + c$, it is seen that the effects of the force $-\rho\nabla(z_r, d)\ddot{z}_r = -\rho\nabla(z_r, d)\dot{e}_w$ and of the added mass $-Z_{\ddot{z}_r}(z_r)\ddot{z}_r = -Z_{\ddot{z}_r}(z_r)\dot{e}_w$ are extremely small and therefore can be neglected. The term accounting for the slamming force can be written as $-\frac{\partial Z_{\ddot{z}_r}}{\partial z_r}(z_r)\dot{z}_r^2 = -\frac{\partial Z_{\ddot{z}_r}}{\partial z_r}(z_r)(e_w+c)^2$, where the function of the depth $-\frac{\partial Z_{\ddot{z}_r}}{\partial z_r}(z_r)$, is bounded, Lipschitz, non-negative, and vanishes when the payload is in the air ($z_r < 0$) or when it is completely submerged ($z_r > d$). The nonlinear viscous drag force term reads as $-\frac{1}{2}\rho C_D A_{pz}(z_r)\dot{z}_r|\dot{z}_r| = -\frac{1}{2}\rho C_D A_{pz}(z_r) \times \mathrm{sgn}(e_w + c)(e_w + c)^2$, where the term $-\frac{1}{2}\rho C_D A_{pz}(z_r)$ is bounded, Lipschitz, nonnegative, vanishes when the payload is in the air, and is equal to a constant, say c_1, when it is completely submerged. As a result, it is convenient to define $f_1(z_r, e_w) = -\frac{\partial Z_{\ddot{z}_r}}{\partial z_r}(z_r) - \frac{1}{2}\rho C_D A_{pz}\mathrm{sgn}(e_w + c) - c_1$ and rewrite the sum of the slamming load and the nonlinear viscous drag force as

$$- \frac{\partial Z_{\ddot{z}_r}}{\partial z_r}(z_r)(e_w + c)^2 - \frac{1}{2}\rho C_D A_{pz}(z_r)\mathrm{sgn}(e_w + c) \times (e_w + c)^2 = [f_1(z_r, e_w) + c_1](e_w + c)^2 \quad (22)$$

where the function $f_1(z_r, e_w)$ is bounded and Lipschitz. A sufficient condition for $f_1(z_r, e_w)$ to be equal to zero is when $z_r > d$ and $|e_w| < c$. The linear drag force term reads as $-d_l(z_r)\dot{z}_r = -d_l(z_r)(e_w + c)$, where the function $-d_l(z_r)$ is bounded, Lipschitz, never positive, equal to zero in the air and equal to a constant, say c_2, when $z_r > d$. As a result, we define $f_2(z_r) \doteq -d_l(z_r) - c_2$ and rewrite

$$-d_l(z_r)\dot{z}_r = [f_2(z_r) + c_2](e_w + c) \quad (23)$$

where the function $f_2(z_r)$ is bounded, Lipschitz, and is equal to zero when the payload is completely submerged. The contribution due to the buoyancy is pre-compensated by including in the control law the term $\frac{1}{k_0}\rho g\nabla(\hat{z}_r, d_0)$, where k_0 is chosen so that $k_0 \in [k_{p_0}, k_{p_1}]$, d_0 denotes the nominal value of the payload's diameter d, and $\hat{z}_r = \hat{z} - \hat{\zeta}_0$ is an estimate of z_r. Notice that by definition, the quantity $\frac{1}{k_0}\rho g\nabla(\hat{z}_r, d_0) - \frac{1}{k_p}\rho g\nabla(z_r, d)$ is bounded, Lipschitz and, as the payload reaches a certain depth, that is when $z_r > d$ and $\hat{z}_r > d_0$, is equal to a constant, denoted here by c_3; hence, by defining $f_3(z_r, \hat{z}_r) = \frac{1}{k_0}\rho g\nabla(\hat{z}_r, d_0) - \frac{1}{k_p}\rho g\nabla(z_r, d) - c_3$, it is convenient to write

$$-\frac{1}{k_p}\rho g\nabla(z_r, d) = c_3 + f_3(z_r, \hat{z}_r) - \frac{1}{k_0}\rho g\nabla(\hat{z}_r, d_0) \quad (24)$$

where $f_3(z_r, \hat{z}_r)$ is bounded, Lipschitz, and is equal to zero when $z_r > d$ and $\hat{z}_r > d_0$. Using results from sections III and IV, an estimate of $e_w(t)$ can be computed as $\hat{e}_w(t) = \hat{\dot{z}}(t) - \hat{\dot{\zeta}}_0(t) - c$. From now on, the dependence of a given quantity by its own arguments will be omitted. Group the constants as $c_4 \doteq -[c_3 + \frac{1}{k_p}(c_1c^2 + c_2c) + \bar{g}]$, define $\mu_1 \doteq \frac{2}{k_p}[f_1 + c_1]\tilde{e}_w + \frac{1}{k_p}[(f_1 + c_1)2c + f_2 + c_2]$, $\mu_2 \doteq \frac{1}{k_p}(f_1 + c_1)$ and

$$\mu_3 \doteq \frac{1}{k_p}(f_1 + c_1)\tilde{e}_w^2 + \frac{1}{k_p}[(f_1 + c_1)2c + f_2 + c_2]\tilde{e}_w - \frac{1}{b}\dot{\tilde{e}}_w + f_3 + \frac{1}{k_p}(f_1c^2 + f_2c),$$

choose k_3 and $\lambda_w > 0$, and change variable as $\tilde{z}_m = z_m - [(m\hat{\theta}_1 + \hat{\theta}_2)\ddot{z}_0 + m\hat{\theta}_1\hat{\ddot{\zeta}}_0 + \hat{z} + \lambda_w\xi_w - k_3\hat{e}_w + \frac{1}{k_0}\rho g\nabla(\hat{z}_r, d_0)]$, where ξ_w is still to be determined; as a result, from equations (22), (23) and (24), after easy but tedious manipulations, the error system (21) in the new coordinates reads as

$$\begin{aligned}
\dot{\hat{e}}_w &= b[\tilde{z}_m - (m\tilde{\theta}_1 + \tilde{\theta}_2)\ddot{z}_0 - m\tilde{\theta}_1\ddot{\zeta}_0 - m\hat{\theta}_1\tilde{\ddot{\zeta}}_0 - \tilde{z} - c_4 \\
&\quad +\lambda_w\xi_w - k_3\hat{e}_w + \mu_1(z_r, e_w, \tilde{e}_w)\hat{e}_w + \mu_2(z_r, e_w)\hat{e}_w^2 \\
&\quad +\mu_3(z_r, e_w, \tilde{e}_w)] \\
\dot{\tilde{z}}_m &= u_w - (m\dot{\hat{\theta}}_1 + \dot{\hat{\theta}}_2)\ddot{z}_0 - (m\hat{\theta}_1 + \hat{\theta}_2)z_0^{(3)} - m\dot{\hat{\theta}}_1\hat{\ddot{\zeta}}_0 \\
&\quad -m\hat{\theta}_1\dot{\hat{\ddot{\zeta}}}_0 - \dot{\hat{z}} - \lambda_w\dot{\xi}_w + k_3\dot{\hat{e}}_w - \tfrac{d}{dt}\tfrac{1}{k_0}\rho g\nabla(\hat{z}_r, d_0)
\end{aligned} \quad (25)$$

where $\tilde{\theta}_1 = \theta_1 - \hat{\theta}_1$, $\tilde{\theta}_2 = \theta_2 - \hat{\theta}_2$, $\tilde{\ddot{\zeta}}_0 = \ddot{\zeta}_0 - \hat{\ddot{\zeta}}_0$, $\tilde{z} = z - \hat{z}$ and $\tilde{e}_w = e_w - \hat{e}_w$. Then, the following result holds.

Theorem 4: There exist numbers $k_3^* > 0$, $k_4^* > 0$ and $\lambda_w^* > 0$ such that, if $k_3 > k_3^*$, $k_4 > k_4^*$ and $\lambda_w > \lambda_w^*$ then the dynamic controller

$$\begin{aligned}
\dot{\xi}_w &= -\lambda_w\xi_w + v \\
u_w &= (m\dot{\hat{\theta}}_1 + \dot{\hat{\theta}}_2)\ddot{z}_0 + (m\hat{\theta}_1 + \hat{\theta}_2)\hat{z}_0^{(3)} + m\dot{\hat{\theta}}_1\hat{\ddot{\zeta}}_0 \\
&\quad +m\hat{\theta}_1\dot{\hat{\ddot{\zeta}}}_0 + \dot{\hat{z}} + \lambda_w\dot{\xi}_w - k_3\dot{\hat{e}}_w + \tfrac{d}{dt}\tfrac{1}{k_0}\rho g\nabla(\hat{z}_r, d_0) \\
&\quad -k_4\tilde{z}_m - k_5\hat{e}_w
\end{aligned} \quad (26)$$

with $v = -(k_3 + \lambda_w)\hat{e}_w + \lambda_w\xi_w$ and $k_5 = 1$, is such that all the trajectories of the closed loop system (25)-(26) are bounded and the regulation error e_w converges to zero.

Proof: The proof is omitted for lack of space. ■

C. Transition from Heave Compensation to Wave Synchronization

When the payload approaches the moonpool, the heave compensating feedback control u_h needs to turn into the wave synchronizing control u_w. The transition is achieved through the blending factor α whose dependence on $\hat{z}$ is as follows

$$\alpha(\hat{z}) = \begin{cases} 0 & \text{if } \hat{z} < h_1 \\ \dfrac{1}{h_2 - h_1}(\hat{z} - h_1) & \text{if } h_1 \leq \hat{z} \leq h_2 \\ 1 & \text{if } \hat{z} > h_2 \end{cases} \quad (27)$$

where $h_1 = -0.20$ m and $h_2 = -0.15$ m are selected so that the transition ends before the payload hits the waves. Blending u_h and u_w gives the following final control law: $u = \alpha(\hat{z})u_w + (1 - \alpha(\hat{z}))u_h$, where $u \doteq \dot{z}_d$ and $\dot{z}_d$ denotes the speed commanded to the servo motor.

VI. Experimental Results

The controller proposed in this paper and the one in [6] were compared experimentally. For each of the two controllers, fifteen tests were carried out at the MClab at NTNU, by generating waves in the basin; the waves are characterized by a JONSWAP spectrum with significant wave height $H_s = 0.02$ m and peak frequency $\omega_s = 4.8$ rad/s; as the latter matches approximately the moonpool and vessel natural frequencies, a *resonant* behavior is induced in the motion of both the vessel and the water level in the moonpool. The scalar gains of the external models were chosen as $k_a = k_b = 10$, $\gamma_a = \gamma_b = 100$; the matrix F was selected so that spec(F)=$\{-10, -10, -10, -10, -10, -10\}$. The parameters of the adaptive observer were selected as $\lambda = \bar{\lambda} = 5$, $\bar{\gamma} = 50$, $\bar{d}_0 = 1$, $\bar{d}_1 = 1$. The gains of the heave-compensating and wave-synchronizing controllers were chosen as $k_1 = 1.5$, $k_2 = 30$, $\gamma_h = 2$, $k_3 = 0.01$, $k_4 = 10$, $\lambda_w = 2$. The initial conditions for the external models and the adaptive observer were selected at the origin; in this way, no a priori knowledge on the waves' frequencies and on the plant parameters is exploited. The values of the frequencies of the harmonics embedded in the internal model control in [6] were chosen to be in the range of the moonpool and vessel resonant frequencies, namely, they were set as: $\omega_1 = 4$ rad/s, $\omega_2 = 4.5$ rad/s and $\omega_3 = 5$ rad/s. The desired velocity c in sections V-A and V-B and the one in [6] was chosen as 0.02 m/s. The averaged results over the fifteen experimental runs are summarized in Table I, where "Int" denotes the internal model-based controller presented in [6], "Ext" denotes the controller proposed in this paper, and "Imp" denotes the improvement achieved by the latter with respect to the former. For the control strategy proposed in this paper, the time history of the regulation error in both phases (Fig. 2) is plotted.

As it can be seen from Table I, the controller proposed in this paper leads to a 13.54% reduction of the maximum of the absolute value of the hydrodynamic force, which in turn implies a reduction of the probability that the payload could suffer damages at water entry. Such an improvement is consistent with the reduction from 0.20 N to 0.11 N of the standard deviation of the hydrodynamic force when the payload is completely submerged ($\sigma(f_z)$). Wire tension parameters are improved as well. More specifically, the improvement related to the standard deviation in the heave compensation phase ($\sigma_{HC}(F_t)$) implies that the proposed controller attains the heave compensation objective better.

Magnitude	Int	Ext	Imp
max($\|f_z\|$)	4.80 N	4.15 N	13.54 %
$\sigma(f_z)$	0.20 N	0.11 N	45.00 %
max(F_t)	6.15 N	6.04 N	1.78 %
min(F_t)	1.15 N	1.70 N	32.35 %
$\sigma_{HC}(F_t)$	0.18 N	0.12 N	33.33 %
$\sigma(F_t)$	1.80 N	1.60 N	11.11 %

TABLE I

Averaged performance comparison.

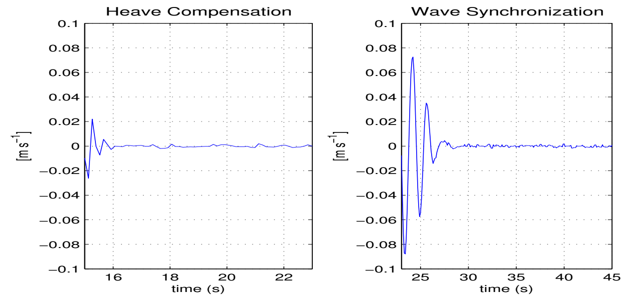

Fig. 2. Experimental results. Left: estimated regulation error $\hat{e}_h$ in the heave compensation phase. Right: estimated regulation error $\hat{e}_w$ in the wave synchronization phase.

This is consistent with the fact that the standard deviation calculated throughout the whole experiment ($\sigma(F_t)$), the maximum ($\max(F_t)$) and the minimum ($\min(F_t)$), are improved as well. In particular, the 32.35% improvement related to $\min(F_t)$ is important, since avoiding negative values of the wire tension is essential in order to prevent high snatch loads.

VII. Conclusions

In this work, a novel control strategy for cranes employed in heavy-lift offshore marine operations was presented. The proposed solution relies upon the use of an adaptive observer and two external models. Experimental results show that the control scheme proposed in this paper leads to removal of restrictive assumptions as well as an overall improvement over the results obtained with a classic internal model-based control as the one proposed in [6].

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